Susceptibility of boiling water reactor pressure vessel and its internals to degradation

Otso Cronvall
Susceptibility of boiling water reactor pressure vessel and its internals to degradation

Otso Cronvall

A doctoral dissertation completed for the degree of Doctor of Science (Technology) to be defended, with the permission of the Aalto University School of Engineering, at a public examination held at the lecture hall M1 of the school on 18.10.2019 at 12:00.

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This work concerns the susceptibility of boiling water reactor (BWR) unit reactor pressure vessel (RPV) and its internals to significant degradation modes. In the top level, this work consists of the following three parts: 1) literature survey and review, 2) component specific survey on susceptibility to degradation, 3) computational analysis approaches, tools and examples. The ageing of nuclear power plants (NPPs) emphasises the need to anticipate the possible degradation modes. The degradation potential of the NPP components is an important issue both in Finland and in other countries.

The survey on the susceptibility to degradation concerns BWR RPV and all its significant internals as well as an investigation of loads they experience. A screening process is performed on the need to carry out computational degradation potential analyses. The computational part consists of a description of applicable analysis approaches as well as of several representative computational examples. The covered approaches are both analytical and numerical, as well as both deterministic and probabilistic. New procedures are also developed, which are also used in the analysis examples. For the screening process, the component specific load induced stresses, strains and temperatures are computed. Using that and other data, the potential to brittle, ductile or other degradation is analysed for the selected components. The analysis targets are the Finnish Olkiluoto NPP units OL1 and OL2 run by the power company TVO. Based on the degradation analysis results, it is concluded that the operational lifetime of the internals of the OL1/OL2 RPVs can be safely continued from 40 to at least 60 years. Importantly, it is concluded that the operational lifetime of the OL1/OL2 RPVs and connecting main nozzles can be safely continued from 40 to even 80 years. According to the conservative TLAA results, the degradation in terms of crack growth is in most cases very or extremely slow. In the few cases with faster crack growth the cracks would be detected in the inspections well before they grow to any significant size.

The structural risk assessment results for the OL1/OL2 RPVs and their internals show that for all components but one the computed risk class is moderate or lower. It is concluded that for the OL1/OL2 RPV and their internals the overall structural risks are small and even in the case with highest risk acceptable.
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Tiivistelmä
Tämä tutkimus käsittelee kiehutusvesireaktorilaitoksen reaktoriaineastian ja sen sisäosien alttiutta erilaisille merkittävälle vaurioitumistavoille. Päätasolla tämä työ jakaantuu seuraaviin kolmeen osaan: 1) kirjallisuustutkimus ja -arvio, 2) komponenttikohtainen tutkimus alttiudesta erilaisille merkittävälle vaurioitumistavoille, 3) laskennalliset lähestymistavat, työkalut ja esimerkit. Ydinvoimalaiden ikääntyminen korostaa tarvetta ottaa huomioon mahdolliset vaurioitumistavat. Ydinvoimalaiden komponenttien vaurioitumispotentialia on tärkeää aihe sekä Suomessa että muissa maissa.

Seulontaprosessissa varten lasketaan komponenttikohtaiset lämpötilojen, jännitysten ja venymäjakaumat. Käytetään kyseisää ja muita tietoja, valituille komponentteille tehdään hauraan ja sitkeään murtoon sekä muiden vaurioitumisvaiheen etenemisen analyysia. Analysointikohteina ovat Suomessa Olkiluodossa sijaitsevat ydinvoimalayksiköt OL1 ja OL2, joita voimahyöty on TVO.

Analysointituloksiin perustuva johtopäätös on että OL1/OL2 reaktoriaineastioiden sisäosien käyttöikä voidaan turvallisesti jatkaa 40 vuodesta vähintään 60 vuoteen asti. Vielä tärkeämpä johtopäätös on että OL1/OL2 reaktoriaineastioiden ja niiden pääyhteyden käyttöikää voidaan turvallisesti jatkaa 40 vuodesta jopa 80 vuoteen asti. Konservatiivisten vaurioitumisanalyysien mukaan laskettu särönkasvu on suurimmassa osassa tapauksia hyvin teillä vähäistä. Niissä harvoissa tapauksissa, joissa laskettu särönkasvu olisi nopeampaa, säröä löydettiisi tarkastuksissa hyvissä ajoin ennen kuin ne kasvaisivat mihinkään merkittävään kokoon.
OL1/OL2 reaktoriaineastioille ja niiden sisäosille tehdyt rakenteellisen riskianalyysin tulosten mukaan kaikille piatisi yhdelle komponentille riskikuluokka on kohtullinen tai matalampi. Johtopäätös on että kyseiselle komponentille rakenteelliset riskit ovat yleisesti ottaen pieniä ja jopa yksittäisessä suurimmalle riskin tapauksessa hyväksyttyvällä tasolla.

Avainsanat
BWR, RPV, sisäosat, vaurioituminen, rakenteiden mekaniikka, murtumismekaniikka

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Preface

The author expresses his gratitude to Professor Juha Paavola for supervising the thesis as well as to Dr. Kim Wallin (VTT) for being its instructor. Gratitude is also expressed to professor Pål Efsing (KTH) and Dr. Robert Tregoning (U.S. NRC) who acted as pre-reviewers of this thesis work. Warmest thanks go to M.Sc. Smeekes (TVO) for his technical support, enthusiasm, patience and expert advice. The support from Swedish Asea-Atom NPPs was also significant. For that, gratitude is expressed to Mr. Lindback (Forsmark NPP), Mr. Jonsson (Oskarshamn NPP) and Mr. Lagerström (Ringhals NPP), to name but a few. Special thanks for expert advice concerning phenomenology and modelling of irradiation embrittlement of RPV steels go to research professor Robert Odette. The author also expresses his gratitude to VTT, power company TVO and BeräkningsGrupp (BG) for funding of this thesis. Gratitude is also addressed to several fellow researchers from VTT and Aalto University for providing useful technical background information and advice during the preparation of this thesis. Last but not least, much gratitude is dedicated to my late father M.Sc. Jyrki Brotherus who had a degree on physics. He provided wise advice and motivation during the early phase of the thesis work. Sadly he passed away before the thesis was completed, he is very much missed.

Espoo, September 11, 2019,

Otso Cronvall
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<tr>
<td>2FM</td>
<td>Two feature solute trap enhanced recombination model for irradiation embrittlement</td>
</tr>
<tr>
<td>3FM</td>
<td>Three feature model for irradiation embrittlement</td>
</tr>
<tr>
<td>ADS</td>
<td>Automatic depressurisation</td>
</tr>
<tr>
<td>ASME</td>
<td>American Society of Mechanical Engineers</td>
</tr>
<tr>
<td>ASTM</td>
<td>American Society for Testing and Materials</td>
</tr>
<tr>
<td>BWR</td>
<td>Boiling water reactor</td>
</tr>
<tr>
<td>BWRVIP</td>
<td>Boiling Water Reactor Vessel and Internals Project</td>
</tr>
<tr>
<td>CASS</td>
<td>Cast stainless steel</td>
</tr>
<tr>
<td>CC</td>
<td>Crevice corrosion</td>
</tr>
<tr>
<td>CCDP</td>
<td>Conditional core damage probability</td>
</tr>
<tr>
<td>CE</td>
<td>Combustion Engineering</td>
</tr>
<tr>
<td>CFD</td>
<td>Computational fluid dynamics</td>
</tr>
<tr>
<td>CGGR</td>
<td>Crack growth rate</td>
</tr>
<tr>
<td>CR</td>
<td>Creep</td>
</tr>
<tr>
<td>CRD</td>
<td>Control rod drive</td>
</tr>
<tr>
<td>CRP</td>
<td>Cu rich precipitates</td>
</tr>
<tr>
<td>CUF</td>
<td>Cumulative usage factor</td>
</tr>
<tr>
<td>CVN</td>
<td>Charpy V-notch</td>
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<tr>
<td>DFM</td>
<td>Deterministic fracture mechanics</td>
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<td>DHC</td>
<td>Delayed hydride cracking</td>
</tr>
<tr>
<td>EAF</td>
<td>Environmentally assisted fatigue</td>
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<tr>
<td>ECCS</td>
<td>Emergency core cooling system</td>
</tr>
<tr>
<td>E/C</td>
<td>Erosion-corrosion</td>
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<tr>
<td>ECP</td>
<td>Electro-chemical corrosion potential</td>
</tr>
<tr>
<td>EDF</td>
<td>Electricité de France</td>
</tr>
<tr>
<td>EFU</td>
<td>Effective full power year</td>
</tr>
<tr>
<td>EOL</td>
<td>End-of-life</td>
</tr>
<tr>
<td>EPFM</td>
<td>Elastic-plastic fracture mechanics</td>
</tr>
<tr>
<td>EPRI</td>
<td>Electric Power Research Institute</td>
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<tr>
<td>EPU</td>
<td>Extended power uprate</td>
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<tr>
<td>FAS</td>
<td>Flow accelerated corrosion</td>
</tr>
<tr>
<td>FCG</td>
<td>Fatigue crack growth</td>
</tr>
<tr>
<td>FE</td>
<td>Finite element</td>
</tr>
<tr>
<td>FITNET</td>
<td>European Fitness For Service Network</td>
</tr>
<tr>
<td>FIV</td>
<td>Flow induced vibration</td>
</tr>
<tr>
<td>FMECA</td>
<td>Failure modes, effects and criticality analysis</td>
</tr>
<tr>
<td>FSD</td>
<td>FS Dynamics</td>
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<tr>
<td>GE</td>
<td>General Electric</td>
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<tr>
<td>GTAW</td>
<td>Gas-tungsten arc welding</td>
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<tr>
<td>HAZ</td>
<td>Heat affected zone</td>
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<tr>
<td>HCF</td>
<td>High-cycle fatigue</td>
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<tr>
<td>HSB</td>
<td>Hot stand-by</td>
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<tr>
<td>HWC</td>
<td>Hydrogen water chemistry</td>
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<tr>
<td>IAEA</td>
<td>International Atomic Energy Agency</td>
</tr>
<tr>
<td>IASCC</td>
<td>Irradiation assisted stress corrosion cracking</td>
</tr>
<tr>
<td>IE</td>
<td>Irradiation embrittlement</td>
</tr>
<tr>
<td>Acronym</td>
<td>Description</td>
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<tr>
<td>----------</td>
<td>--------------------------------------------------</td>
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<tr>
<td>IGSCC</td>
<td>Intergranular stress corrosion cracking</td>
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<tr>
<td>IHSI</td>
<td>Induction heat stress improvement</td>
</tr>
<tr>
<td>IMT</td>
<td>Issue Management Table</td>
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<td>ISI</td>
<td>In-service inspection</td>
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<tr>
<td>ISP</td>
<td>Integrated surveillance programme</td>
</tr>
<tr>
<td>IWM</td>
<td>Fraunhofer-Institut für Werkstoffmechanik</td>
</tr>
<tr>
<td>JEAC</td>
<td>Japan Electric Association</td>
</tr>
<tr>
<td>LBP</td>
<td>Late blooming phase</td>
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<tr>
<td>LCF</td>
<td>Low-cycle fatigue</td>
</tr>
<tr>
<td>LEFM</td>
<td>Linear-elastic fracture mechanics</td>
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<tr>
<td>LHS</td>
<td>Latin hypercube simulation</td>
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<tr>
<td>LP</td>
<td>Laser peening</td>
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<tr>
<td>LPRM</td>
<td>Local power range monitor</td>
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<td>LTO</td>
<td>Long-term operation</td>
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<td>LTSCC</td>
<td>Low temperature creep cracking</td>
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<td>LWR</td>
<td>Light water reactor</td>
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<td>MCS</td>
<td>Monte Carlo simulation</td>
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<tr>
<td>MDM</td>
<td>Materials Degradation Matrix</td>
</tr>
<tr>
<td>MIC</td>
<td>Microbiologically induced corrosion</td>
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<td>MNP</td>
<td>Mn-Ni rich precipitates</td>
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<td>MRP</td>
<td>Materials Reliability Program</td>
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<td>MSIP</td>
<td>Mechanical stress improvement</td>
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<td>MSIV</td>
<td>Main steam isolation valve</td>
</tr>
<tr>
<td>MW</td>
<td>Mechanical wear</td>
</tr>
<tr>
<td>NDT</td>
<td>Non-destructive testing</td>
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<tr>
<td>NMCA</td>
<td>Noble Metal Chemical Application</td>
</tr>
<tr>
<td>NWC</td>
<td>Normal water chemistry</td>
</tr>
<tr>
<td>NPP</td>
<td>Nuclear power plant</td>
</tr>
<tr>
<td>OL1</td>
<td>Olkiluoto 1 nuclear power plant unit</td>
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<tr>
<td>OL2</td>
<td>Olkiluoto 2 nuclear power plant unit</td>
</tr>
<tr>
<td>ODSCC</td>
<td>Outside diameter stress corrosion cracking</td>
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<tr>
<td>PC</td>
<td>Pitting corrosion</td>
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<td>PDF</td>
<td>Probability density function</td>
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<td>PFM</td>
<td>Probabilistic fracture mechanics</td>
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<td>PGA</td>
<td>Peak ground acceleration</td>
</tr>
<tr>
<td>POD</td>
<td>Probability of detection</td>
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<tr>
<td>POF</td>
<td>Probability of failure</td>
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<tr>
<td>PRA</td>
<td>Probabilistic Risk Assessment</td>
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<tr>
<td>PREDB</td>
<td>U.S. power reactor embrittlement database</td>
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<tr>
<td>PSR</td>
<td>Periodic safety review</td>
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<tr>
<td>PTS</td>
<td>Pressurised thermal shock</td>
</tr>
<tr>
<td>PWHT</td>
<td>Post-weld heat treatment</td>
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<td>PWR</td>
<td>Pressurized water reactor</td>
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<td>PWSCC</td>
<td>Primary water stress corrosion cracking</td>
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<tr>
<td>R &amp; D</td>
<td>Research and development</td>
</tr>
<tr>
<td>RDM</td>
<td>Rotterdam Dry-dock and Manufacturing</td>
</tr>
<tr>
<td>RED</td>
<td>Radiation enhanced diffusion</td>
</tr>
<tr>
<td>RIM</td>
<td>Reactor Internals Management</td>
</tr>
<tr>
<td>RPV</td>
<td>Reactor pressure vessel</td>
</tr>
<tr>
<td>RT</td>
<td>Room temperature</td>
</tr>
<tr>
<td>SAFT</td>
<td>Synthetic aperture focusing technique</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
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<tr>
<td>SCC</td>
<td>Stress corrosion cracking</td>
</tr>
<tr>
<td>SIL</td>
<td>Service Information letter</td>
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<td>SIRM</td>
<td>Intermediate range monitor</td>
</tr>
<tr>
<td>SP</td>
<td>Shot peening</td>
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<tr>
<td>SRM</td>
<td>Standard reference material</td>
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<td>SRV</td>
<td>Safety relief valve</td>
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<tr>
<td>SSM</td>
<td>Radiation Safety Authority of Sweden</td>
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<tr>
<td>STUK</td>
<td>Radiation and Nuclear Safety Authority of Finland</td>
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<tr>
<td>TE</td>
<td>Thermal embrittlement</td>
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<td>TGSCC</td>
<td>Transgranular stress corrosion cracking</td>
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<tr>
<td>TIP</td>
<td>Calibration detector</td>
</tr>
<tr>
<td>TLAA</td>
<td>Time-limited ageing analyses</td>
</tr>
<tr>
<td>Transient</td>
<td>Time history of temperature, pressure and flow rate</td>
</tr>
<tr>
<td>TVO</td>
<td>Teollisuuden Voima Oy</td>
</tr>
<tr>
<td>UMD</td>
<td>Unstable matrix defects</td>
</tr>
<tr>
<td>USE</td>
<td>Upper shelf energy</td>
</tr>
<tr>
<td>UT</td>
<td>Ultrasonic technique</td>
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<tr>
<td>U.S. NRC</td>
<td>United States Nuclear Regulatory Commission</td>
</tr>
<tr>
<td>VTT</td>
<td>Technical Research Centre of Finland</td>
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<tr>
<td>VVER</td>
<td>Vodo-vodjanoi energetitšeski reaktor</td>
</tr>
<tr>
<td>WJP</td>
<td>Water jet peening</td>
</tr>
<tr>
<td>WOL</td>
<td>Structural weld overlay</td>
</tr>
<tr>
<td>WRS</td>
<td>Weld residual stress</td>
</tr>
</tbody>
</table>

**Latin letters**

- **a**: Crack depth
- **a_{adj}**: Dimensional adjustment coefficient
- **b**: Width
- **c**: Half of crack length
- **C**: Specific heat
- **C**: Effective corrosion rate in absence of coating
- **C**: Rate parameter in computation of general corrosion
- **C_b**: Bulk concentration
- **C_F**: Chemistry factor
- **C_{FA}, m_{FA}**: Material, temperature and environment specific constants in fatigue crack growth computation
- **C_s**: Surface concentration
- **C_{SCC}, n_{SCC}**: Material, temperature and environment specific constants in stress corrosion cracking computation
- **Cu**: Quantity of copper
- **C_{M_{adj}}**: Dimensional adjustment coefficient
- **C_{V}**: Charpy impact energy
- **C_{V_{adj}}**: Dimensional adjustment coefficient
- **D**: Total accumulated fatigue damage
- **D_i**: Inner diameter
- **D_o**: Outer diameter
- **E**: Elastic modulus, Young’s modulus
- **E**: Energy
- **E_k**: Activation energy
Accumulated neutron fluence
Flow rate
Temperature factor in computation of flow accelerated corrosion
Mass transfer factor in computation of flow accelerated corrosion
Geometry factor in computation of flow accelerated corrosion
pH factor in computation of flow accelerated corrosion
Oxygen factor in computation of flow accelerated corrosion
Alloy factor in computation of flow accelerated corrosion
Void fraction factor in computation of flow accelerated corrosion
Dimensional adjustment coefficient
Nominal environmental fatigue correction factor for stress cycle $i$
Nominal environmental fatigue correction factor
Environmental fatigue correction factor for service temperature
Fatigue correction factor for transferring fatigue data obtained in laboratory with test specimens to real components
Environmental fatigue correction factor for service water
Environmental fatigue correction factor for real effects of LWR environment
Value of neutron fluence at inner wetted surface
Height
Hardness of the softer component
$J$-integral, the strain energy rate
Deformed fracture toughness,
Mode I fracture toughness, applicable to both brittle and ductile materials
Mass transfer coefficient
Empirically determined wear coefficient
Mode I stress intensity factor
Critical Mode I fracture toughness for the lower bound crack arrest
Dimensional adjustment coefficient
Critical Mode I fracture toughness for the lower bound crack arrest divided by the specified safety factor
Critical Mode I fracture toughness for the lower bound crack initiation
Mode I stress intensity factor for pure tension
Dimensionless stress intensity parameter
Mode I stress intensity factor caused by mechanical loads
Maximum value of Mode I stress intensity factor
Minimum value of Mode I stress intensity factor
Mode I stress intensity factor caused by thermal loads
Mode I stress intensity factor threshold
Median $J$-Integral based fracture toughness for ferritic steels
Length
Power law exponent
Fatigue life until failure
Number of simulations
Number of load cycles
Fatigue life number of load cycles in air at room temperature
Number of failures
Number of load cycles under stress level $i$
Number of load cycles that would produce fatigue failure under stress level $i$
Quantity of nickel
Dimensional adjustment coefficient
Fatigue life number of load cycles in water at the service temperature
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$p$</td>
<td>Pressure</td>
</tr>
<tr>
<td>$P$</td>
<td>Quantity of phosphorus</td>
</tr>
<tr>
<td>$P_{adj}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$Q$</td>
<td>Contact normal force</td>
</tr>
<tr>
<td>$Q_g$</td>
<td>Thermal activation energy for stress corrosion cracking</td>
</tr>
<tr>
<td>$R$</td>
<td>Stress ratio</td>
</tr>
<tr>
<td>$R$</td>
<td>Universal gas constant</td>
</tr>
<tr>
<td>$R_{adj1}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$R_{adj2}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$R_{E-C}$</td>
<td>Total rate of erosion-corrosion</td>
</tr>
<tr>
<td>$R_i$</td>
<td>Inner radius</td>
</tr>
<tr>
<td>$R_k$</td>
<td>Kinetic rate of oxide dissolution</td>
</tr>
<tr>
<td>$R_{MT}$</td>
<td>Mass transferred limited rate</td>
</tr>
<tr>
<td>$R_o$</td>
<td>Outer radius</td>
</tr>
<tr>
<td>$R_{limit}$</td>
<td>Reference nil-ductility temperature shift limit</td>
</tr>
<tr>
<td>$R_{NDT}$</td>
<td>Reference nil-ductility temperature</td>
</tr>
<tr>
<td>$R_{T0}$</td>
<td>Reference nil-ductility temperature</td>
</tr>
<tr>
<td>$R_{NDT,aged}$</td>
<td>Reference nil-ductility temperature of thermally aged RPV steels and associated welds</td>
</tr>
<tr>
<td>$R_{NDT,ia}$</td>
<td>Reference nil-ductility temperature after thermal annealing,</td>
</tr>
<tr>
<td>$R_{NDT,initial}$</td>
<td>Reference nil-ductility temperature for the unirradiated material</td>
</tr>
<tr>
<td>$s$</td>
<td>Sliding distance</td>
</tr>
<tr>
<td>$S_i$</td>
<td>Stress level</td>
</tr>
<tr>
<td>$S_i$</td>
<td>Quantity of silicon</td>
</tr>
<tr>
<td>$S_{adj}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$S_m$</td>
<td>Design stress</td>
</tr>
<tr>
<td>$S_t$</td>
<td>Tensile strength</td>
</tr>
<tr>
<td>$S_y$</td>
<td>Yield strength</td>
</tr>
<tr>
<td>$t$</td>
<td>Time</td>
</tr>
<tr>
<td>$t$</td>
<td>Wall thickness</td>
</tr>
<tr>
<td>$T$</td>
<td>Temperature</td>
</tr>
<tr>
<td>$T_0$</td>
<td>Reference fracture toughness transition temperature corresponding to the temperature at which median $K_{JC} = 100 \text{ MPa} \cdot \text{m}^{-1}$</td>
</tr>
<tr>
<td>$T_{100%}$</td>
<td>Temperature under 100 % normal operation</td>
</tr>
<tr>
<td>$t_a$</td>
<td>Annealing time</td>
</tr>
<tr>
<td>$T_a$</td>
<td>Annealing temperature</td>
</tr>
<tr>
<td>$T_{adj}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$t_{adj}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
<tr>
<td>$t_i$</td>
<td>Initiation time for onset of general corrosion</td>
</tr>
<tr>
<td>$T_{ref}$</td>
<td>Absolute reference temperature</td>
</tr>
<tr>
<td>$T_{K0.9mm}$</td>
<td>0.9 mm lateral expansion transition temperature</td>
</tr>
<tr>
<td>$TK_{41J}$</td>
<td>41 J impact energy transition temperature</td>
</tr>
<tr>
<td>$TK_{56J}$</td>
<td>56 J impact energy transition temperature</td>
</tr>
<tr>
<td>$T_m$</td>
<td>Mean reference temperature of measured populations</td>
</tr>
<tr>
<td>$U_1, \ldots, U_n$</td>
<td>Partial fatigue usage factors</td>
</tr>
<tr>
<td>$V$</td>
<td>Volumetric amount of material loss due to wear</td>
</tr>
<tr>
<td>$W_C$</td>
<td>Thickness of the layer that has corroded away under flow accelerated corrosion</td>
</tr>
<tr>
<td>$x$</td>
<td>Coordinate through component wall</td>
</tr>
<tr>
<td>$x$</td>
<td>depth of propagation of general corrosion</td>
</tr>
<tr>
<td>$x_{adj}$</td>
<td>Dimensional adjustment coefficient</td>
</tr>
</tbody>
</table>
Greek letters

\( \alpha \)  Crack growth rate coefficient for stress corrosion cracking
\( \alpha \)  time-order parameter in computation of general corrosion
\( \alpha_r \)  Coefficient of thermal expansion
\( \beta \)  Dimensionless exponent for computing stress corrosion cracking
\( \Delta a \)  Crack extension
\( \Delta a_{adj} \)  Dimensional adjustment coefficient
\( \Delta \phi_R \)  is rate of flow accelerated corrosion
\( \Delta \)  Damage fraction due to fatigue
\( \Delta K_{eff} \)  Effective stress intensity factor range
\( \Delta K_I \)  Range of Mode I stress intensity factor
\( \Delta K_{I,adj} \)  Dimensional adjustment coefficient
\( \Delta R_{NTD} \)  Shift in reference nil-ductility temperature
\( \Delta R_{NTD,ageing} \)  Shift in reference nil-ductility temperature of RPV steels and associated welds due to thermal ageing
\( \varepsilon u \)  Ultimate strain
\( \phi \)  Neutron flux
\( \phi_{adj} \)  Dimensional adjustment coefficient
\( \lambda \)  Thermal conductivity
\( \nu \)  Poisson’s coefficient
\( \rho \)  Density
\( \rho_{st} \)  Density of steel
\( \sigma_{0.2} \)  Yield strength of irradiated austenitic stainless steel
\( \sigma_{adj} \)  Dimensional adjustment coefficient
\( \sigma_{\Delta} \)  Standard deviation of \( \Delta R_{NTD} \)
\( \sigma_{\Delta,adj} \)  Dimensional adjustment coefficient
\( \sigma_T \)  Tensional membrane stress
\( \sigma_I \)  Standard deviation for the initial \( RT_{NDT} \)
\( \sigma_{I,adj} \)  Dimensional adjustment coefficient
\( \sigma_{Tm} \)  Standard deviation around the mean reference temperature of measured populations
1 Objectives

The objectives of this dissertation are summarised in the following. This includes also the new developments and summarising the steps of how to achieve the objectives.

The main objective is to demonstrate that the operational lifetime of a boiling water reactor (BWR) unit reactor pressure vessel (RPV) and its main nozzles can be safely extended from 40 to even 80 years. The secondary objective is to demonstrate that the operational lifetime of the internals of a BWR RPV can be safely extended from 40 to at least 60 years. Most of the internals are replaceable, whereas the RPV is not. As a necessary support to achieve these objectives, some new analysis developments are provided.

Concerning original characteristics, the whole of this work has those. This is because to the knowledge of the author, this is the first dissertation this far that concerns in detail the susceptibility of a BWR RPV and internals to degradation mechanisms as well as related degradation analyses. As developments, this work presents for a BWR RPV and its internals both a new screening procedure and a new structural risk assessment procedure.

To fulfil the above mentioned objectives, a wide scope of information and computational analyses is presented, as summarised step by step in the following.

The information on degradation issues is gathered from all significant sources, including the International Atomic Energy Agency (IAEA), research institutes, national regulators and scientific literature. An important source of technical information and support is the Finnish power company TVO, who operates the two Asea-Atom BWR units at Olkiluoto. The collection of the needed data includes also discussions with experts from the Swedish Asea-Atom nuclear power plants (NPPs), namely those at Forsmark, Oskarshamn and Ringhals. Former Asea-Atom is nowadays Westinghouse. The gathered information on significant degradation mechanisms concerns overall descriptions, observed degradation, computational modelling, susceptibility, mitigation and inspections.

The investigation on the susceptibility of BWR RPV and its internals to degradation concerns the Olkiluoto NPP units OL1 and OL2. This investigation includes describing the function and characteristics of the selected components as well as their susceptibility to degradation. The susceptible components are considered in the ensuing analyses. These analyses require information on the loads that the considered components are exposed to. Such loads are described.

The susceptible components are screened before continuing to the computational degradation analyses. The developed new screening process is used. Only those components that screen in enter the degradation analyses.

The first phase of the computational part is the heat transfer and stress/strain analyses. The results from these analyses are a part of the necessary input data for the ensuing degradation analyses. The results from the latter analyses depict the propagation of degradation for all components that screened-in, considering all degradation mechanisms that they are susceptible to. For the OL1/OL2 RPV the considered time span is 80 years, whereas that for its internals is 60 years. The computational part includes also the structural risk analysis for all considered OL1/OL2 components.
The results from the performed analyses demonstrate the fulfilment of the above mentioned objectives.
## 2 Introduction and scope

This work concerns the susceptibility of BWR RPVs and their internal components (internals) to significant degradation mechanisms. The RPV is comprised of a shell and a removable top head, both with flanges, and of a bottom part. The shell accommodates multiple nozzles, penetrations and control rod drive stub tubes, RPV support skirt and several attachment welds. The main internals of BWR RPVs include: steam dryer, steam separator stand pipes, core shroud, core spray piping, fuel assembly, control rods, control rod guide tubes and nozzles as well as recirculation pumps.

Operating BWR RPVs and internals have been fabricated by several suppliers. Those operated in Finland and Sweden were fabricated by Asea-Atom (which is nowadays Westinghouse). On average, the height of a BWR RPV is approximately 19600 mm and the outer diameter is approximately 5660 mm, while the wall thickness is approximately 140 mm. Figure 2-1 shows the ASEA-ATOM BWR RPV and some of its main internals. A more detailed description of the BWR RPV and internals is presented in Section 5.

As for the literature part, the plant experiences from Finland and those from abroad are important data sources. These data and associated technical documentation have been collected to databases by license holders, regulators and research organisations. On the other hand, published results from experimental and computational research projects by research organisations from Nordic and other countries offer also valuable data on these issues. As applicable, these data sources can be of support when weighing the possible susceptibility of BWR RPV and its internals to different degradation mechanisms. All known degradation mechanisms and their modelling approaches will be handled. These mechanisms include irradiation embrittlement, thermal embrittlement, fatigue, stress corrosion cracking (SCC), general corrosion, erosion-corrosion, flow accelerated corrosion (FAC), mechanical wear, creep, assembly errors and vibration. After the detailed discussion all degradation mechanisms are summarized in different ways to investigate their relation and to make further handling more structured.

The component specific survey on susceptibility to degradation mechanisms concerns BWR RPV and all its significant internals as well as an investigation of component specific loads and screening process on the need to carry out computational analyses.

The computational part consists of a description of applicable analysis approaches as well as of several representative computational examples for the OL1/OL2 units. The covered existing computational approaches are both analytical and numerical, as well as both deterministic and probabilistic. In the example analyses, both existing and new computational procedures are applied to the OL1/OL2 RPV internals, as selected through the screening process. For them, load induced stresses, strains and temperatures are computed first, then brittle and/or ductile degradation.

A structural risk assessment procedure was developed for BWR RPV and its internals. In this work it is applied to the OL1/OL2 units.

This work ends with a summary and conclusions, including suggestions for further research.

The structure of this work is as follows. The objectives of this work are presented in Section 1. This is followed by introduction and scope of thesis in Section 2. The literature survey on degradation mechanisms affecting BWR RPVs and their internals is summarised in Sections 3
and 4, respectively. This is followed by a description on the susceptibility of the OL1/OL2 RPV and some of its internals to degradation in Section 5. The loads for these components are described in Section 6. The component specific screening process as well as computational analysis approaches, tools and examples are described in Sections 7 and 8, respectively. The assessment of the structural risk for failure for the OL1/OL2 RPV and its internals is presented in Section 9. The summary of the whole work is presented in Section 10. The conclusions from the work and suggestions for further research are presented in Section 11. Appendix A describes in detail the results from the literature survey on degradation mechanisms affecting NPP structures and components. Appendix B describes in detail the results from the survey on and modelling of degradation mechanisms affecting BWR RPV and its internals. Appendix C contains a summary on the characteristics and degradation susceptibility of all considered OL1/OL2 components.

![Diagram of Asea-Atom BWR RPV and some of its main internals](image)

**Figure 2-1. Vertical section of Asea-Atom BWR RPV and some of its main internals, from ref. [1].**
3 Degradation mechanisms affecting nuclear power plant structures and components

The commonly used expression “degradation mechanism” is also used in this work to denote the different kinds of degradation phenomena which can affect NPP structures and components. They can act both alone and in combination. However, most often one degradation mechanism either governs over the others, or is acting alone. The degradation mechanisms are also called ageing mechanisms. Both of these expressions are used in the following as they are interchangeable. The focus of this study is on degradation mechanisms which can affect the BWR RPV and its internals. These components are metallic. The base material of the BWR RPVs is ferritic steel, whereas that of internals is typically austenitic stainless steel or nickel-base alloy [1].

Ageing mechanisms are specific processes that gradually change characteristics of a component with time and use [1]. Ageing degradation means those cumulative changes that can impair the ability of a component to function within acceptance criteria. Service conditions outside the prescribed limits can accelerate the rate of degradation.

The technical definition of ageing given by the International Atomic Energy Agency (IAEA) is the following [2]: Ageing is the continuous time dependent degradation of materials due to normal service conditions, which include normal operation and transient conditions.

3.1 Overview of degradation mechanisms

The degradation modes are presented first. The relevant degradation mechanisms are presented in Table 3.1-1. During recent decades a large number of documents have been published on degradation of NPP components. The earlier documents are to varying extent outdated due to a smaller number of experienced operational reactor years. Here, the descriptions of degradation mechanisms mainly follow the most recent and/or relevant reports on the issue by IAEA [1,2], the Electric Power Research Institute (EPRI) [3,4,5], the United States Nuclear Regulatory Commission (U.S. NRC) [6,7,8,128] and the Swedish Radiation Safety Authority (SSM) [9].

Depending on the metal or alloy, the intensity of the external loading and environmental impact, there may be a high potential for degradation. The processes of degradation can result in material and geometry changes, of which the most important are [2]:

- reduced toughness (embrittlement),
- cracking,
- swelling,
- thinning,
- denting, and
- pitting.
Table 3.1-1. Relevant degradation mechanisms concerning BWR RPV and internals [1-8].

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Degradation sub-mechanism</th>
</tr>
</thead>
<tbody>
<tr>
<td>Irradiation embrittlement (IE)</td>
<td></td>
</tr>
<tr>
<td>Thermal embrittlement (TE)</td>
<td></td>
</tr>
<tr>
<td>Fatigue (FA)</td>
<td>• low-cycle fatigue (LCF)</td>
</tr>
<tr>
<td></td>
<td>• high-cycle fatigue (HCF)</td>
</tr>
<tr>
<td></td>
<td>• environmentally assisted fatigue (EAF)</td>
</tr>
<tr>
<td>Stress corrosion cracking (SCC)</td>
<td>• intergranular SCC (IGSCC)</td>
</tr>
<tr>
<td></td>
<td>• transgranular SCC (TGSCC)</td>
</tr>
<tr>
<td></td>
<td>• irradiation assisted SCC (IASCC)</td>
</tr>
<tr>
<td></td>
<td>• primary water SCC (PWSCC)</td>
</tr>
<tr>
<td>General corrosion (GC)</td>
<td></td>
</tr>
<tr>
<td>Local corrosion</td>
<td>• pitting corrosion (PC)</td>
</tr>
<tr>
<td></td>
<td>• crevice corrosion (CC)</td>
</tr>
<tr>
<td>Erosion-corrosion, flow accelerated corrosion (FAC)</td>
<td></td>
</tr>
<tr>
<td>Creep (CR)</td>
<td></td>
</tr>
<tr>
<td>Mechanical wear (MW)</td>
<td></td>
</tr>
</tbody>
</table>

EPRI has provided for BWR RPV and internals a very thorough and detailed investigation in analysis report series produced by the Boiling Water Reactor Vessel and Internals Project (BWRVIP). For this work the BWRVIP series is the most important one. However, there are limitations to the availability of most BWRVIP documents/reports. Another important series of NPP analysis reports provided by EPRI has been produced by the Materials Reliability Program (MRP). The MRP series focuses on pressurized water reactor (PWR) plants. On the other hand, the EPRI Materials Degradation Matrix, Revision 3 [3] contains detailed summaries of these degradation issues and uses most of the BWRVIP and MRP reports as references.

Some degradation mechanisms concerning the BWR RPV and internals have been left out from Table 3.1-1. This is because they are either very rare, concern only certain safety insignificant components, do not concern BWR RPV and internals, and/or have emerged only in the earliest phase of plant operation. In the last case they have then been removed altogether with improved design, maintenance and fabrication practices. This information is confirmed by ref. [3]. For any significant creep the operational temperatures of BWR RPVs and internals are too low. The bolts in some joints do experience some stress relaxation, but as they are easily replaceable considering them is excluded here.

For the BWR RPVs and internals, these minor and/or insignificant degradation mechanisms are:

- Corrosion:
  - wastage,
  - fouling,
  - galvanic corrosion,
  - microbiologically induced corrosion (MIC),
- Irradiation-creep,
- Stress relaxation,
- Void swelling.
3.2 Summary on relevant degradation mechanisms

The degradation mechanisms concerning metallic NPP components are summarised in the following. In addition to already mentioned refs. [1-8], other relevant background documents include refs. [10-14,16,17]. In addition to loading and environmental conditions, degradation of NPP components depends very much on the material. The BWR RPVs are of low alloy steel, whereas their internals are mainly of austenitic stainless steel. As for piping systems, they are mostly made of austenitic stainless steel, and to a lesser extent of ferritic steel [1,3,8].

Of the covered literature on degradation mechanisms of metallic NPP components, the EPRI Materials Degradation Matrix, Revision 3 [3] is the most detailed and widest in scope as well as the most recent one.

Detailed descriptions of the degradation mechanisms concerning metallic NPP components are presented in Appendix A: Detailed Literature survey on degradation mechanisms affecting nuclear power plant structures and components.

Irradiation embrittlement

Neutrons from the nuclear fuel produce energetic primary recoil atoms in metals, which displace large numbers of atoms from their crystal lattice positions by a chain of atomic collisions [17]. This number of neutrons bombarding a given location is traditionally measured by the fluence (number of neutrons per square meter, n/m², for energy $E > 1.0$ MeV) or displacements per atom (dpa). The correlation data from different sources with diverse neutron energy spectra is best accomplished with dpa, which accounts for both fluence and neutron energy levels [4]. The fluence or dpa provide part of the information needed to assess irradiation embrittlement.

Irradiation embrittlement of metals is caused by the lattice defects induced by neutron bombardment. High energy neutrons cause point defects. Part of these defects form various irradiation induced microstructural features consisting of dislocations, precipitates and cavities. Then, the cavities can be associated with other microstructural details, such as precipitates, dislocations and grain boundaries. These defects and precipitates from irradiation are obstacles to dislocation movement and result in an increase of yield and tensile strength as well as decreased work hardening capacity and ductility, and loss of fracture toughness [3,17].

Illustratively, Figure 3.2-1 shows for ferritic RPV steel and welds the effect of irradiation on the tensile fracture stress and the corresponding effect on the Charpy test impact toughness. These are shown as a shift of the ductile-to-brittle transition to higher temperatures and the reduction of the upper shelf energy. The significant role of copper content is shown for two welds, which are similar, except for the copper content.
Figure 3.2-1. Schematic diagrams depicting (a) how irradiation induced strength increase results in an upward shift in the Charpy impact toughness transition temperature, and (b) showing the significant role of copper content towards increasing radiation sensitivity, from ref. [70].

Thermal embrittlement

Thermal embrittlement of metals is a time and temperature dependent degradation mechanism. It is caused by the thermally activated movement of lattice atoms over a relatively long time period, a process which can occur without external mechanical load. Resulting changes in microstructure and material properties include embrittlement, as indicated by the decrease in ductility and toughness, as well as an increase in strength properties and hardness [1].

Fatigue

Fatigue of metals is defined as the structural deterioration that occurs as a result of repeated stress/strain cycles caused by fluctuating mechanical load and/or temperature. After enough many repeated cyclic loads of sufficient magnitude, microstructural damage can accumulate,
leading to macroscopic crack initiation at the locations experiencing the highest stress/strain fluctuation. Subsequent continued cyclic loading can lead to the growth of the initiated crack. Fatigue behaviour is related to a variety of parameters, most importantly stress/strain range, mean stress, cycling frequency, surface roughness and environmental conditions [1,3,10,11].

A metal component subjected to fluctuating stress will fail at stresses much lower than those required to cause fracture in a single application of a load [16]. This fatigue limit is an important material property from an engineering point of view. It can be formally defined as a stress amplitude for which the fatigue life becomes infinite in view of the asymptotic character of the stress versus the number of load cycles (S-N) curve.

Fatigue at high amplitudes and fatigue lives up to approximately 10000 load cycles is commonly called low-cycle fatigue. If fatigue covers a large number of cycles, approximately 100000 load cycles or more, it is called high-cycle fatigue [11]. The boundary between low-cycle and high-cycle fatigue is not defined by a specific number of load cycles.

The crack initiation period includes the growth of the initial micro-crack(s). Because the growth rate is still low, the initiation period may cover a significant part of the fatigue life. This is illustrated by the generalized picture of crack growth curves by Schijve [11], see Figure 3.2-2 below. It schematically shows the crack growth development as a function of the percentage of the fatigue life consumed.

![Figure 3.2-2. Different scenarios for fatigue crack growth, from ref. [11]. Therein, the dash-dot lines are approximations for illustration purposes.](image)
Stress corrosion cracking (SCC)

SCC corresponds to crack initiation and ensuing crack growth in susceptible steels or alloys under the influence of tensile stress and a corrosive environment. SCC is a complex phenomenon, driven by the synergistic interaction of mechanical, electro-chemical and metallurgical factors [1,12,13,14,15]. BWR RPV internals are potentially susceptible to the following two forms of SCC [1]:

- IGSCC,
- TGSCC, and
- IASCC.

SCC can proceed through a material in either of the following two modes: intergranular (IGSCC), i.e. along the grain boundaries, or transgranular (TGSCC), i.e. through the grains. Sometimes the modes are mixed or the mode switches from one mode to the other. IGSCC and TGSCC often occur in the same steel or alloy, depending on the environment, the microstructure or the stress/strain state.

SCC can occur in ductile metallic materials with little or no plastic strain accumulation associated with the process [7]. The three necessary conditions for the occurrence of IGSCC are described as follows [1]:

- susceptible material,
- tensile stress,
- corrosive environment.

There has been two major material factors that have contributed to IGSCC of austenitic stainless steels and alloys in BWR components: thermal sensitization and cold work [28].

Exposure to high levels of neutron fluence can also cause stainless steels to become susceptible to SCC. This is a special form of SCC known as IASCC, which is also characterized by intergranular crack initiation and propagation. However, there are subtle differences between IASCC and IGSCC. Austenitic stainless steels that undergo IASCC need not be thermally sensitized or cold worked. IASCC is also highly dependent on neutron fluence exposure level [28].

General corrosion

General corrosion is characterized by uniform surface loss through material oxidation. In aqueous solutions including reactor coolants, it is primarily an electro-chemical process involving an anodic oxidation reaction to form positively charged metal cations, which is balanced electrically by a cathodic reaction involving the reduction of oxygen in solution (if present) and/or reduction of hydrogen ions to molecular hydrogen gas. The point of balance between the anodic and cathodic currents determines the characteristic corrosion potential, i.e. electro-chemical corrosion potential (ECP), which is measurable [1,3].

General corrosion of all metallic materials in reactor coolants typically proceeds at a few microns per year at most [3]. For BWR RPV and its internals general corrosion is very unlikely. In case of the BWR RPV this is mainly due to the inner cladding of austenitic stainless steel.
Local corrosion

The modes of local corrosion are crevice corrosion and pitting corrosion. These corrosion modes cannot occur in reactor coolant environments without significant perturbation of the intended water chemistry. For this reason, localized corrosion modes in water cooled reactors mainly concern tertiary water systems as well as systems containing stagnant water.

Crevice corrosion is caused by corrosion cells which are nucleated by differences in concentration in the corrosive environment. The corrosion manifested in the crevice can be uniform, shallow pitting or selective [2]. Crevice corrosion is associated with occluded volumes inside which a quasi-stagnant solution is present with a mechanism making the solution more aggressive, e.g. increased impurity concentration with increased acidity or alkalinity [3].

The mechanism of pitting corrosion can be divided into three consecutive steps [35,36]:
- initiation,
- metastable propagation, and
- stable propagation.

Pitting corrosion has very similar attributes to electro-chemical crevice corrosion, as it also depends in part on the creation of a localized environment within the pit. However, an important difference is that the features that lead to pit initiation are inherent in the microscopic material and non-metallic inclusion properties, as compared to the macroscopic dimensions associated with crevice corrosion.

Erosion-corrosion, flow accelerated corrosion

The movement of solutions above a threshold velocity can degrade metals by the interaction of fluid induced mechanical wear or abrasion with corrosion. The general expression erosion-corrosion (E/C) includes all forms of accelerated attack in which protective surface films and/or the metal surface itself are removed by the combination of solution velocity and corrosion, such as impingement attack, cavitation damage and fretting corrosion [1].

Flow accelerated corrosion (FAC) corresponds to erosion (or thinning) of metallic component wall without threshold solution velocity. FAC is a complex phenomenon that depends on many parameters of water chemistry, material composition and hydro-dynamics [1]. FAC can occur in water or steam under single or two-phase conditions.

FAC and E/C are characterized by the constant removal of protective oxide films from the metal surface, ranging from thin invisible passive films to thick visible films of corrosion products [41]. For BWR RPV and its internals both FAC and E/C are very unlikely.

Creep

Creep is the deformation that occurs over a period of time in a material subjected to constant stress, even below the elastic limit. For metallic materials, the creep reaches a significant level when the temperature exceeds 40 % of the absolute melting point [2,16]. Due to thermal activation, materials can slowly and continuously deform even under constant stress, and eventually fail.
The propagation of creep is determined by several competing reactions, including [16]:
- strain hardening,
- softening processes; recovery, re-crystallisation, strain softening and precipitation over ageing,
- damage processes; cavitation and cracking.

Deformation due to creep is usually divided into three regimes: primary, secondary and tertiary. These regimes are controlled by different mechanisms. Components are usually designed to be operated only in the primary and secondary regimes [2,16].

An aggressive environment can have an influence on the creep behaviour and on the degree of damage resulting by it. In metallic materials, an internal oxidation process can occur and accelerate the creep process [2].

**Mechanical wear**

Wear of metals is broadly characterized as mechanically induced or aided degradation mechanism due to contact of two materials. Generally, wear results from concurrent effects of vibration and corrosion [1]. Wear is the loss of material, generally measured as the rate of removal of surface material, caused by the relative motion between adjacent metal surfaces or by the action of hard abrasive particles in contact with a metal surface [3]. In BWR NPPs also sliding friction can occur between moving parts.

As a consequence of the removal of wear particles, fresh surfaces are being created, mostly by plastic deformation in the micro-range. In chemically active environments, chemical reactions with these highly excited surface areas can occur. This leads either to the formation of surface layers which slow down the wear process or to chemical reactions through which the process of material removal is accelerated together with a simultaneous corrosion process [2].

**Interaction of degradation mechanisms**

According to experience, when some degradation mechanisms act simultaneously, they can create a joint effect, which in some cases is even more severe than the arithmetically added result of their separate effects. In these cases, the resulting degradation phenomenon is often quite complex. Due to this, and realising that the interaction of various degradation mechanisms is still a more recent research topic, the physical mechanisms of most interaction phenomena are not clear.

The better known interacting degradation mechanisms are:
- irradiation-creep,
- corrosion-fatigue (environmentally assisted fatigue), and
- creep-fatigue.

**3.3 Observed degradation**

To date the degradation by crack growth of BWR RPV and its internals has been limited to SCC and fatigue in their various forms [1]. Although degradation due to other mechanisms during plant life cannot be discounted, such occurrences would be expected to occur earlier in
the plant life. The field experience and the understanding of relevant degradation mechanisms support this conclusion.

Incidents documented to date indicate that the cracking frequency of reactor internals is increasing, due in part to expanded scope of inspections. Cracking indications are typically found in heat affected zone (HAZ) material adjacent to circumferential welds, although longitudinally orientated cracks have also been observed. Cracking is typically caused by IGSCC and IASCC, and has been observed in all three stainless steel types of BWR core shrouds in worldwide use, i.e. Type 304, 304L, 316L and 347 stainless steels. To date, in three plants with Type 304L stainless steel there has been cracks in the weldments located in the upper half of the core shroud, and in two plants with Type 347 stainless steel there has been cracks in both the upper and lower weldments of the core shroud [1].

The observed cracking degradation history of BWR RPVs and their internals is summarised in the following. As mentioned earlier, operating BWR RPVs and internals have been fabricated by several suppliers. Due to this all BWR RPVs and internals are not identical, but differ supplier specifically in several configuration and material issues to some extent. Figure 2-1 in Section 2 shows an Asea-Atom BWR RPV and some of its main internals.

The cracking history data in the following concerns Swedish and Finnish BWR units, and is mainly from SKI report 02:50 [53]. This report covers cracking occurrences from 1972 to 2000. All these BWR units are designed by Asea-Atom (nowadays Westinghouse) [60]. The cracking history data concerning OL1 and OL2 was taken from ref. [54]. As it is a confidential report, the description of its data here is more general. In addition to using existing documented data, the author of this work visited the Olkiluoto, Forsmark, Oskarshamn and Ringhals NPPs during 2015-2016, to discuss and learn about the possible more recent flaw findings in their BWR units. These discussions are also summarised here.

The considered Nordic BWR units are:
- Barsebäck 1,
- Barsebäck 2,
- Forsmark 1,
- Forsmark 2,
- Forsmark 3,
- Oskarshamn 2,
- Oskarshamn 3,
- Ringhals 1,
- Olkiluoto 1, and
- Olkiluoto 2.

Of these BWR units, Barsebäck 1 and 2 were decommissioned during the first decade of this millennium. However, before that, they had been in operation for about 30 years. As for experienced degradation mechanisms, the Swedish BWR RPVs and their internals have mostly been damaged by IGSCC and IASCC. Thus, the scope here concerns mainly those degradation mechanisms. Table 3.3-1 below shows for Swedish BWR RPV internals the IGSCC and IASCC occurrences detected by the end of 2000. Then, Table 3.3-2 summarizes the corresponding crack findings for the OL1 and OL2 units by the end of 2000. The information concerning more recently detected cracks in the Nordic BWRs is based on the discussions between the author of this work and the experts from the above mentioned four NPPs.
Note, that all crack findings presented in Tables 3.3-1 and 3.3-2 as well as those described by the experts from the above mentioned four NPPs have been fully repaired or the component replaced.

Table 3.3-1. For Swedish BWR RPV internals, the IGSCC and IASCC occurrences detected by the end of 2000 [53].

<table>
<thead>
<tr>
<th>Component</th>
<th>No. of detected cracks</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core shroud lid bracket</td>
<td>45</td>
<td>Ni-based X-750</td>
</tr>
<tr>
<td>RPV head spring beams and brackets</td>
<td>19</td>
<td>Ni-based Alloy 182</td>
</tr>
<tr>
<td>Bolt, nut</td>
<td>14</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core shroud lid support</td>
<td>13</td>
<td>Ni-based X-750</td>
</tr>
<tr>
<td>Steam separator</td>
<td>5</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core grid support</td>
<td>3</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core shroud</td>
<td>1</td>
<td>stabilized stainless steel</td>
</tr>
<tr>
<td>Feedwater sparger</td>
<td>1</td>
<td>stainless steel</td>
</tr>
</tbody>
</table>

Table 3.3-2. For OL1/OL2 RPV and internals, general summary of the crack findings by the end of 2000 [54].

<table>
<thead>
<tr>
<th>Component</th>
<th>Cause of cracks</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core grid</td>
<td>IASCC</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Spring beams</td>
<td>IGSCC</td>
<td>Bofors CRO684</td>
</tr>
<tr>
<td>Spring beam support brackets</td>
<td>IGSCC</td>
<td>nickel-base alloy</td>
</tr>
<tr>
<td>RPV flange</td>
<td>hot cracks</td>
<td>ferritic steel</td>
</tr>
<tr>
<td>Steam separator outlet</td>
<td>IGSCC</td>
<td>stainless steel</td>
</tr>
</tbody>
</table>

The first discussions on more recent cracking was with the OL1/OL2 experts. For that purpose the author of this work spent three days in the Olkiluoto NPP site in December 2015. In addition to discussions in the meetings, confidential documents on detected cracking were made available for the duration of the visit. The program of the visits to the three Swedish NPPs was similar.

According to TVO expert [55] and associated confidential documentation, cracks have been detected from the OL1/OL2 RPV and internals during 2000-2016 from:
- Core shroud lifting lug,
- New feedwater spargers,
- Feedwater nozzle to safe-end weld,
- Core spray nozzle to safe-end weld,
- Flange cooling nozzle/pipe weld,
- Steam dryer welds for steam guide plates, and
- Steam dryer outlet packages.

Next discussions on more recent cracking were carried out with the Forsmark NPP experts. For that purpose, the author of this work spent two days in the Forsmark NPP site in May 2016. According to the Forsmark expert [56] and associated confidential documentation, cracks have been detected from F1/F2/F3 RPV and internals during 2000-2016 from:
- Spring beam support brackets,
- Flange cooling spray piping,
• Long nozzle pipes in cooling spray piping,
• Steam dryer support beams,
• Inner roof plates of the steam dryer,
• Core shroud support leg,
• Control rods,
• Control rod blades, and
• Water level measurement nozzle/PRV weld.

For discussions on more recent cracking with the Oskarshamn NPP experts, the author of this work spent one day in the Oskarshamn NPP site in November 2016. According to the Oskarshamn expert [57] and associated confidential documentation, cracks have been detected from the RPVs and internals of the OKG1/OKG2/OKG3 NPP units during 2000-2016 from:
• Long nozzle pipes in cooling spray piping,
• Spring beam support brackets,
• Steam separator,
• Core shroud lid,
• Feedwater nozzle,
• J-groove welds of small nozzles,
• Control rods,
• Core shroud shell welds, and
• Core shroud support legs.

For discussions on more recent cracking with the Ringhals NPP experts, the author of this work spent one day in the Ringhals NPP site in December 2016. According to the Ringhals expert [58], associated confidential documentation and SKI Report 2004:16 [59], cracks have been detected from the RPV and internals of the RAB1 NPP unit during 2000-2016 from:
• Core shroud lid,
• Water level measurement nozzle,
• Core spray nozzle,
• Other nozzles manufactured of Alloy 600 and welded with Alloy 182.

Table 3.3-3 presents a summary on the degradation experience concerning BWR RPV components important to safety. Note, that Asea-Atom BWR NPPs do not have jet pumps. These pumps are relevant mainly/only for BWR units by General electric.
Table 3.3-3. Summary on degradation experience concerning BWR RPV internals important to safety [1].

<table>
<thead>
<tr>
<th>Component</th>
<th>Degradation mechanism</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Core Plate</td>
<td>IGSCC</td>
</tr>
<tr>
<td>2. Core Spray Internal Piping</td>
<td>IGSCC</td>
</tr>
<tr>
<td>3. Core Spray Sparger</td>
<td>IGSCC</td>
</tr>
<tr>
<td>4. CRD Guide Tube</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>5. CRD Housing</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>6. In-Core Housing</td>
<td>IGSCC</td>
</tr>
<tr>
<td>7. Jet Pump</td>
<td>• Diffuser IGSCC</td>
</tr>
<tr>
<td></td>
<td>• Hold-down beam IGSCC</td>
</tr>
<tr>
<td></td>
<td>• Inlet mixer Fatigue due to improper installation</td>
</tr>
<tr>
<td>8. LPCI Coupling</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>9. Orificed Fuel Support</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>10. Core shroud</td>
<td>IGSCC/IASCC</td>
</tr>
<tr>
<td>11. Shroud Support</td>
<td>IGSCC</td>
</tr>
<tr>
<td>12. Top Guide</td>
<td>IGSCC/IASCC</td>
</tr>
</tbody>
</table>
4 Survey on and modelling of degradation mechanisms affecting BWR RPV and its internals

This section describes modelling of and susceptibility to significant degradation mechanisms that affect BWR RPV and its internals, and evaluates the potential significance of their effects on the continued performance of safety functions of the considered components throughout the plant service life.

The survey on computational modelling of and susceptibility to degradation mechanisms mainly follows the most recent and/or relevant reports on the issue by IAEA [1,2], EPRI [3,4,5], U.S. NRC [6,7,8] and Swedish Radiation Safety Authority (SSM) [9]. In addition, also relevant journal articles, conference papers, academic theses, dissertations, handbooks and technical reports publicly available are covered.

National codes and standards, such as ASME Code from the U.S., provide computational models for irradiation embrittlement, SCC and fatigue. For other relevant degradation mechanisms, computational models are seldom presented in the national codes and standards, due to which they are taken from fitness-for-service handbooks and reliable technical reports, such as those by EPRI.

Degradation mechanisms are specific processes that gradually change characteristics of a component with time and use. Ageing degradation concerns those cumulative changes that can impair the ability of a component to function within acceptance criteria. Service conditions outside prescribed limits can accelerate the rate of degradation.

Survey of age related degradation mechanisms is here based on BWR service experience, laboratory data, and relevant experience from other industries, where applicable.

The following significant degradation mechanisms, as according to ref. [1], will be considered in the following:

- irradiation embrittlement,
- thermal embrittlement,
- fatigue,
- stress corrosion cracking (SCC),
- irradiation accelerated SCC (IASCC),
- general corrosion,
- erosion-corrosion, flow accelerated corrosion (FAC), and
- mechanical wear.

The information on irradiation embrittlement is described in more detail than that concerning other covered degradation mechanisms. This is because it relates most to the main argument of the dissertation, i.e. extension of operational lifetime of a BWR RPV to at least 80 years. Of the degradation mechanisms listed in Table 3.1-1 in Section 3.1, creep is not described further here. This is because under operational BWR temperatures its effect is practically negligible. However, it is possible that creep would have some effect later, during the long-term operation (LTO) period. As there still is no evidence concerning this for BWR RPVs and internals, this issue is excluded here.
4.1 Irradiation embrittlement

The significance of irradiation embrittlement for a given component depends on the probability of cracking, the loading of the component and the degree of irradiation embrittlement [17]. The main degrading effect of irradiation embrittlement is the decrease in material fracture toughness. The physical phenomena concerning irradiation embrittlement are summarised in Section 3.2.

4.1.1 Computational modelling of irradiation embrittlement

The majority of procedures developed within various surveillance programs to assess irradiation embrittlement correlate the shift of the material specific reference nil-ductility temperature, \( RT_{NDT} \), from ductile to brittle with different parameters. These include neutron fluence, irradiation temperature, chemical composition, neutron flux, neutron spectrum and microstructural state. These procedures incorporate both physically motivated features and empirical calibration. This semi-empirical nature means that the equations are not applicable to all materials. Here the computational modelling of irradiation embrittlement concerns only ferritic steels.

According to the U.S. ASME code approach documented in Regulatory Guide 1.99, Rev 2 [18], \( RT_{NDT} \) is defined as the shift in the 41 J impact energy transition temperature, \( TK_{41J} \). It is assumed that the true static fracture toughness shift and crack arrest shift is equal to or less than \( \Delta TK_{41J} (\Delta RT_{NDT}) \). An additional material specific margin in the determination of \( RT_{NDT} \) is also required. For welds the margin is 31 °C, and for base metals it is 19 °C [18]. In both cases, the margin is not more than \( \Delta RT_{NDT}/2 \). This margin is added to the experimentally measured or computed \( \Delta RT_{NDT} \) value.

The 41 J transition temperature is measured with Charpy V-notch (CVN) test and the obtained upper shelf energy (USE) is an indirect measure of the ductile initiation fracture toughness at higher temperature upper shelf levels [70].

When credible surveillance data from the reactor in question is not available, the adjusted \( RT_{NDT} [^\circ C] \) for each material in the RPV beltline is to be calculated as [18]:

\[
RT_{NDT} = RT_{NDT, initial} + \Delta RT_{NDT} + \text{margin}
\]  

(4.1.1-1)

where \( RT_{NDT, initial}[^\circ C] \) is the reference temperature for the unirradiated material, as defined in Paragraph NB-2331 of ASME Section III [147]. If the measured values of initial \( RT_{NDT} \) for the material in question are not available, generic mean values for that class of material can be used.

The parameter \( \Delta RT_{NDT}[^\circ C] \) is the mean value of the adjustment in reference temperature caused by irradiation. The definition of it can be written as [18]:

\[
\Delta RT_{NDT} = (CF) f^{(0.28 - 0.10 \log(f_{adj}))}
\]  

(4.1.1-2)

where \( CF[^\circ C] \) is the chemistry factor, \( f[\times 1.0E-19 \text{ n/cm}^2] \) is the accumulated neutron fluence (for \( E > 1.0 \text{ MeV} \)) and \( f_{adj} = 1.0 \text{ n/cm}^2 \). RG 1.99 [18] contains tabulated \( CF \) data for both welds and base metals. Even when \( \Delta RT_{NDT} \) is calculated with the chemistry factor it is required to use the additional safety margin prescribed in RG 1.99 [18]. When two or more credible surveillance data sets are available \( CF \) is to be determined from the data by least square fitting.
The margin in equation (4.1.1-1) is the quantity that is to be added to obtain conservative, upper bound values of adjusted reference temperature for the calculations required by Appendix G of 10 CFR Part 50 [118]. The definition of the margin [°C] can be written as:

\[
\text{margin} = 2 \left( \frac{\sigma_I}{\sigma_{I,adj}} \right)^2 + \left( \frac{\sigma_\Delta}{\sigma_{\Delta,adj}} \right)^2
\]

(4.1.1-3)

where \( \sigma_I \) is the standard deviation of the initial \( RT_{NDT} \), \( \sigma_\Delta \) is the standard deviation of \( \Delta RT_{NDT} \), and \( \sigma_{I,adj} = \sigma_{\Delta,adj} = 1 \text{ °C} \). The values for \( \sigma_I \) and \( \sigma_\Delta \) are given in RG 1.99 [18] for both base metal and welds.

RG 1.99 [18] considers also the decrease of accumulated fluence through the wall thickness, i.e. attenuation. This can be written as follows:

\[
f = f_{surf} \exp \left( \frac{-0.24(25.4 \times x)}{x_{adj}} \right)
\]

(4.1.1-4)

where \( f_{surf} \) is the calculated value of the neutron fluence at the inner wetted surface, \( x \text{ [mm]} \) is the depth coordinate through the wall with origin at inner surface and \( x_{adj} = 1 \text{ mm} \). The procedures of RG 1.99 [18] are based on only 177 data points and on the incomplete understanding of embrittlement mechanisms that was available in the 1980's [78]. By scope the procedure of ref. [18]:

- applies to U.S. RPV base materials and to their welds,
- is valid as such for a nominal irradiation temperature of 288 °C, below 274 °C embrittlement is greater, and above 310 °C less embrittlement occurs, with correction factor used for justification by reference to actual data.

In the French RSE-M code [61], there are two equations for the \( RT_{NDT} \) shift of ferritic RPV base material. These are FIM and FIS equations, as derived by Brillaud et al. [62], and they are based on extensive test reactor experiments. The FIM equation for \( \Delta RT_{NDT} \text{ [°C]} \) can be written as:

\[
\Delta RT_{NDT} = \left[ 17.3 + 1537 \left( \frac{P}{P_{adj}} - 0.008 \right) + 238 \left( \frac{Cu}{Cu_{adj}} - 0.08 \right) + 191 \left( \frac{Ni}{Ni_{adj}} \right)^2 \left( \frac{Cu}{Cu_{adj}} \right) \right] \times \left( \frac{f}{f_{adj}} \right)^{0.35} \times (1 \text{ °C})
\]

(4.1.1-5a)

whereas the original FIS equation can be written as:

\[
\Delta RT_{NDT} = \left[ 8 + 24 + 1537 \left( \frac{P}{P_{adj}} - 0.008 \right) + 238 \left( \frac{Cu}{Cu_{adj}} - 0.08 \right) + 191 \left( \frac{Ni}{Ni_{adj}} \right)^2 \left( \frac{Cu}{Cu_{adj}} \right) \right] \times \left( \frac{f}{f_{adj}} \right)^{0.35} \times (1 \text{ °C})
\]

(4.1.1-5b)
where \( P, Cu \) and \( Ni \) are the quantities of phosphorus, copper and nickel in units of weight percentage [weight-%] and \( P_{adj} = Cu_{adj} = Ni_{adj} = 1.0 \) weight-%. These two equations are essentially the same, but whereas the FIM equation is a direct best fit to data, the FIS equation is more conservative, from a safety point of view.

The French approach differs from the ASME methodology. The \( RT_{NDT} \) shift is defined as the shift in the 56 J impact energy transition temperature, \( TK_{56J} \) [°C], or the shift in the 0.9 mm lateral expansion transition temperature, \( TK_{0,9mm} \) [°C], whichever is greater. The lateral expansion corresponding to a certain energy level is affected by the material yield strength. Increasing the yield strength makes plastic deformation of the specimen more work demanding. Therefore, \( \Delta RT_{NDT} \) is generally controlled by \( \Delta TK_{0,9mm} \). The French approach does not apply additional safety margins. The French codes [63,64] allow the fluence dependence of \( \Delta RT_{NDT} \) to be determined experimentally, but do not give any recommendations for the type of expression to be used.

FIM and FIS equations were developed in the late 1980’s. To incorporate all French surveillance data obtained since then and to take better into account the high fluence region, Todeschini et al. [65] developed an updated \( RT_{NDT} \) shift equation.

The German Nuclear Safety Standards KTA 3201.2 [66] and KTA 3203 [67] require that the USE of the RPV base material must remain above 68 J during operation. In the KTA 3203 standard [67], the \( RT_{NDT} \) shift of the ferritic RPV base material is expressed as a temperature shift limit, \( RT_{\text{limit}} \) [°C], instead of a predictive equation. This model is very conservative and will be included only as an inspection whether the determined temperature transition shifts exceed it or not.

As for the chemistry factor \( CF \), the European approaches also apply that concept to determine the irradiation shift directly from the steel chemistry. The French codes contain chemistry factor equations which are dependent on \( P, Cu \) and \( Ni \).

The Japan Electric Association (JEAC) describes in standard JEAC 4201-2000 [68] a method to calculate the \( RT_{NDT} \) shift caused by neutron irradiation throughout the service life of the RPV materials. It is calculated separately for base and weld materials. These equations recognize the effects of \( P, Cu \) and synergism between \( Cu \) and \( Ni \). The equation for base material can be written as:

\[
\Delta RT_{NDT} = \left[ -16 + 1210 \left( \frac{P}{P_{adj}} \right) + 215 \left( \frac{Cu}{Cu_{adj}} \right) + 77 \sqrt{\frac{Ni}{Ni_{adj}}} \right] \left( \frac{Cu}{Cu_{adj}} \right) \\
\times \left[ \left( \frac{f}{f_{adj}} \right)^{0.29-0.04 \log(f/f_{adj})} \right] \times (1 \degree C)
\]  

(4.1.1-6a)

The equation for weld material can be written as:
\[
\Delta R_{NTD} = \left[ -26 - 24 \left( \frac{Si}{Si_{adj}} \right) - 61 \left( \frac{Ni}{Ni_{adj}} \right) + 301 \sqrt{\left( \frac{Ni}{Ni_{adj}} \right) \left( \frac{Cu}{Cu_{adj}} \right)} \times \left( \frac{f}{f_{adj}} \right)^{0.25 - 0.10 \log} \right] \times (1 \, ^\circ C)
\]

(4.1.1-6b)

where \(Si\) is the quantity of silicon in units of weight percentage [weight-%] and \(Si_{adj} = 1.0\) weight-%. No thresholds are explicitly given for equations (4.1.1-6). The embrittlement correlation equations of JEAC4201-2000 [68] were replaced in JEAC4201-2007 [69] by a new embrittlement correlation method proposed by Soneda et al. [70]. The development of this correlation was needed partly because a lot of new surveillance data had been accumulated, but the major reason was the large underestimation of the surveillance data as predicted by the JEAC4201-2000 [68] method in high-copper base metals irradiated at very low flux conditions in BWRs. It was concluded that this large amount of embrittlement is due to the effect of low flux irradiation, which was not considered earlier. Based on results from microstructural characterization of base metals and weld metals several other important conclusions were also made. All these conclusions were incorporated to a relatively large set of time dependent equations describing the microstructural evolution as well as the \(RT_{NDT}\) shift. These complicated equations can only be solved with a computer code. After the development of the JEAC4201-2007 method [69] some new surveillance data were generated, particularly at very high fluences in PWR plants. Half of the data was well predicted by the JEAC4201-2007 method [69] but the remaining half was under predicted. Due to this discrepancy the model coefficients were calibrated against the updated surveillance data. The revised embrittlement correlation method was adopted as 2013 addendum to JEAC4201-2013 [73].

More recent advanced models for \(RT_{NDT}\) shift of ferritic RPV base material are described here without equations due to their large number and complexity.

Due to better understanding of the irradiation embrittlement processes and broader databases of surveillance results, new models for the \(RT_{NDT}\) shift have been and are being developed. The E 900-02 model, also known as the Regulatory Guide 1.99 Rev. 3, was introduced in 2002 and is recommended by the American Society for Testing and Materials (ASTM) [74]. This model considers two degradation mechanisms, which are stable matrix damage and copper rich precipitations. This \(RT_{NDT}\) shift model is more accurate than the RG 1.99, Rev 2 [18] model and other earlier models.

The EONY model has been developed by Eason, Odette, Nanstadt and Yamamoto [75,76], which effort was motivated by further surveillance data obtained during 2003-2004, both from BWRs and PWRs. To be able to represent correctly the data from BWRs, for which the fast flux at the RPV wall is substantially lower than that in PWRs, the notion of an effective fluence was introduced. This model is also limited to consider stable matrix damage and copper rich precipitations. This \(RT_{NDT}\) shift model is more accurate than the RG 1.99, Rev 2 [18] model and other earlier models.

The wide range embrittlement trend curve by Kirk [77] is based on larger amount of experimental data than the models described above. This approach differs from the others by applying good representations of empirical trends instead of a physically guided mathematical form. The model considers the threshold values for minimum and maximum copper contents as well as neutron fluence and threshold fluence for embrittlement to occur. The forms of two
significant model terms were determined by regression analysis of the copper threshold values estimated at different fluences. This $RT_{NDT}$ shift model is more accurate than the RG 1.99, Rev. 2 [18] model and other earlier models.

None of the $RT_{NDT}$ shift models presented above consider any secondary or parallel processes, such as late blooming phase (LBP). In addition, since these models are developed using specific databases and materials, test results from significantly different physical conditions may not agree well with predictions. For instance, materials with a composition very different from those in the database should not be expected to behave accordingly. Internationally there is ongoing research on the significance of the LBP to the $RT_{NDT}$ shift and associated models. The results from these research efforts are not yet available.

Eason, Odette, et al. published in 2013 a physically based correlation for irradiation induced transition temperature shifts for RPV steels [78]. In terms of fluence this correlation is valid up to 5.0E+19 n/cm², and is thus applicable also to design lifetime of 40 years for BWR plants and beyond. The 2013 publication is a shorter presentation of the model described originally in a more detailed project report published in 2007 [79]. The correlation consists of two terms, which correspond to stable matrix features (SMF) and Cu rich precipitates (CRP). The correlation has been fitted to data from U.S. NPP RPVs. The applicability of the correlation for BWR plants is further enhanced by its capability to capture also the low flux conditions of BWRs. This correlation does not specifically take into account the possible LBP. The Eason, Odette, et al. model [78] is now included also in the ASME code, see 2015 Edition of ASME Section XI [81]. By scope the procedure of ref. [78]:

- apply to U.S. RPV base materials and to their welds,
- is based on 855 data points,
- is valid as such for a nominal irradiation temperature from 272 to 299 °C,
- is valid for fluences from $9.26 \times 10^{15}$ to $7.13 \times 10^{19}$ n/cm², and
- is valid for fluxes from $1.81 \times 10^8$ to $9.71 \times 10^{11}$ (n/cm²)/s.

By range of validity the Eason, Odette, et al. model [78] is applicable to the RPVs of the Asea-Atom BWR units, such as OL1 and OL2.

Odette and Yamamoto developed also in 2013 two physically based RPV steel irradiation embrittlement prediction models [80] applicable to BWR plants. They take into account the dose rate effect, radiation enhanced diffusion and precipitation as well as low flux. These models are:

- Two feature solute trap enhanced recombination model (2FM), and
- Three feature model (3FM) with additional unstable matrix defects (UMD) hardening and sinks.

These RPV steel irradiation embrittlement models have been fitted against U.S. NPP RPV embrittlement database (PREDB) data, of which 99 % is for fluences less than 5.0E+19 n/cm². Thus, the 2FM and 3FM models are less accurate for high fluences. 2FM model is applicable to fluence levels expected in the currently planned reactor lifetime and flux levels of up to 1.0E+12 n/(cm²)s. The model assumes that dose rate effects are solely due to the flux dependence of radiation enhanced diffusion (RED). The 2FM model considers two hardening features, which again are SMF and CRP. The 3FM model is an update of the original one that was developed in 1990’s to treat the effects of high flux test reactor irradiation. In addition to SMF and CRP, 3FM model considers also the effect of unstable matrix defects (UMD). Due to their effect, increasing flux can increase, decrease, or leave unaffected hardening and embrittlement, depending on the alloy composition, irradiation temperature, flux and fluence.
The description of the 3FM model on irradiation hardening and embrittlement is extensive enough but still approximate. In reality, there is no sharp delineation between UMD, SMF and CRP, especially at high flux. Such features also implicitly interact with one another by mechanisms, such as competition for finite numbers of solute atoms and excluded volume effects. In effect, the 3FM model only applies to very high flux test reactor data. The 2FM model is the foundation of the regulatory approach in the U.S. and it more than covers BWRs even for extended life [82]. The 2FM and 3FM models do not specifically take into account the LBP.

The $RT_{NDT}$ shift models in Regulatory Guide 1.99, Rev 2 [18], French RSE-M code [61] and German KTA 3203 standard [67] are the most conservative ones. They are based on relatively small amount of experimental data. On the other hand, the $RT_{NDT}$ shift model in Japanese code JEAC4201-2000 [68] gives underestimating results for low fluxes. The thoroughly updated Japanese $RT_{NDT}$ shift model in JEAC4201-2013 [73] gives much more realistic results. In general, most recent $RT_{NDT}$ shift models give also most accurate results. These include the EONY model [75,76], the wide range embrittlement trend curve by Kirk [77] and the model by Eason, Odette, et al. [78]. This is because they take into account more significant affecting variables than the earlier models and because they are based on a much larger amount of experimental data. Arguably, the most realistic $RT_{NDT}$ shift model is presently that by Eason, Odette, et al. [78]. This model is also best applicable to the RPVs of the Asea-Atom BWR plants.

The change of fracture toughness due to irradiation is commonly defined through the shift in $RT_{NDT}$. Appendix A of ASME Section XI [81] presents for ferritic RPV base material definitions of critical fracture toughness for the lower bound crack initiation value, $K_{Ic}$ [MPa\cdot\text{m}^{1/2}] and for the lower bound crack arrest value, $K_{Ia}$ [MPa\cdot\text{m}^{1/2}]. The effects of irradiation on the crack initiation and arrest fracture toughness can be estimated by applying the shift in the $RT_{NDT}$ to shift the ASME lower bound curves by moving the curves by the same shift amount, but leaving the shapes unaltered. These relationships can be written as [81]:

$$K_{Ic} = \left(36.5 + 22.783 \exp \left[0.036 \left( \frac{T - RT_{NDT}}{T_{adj}} \right) \right] \right) \times \left(1 \text{ MPa}\cdot\text{m}^{1/2} \right)$$

(4.1.1-7a)

$$K_{Ia} = \left(29.4 + 13.675 \exp \left[0.0261 \left( \frac{T - RT_{NDT}}{T_{adj}} \right) \right] \right) \times \left(1 \text{ MPa}\cdot\text{m}^{1/2} \right)$$

(4.1.1-7b)

where $T_{adj} = 1.0$ °C. The upper limit value for both $K_{Ic}$ and $K_{Ia}$ is 220 MPa\cdot\text{m}^{1/2} [83]. For LWR plants the decrease of the upper shelf value of both $K_{Ic}$ and $K_{Ia}$ as a function of fluence can be assessed by using the simple procedure in ref. [83]. This procedure is a correlation as based on measured data.

According to the KTA 3201.2 [66], the fracture toughness during operation is determined with the given fracture toughness curve by using the adjusted reference temperature. This temperature may be determined either according to the reference temperature $RT_{NDT}$ concept or the Master Curve concept. The latter concept has been developed in VTT by Wallin, see e.g. refs. [84,85,86]. The Master Curve concept is applicable to the RPVs of the Asea-Atom BWR plants.
According to the RCC-M code [63] the fracture toughness during operation is determined with the given fracture toughness curve by using the adjusted reference temperature. Again, the adjusted reference temperature may be determined either according to the reference temperature $RT_{NDT}$ concept or the Master Curve concept.

The Master Curve concept together with some modifications is described in the following. The scatter of fracture toughness in the transition region of low alloyed ferritic RPV steels can be modelled with Weibull statistics. This approach, proposed by Wallin [84] and implemented in Test Standard ASTM E1921-05 [86], characterizes the median $J$-Integral based fracture toughness, $K_{JC}$, of ferritic steels at the onset of cleavage cracking. It uses a concept of universal temperature dependence of fracture toughness in the transition region, which is called the Master Curve. It is defined as the median fracture toughness of a test specimen, adjusted to a crack front length of 25.4 mm (1 inch).

The temperature dependence of the fracture toughness $K_{JC}$ [MPa $\sqrt{m}$] according to the Master Curve can be written as [86]:

$$K_{JC} = \left\{ 30 + 70 \exp \left( 0.019 \left( \frac{T - T_0}{T_{adj}} \right) \right) \right\} \times (1 \text{ MPa} \sqrt{m})$$  \hspace{1cm} (4.1.1-8)

where the unit of the reference fracture toughness transition temperature $T_0$ is °C. $T_0$ corresponds to the temperature at which $K_{JC} = 100$ MPa $\sqrt{m}$. This simple equation, which describes the shape of the median Master Curve, is assumed to be constant for all ferritic steel types, fluence levels and annealing heat treatments.

Instead of indirect CVN tests, the measurement of actual fracture toughness of irradiated small surveillance specimens has become possible using elastic-plastic fracture mechanics methods, primarily employing the $J$-integral measure of toughness. The ductile Master Curve reference temperature $T_0$ can be determined using a small number of test specimens and the $J$-integral resistance ($J$-$R$) curve can be measured for assessment of ductile fracture initiation and tearing resistance [70].

Two more analysis procedures have been developed for a material that contains randomly distributed macroscopic inhomogeneities. These are the fitness-for-service assessment procedure SINTAP [87] and the multi-modal (MM) method [88,89]. SINTAP contains a lower tail modification of the Master Curve analysis which enables conservative lower bound type fracture toughness estimates. In the MM approach, the total dataset is presumed to be composed of several subsets (populations), even up to infinitely many. Each subset follows the Master Curve distribution but has a different $T_0$. The combined distribution is fully defined by two parameters: the mean reference temperature of all populations, $T_m$, and the standard deviation around the mean $\sigma_{Tm}$ [90].

There exists a quite large number of correlations for determining the effect of neutron irradiation to fracture toughness of ferritic RPV base materials. For collections of relevant correlations, see e.g. IAEA report No. NP-T-3.11 [74] and article PVP2014-28540 [91].

The materials of the BWR RPV internals are mainly of austenitic stainless steel and nickel-based alloys. For the former material type report NUREG/CR-7027 [126] presents for ductile fracture toughness $J_{lc}$ a disposition curve that bounds the existing experimental data. The trend curve takes into consideration: (a) a threshold neutron exposure for irradiation embrittlement
of austenitic stainless steels and a minimum fracture toughness for these materials irradiated to less than the threshold value, (b) a saturation neutron exposure and a saturation fracture toughness for materials irradiated to greater than this value, and (c) a description of the change in fracture toughness between the threshold and saturation neutron exposures. The fracture toughness $J_{lc}$ [kJ/m$^2$] can be written as [126]:

$$J_{lc} = \left\{ 7.5 + 110 \exp\left[ -0.35\left( f/f_{adj}\right)^4 \right] \right\} \times \left( 1 \text{ kJ/m}^2 \right)$$

(4.1.1-9)

where the unit of the fluence $f$ is now dpa and $f_{adj} = 1.0$ dpa. The lower bound trend curve given by equation (4.1.1-9) is consistent with the EPRI lower bound model proposed for PWR plants. The MRP model for fracture toughness is expressed in terms of a lower bound $K_{lc}$ curve, where $K_{lc}$ is the equivalent critical stress intensity factor. MRP stands for Materials Reliability Program. Which works within/through EPRI. This model bounds all the fracture toughness data from the fast reactors, BWRs, and PWRs as a function of the neutron dose. $K_{lc}$ [MPa$\sqrt{m}$] can be written as [129]:

$$K_{lc} = \left\{ 80 - 142\left[ 1 - \exp\left( f/f_{adj}\right) \right] \right\} \times \left( 1 \text{ MPa}\sqrt{\text{m}} \right)$$

(4.1.1-10)

where the unit of fluence $f$ is again dpa and $f_{adj} = 1.0$ dpa. As for change of material strength properties of austenitic stainless steels due to irradiation, reports NUREG/CR-7027 [126] and MRP-135-Rev.1 [127] present for types 304 and 316 steels fluence dependent correlations for the yield strength and the ultimate tensile strength, respectively.

The earliest fracture toughness models are also the most conservative ones. These include the models in Appendix A of ASME Section XI [81] and in the RCC-M code [63]. There are several such earlier models. The commonly used ones are listed in IAEA report No. NP-T-3.11 [74] and article PVP2014-28540 [91]. Arguably, presently the most realistic and accurate development is the Master Curve concept by Wallin [84, 86]. This is because it is based on measuring the reference temperature $T_0$ from the test specimens instead of indirect CVN tests. With the Master Curve even a small number of test specimens suffices. Moreover, the Master Curve concept is the first fracture toughness model that takes into account the scatter of fracture toughness in the transition region. It is to be kept in mind that the fracture toughness models in question concern only low alloy RPV base materials.

### 4.1.2 Susceptibility to irradiation embrittlement

The scope in the following covers both low alloy and carbon steels as well as austenitic stainless steels. Low alloy and carbon steels do exhibit a sharp ductile to brittle transition behaviour, whereas wrought austenitic stainless steels do not. The toughness losses of the latter steels due to irradiation tend to accumulate with increasing fluence and saturate at levels > 1.0E+25 n/m$^2$. Information in ref. [19] describes the results of a fracture toughness study performed on irradiated reactor internal material of type 304 stainless steel taken from operating BWRs with fluences ranging from 1.0E+25 to 6.0E+25 n/m$^2$ ($E > 1$ MeV). That study confirmed a fracture toughness saturation level of 55 MPa$\sqrt{m}$ for all fluences considered, and can be directly applied to the evaluation of highly irradiated RPV internals [1].

Sparse or non-existent data at high fluences accumulated over long times create uncertainties in irradiation embrittlement predictions [94]. This issue relates to the extension of operational life to 60 and 80 years. More recent experimental evidence shows that late blooming phase (LBP) could produce a significant degradation effect to RPV base material after 40 years of
plant operation. This phase concerns the behaviour of nickel, manganese and silicon after accumulation of high fluence, and can result with unanticipated drop in $RT_{NDT}$ [170].

Table 4.1.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to irradiation embrittlement.

Table 4.1.2-1. Summary on susceptibility of materials of BWR RPV and its internals to irradiation embrittlement [1,3,18,130]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X • affects only within a few meters from reactor core,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X • for ferritic steels, the decrease of fracture toughness as a function of fluence does not saturate,</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>• for austenitic stainless steels, the decrease of fracture toughness as a function of fluence saturates between 5 and 10 dpa,</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X • see Section 4.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X • see Section 4.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X • see Section 4.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X • see Section 4.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X • see Section 4.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
</tbody>
</table>

4.2 Thermal embrittlement

Thermal embrittlement causes changes in microstructure and material properties. They are the consequence of diffusion processes as indicated by a decrease in fracture toughness but as an increase in the yield strength and the tensile strength [1]. The physical phenomena concerning thermal embrittlement are summarised in Section 3.2. For a more detailed description on the phenomenology of thermal embrittlement, see Appendix A.2.2.

4.2.1 Computational modelling of thermal embrittlement

There are not too many procedures for computational assessment of thermal embrittlement (or thermal ageing) of metallic NPP materials. These procedures are material type specific correlations, as based on fitting to laboratory results. Here the computational modelling of thermal embrittlement concerns only ferritic steels.

As for national codes, assessment of material degradation of RPV base material due to thermal embrittlement is considered in two French codes, namely 2012 edition of RCC-M [63] and 2010 edition of RSE-M [95]. According to 2012 edition of RCC-M [63] the $RT_{NDT}$ of thermally aged RPV steels and associated welds, $RT_{NDT,aged} \, [\text{°C}]$, is computed as:

$$RT_{NDT,aged} = RT_{NDT,initial} + \Delta RT_{NDT,aging}$$  \hspace{1cm} (4.2.1-1)

The shifts $\Delta RT_{NDT,aging} \, [\text{°C}]$ for base metal and HAZ are shown in Table 4.2.1-1 below. The accuracy of this simple procedure may not be very good, but it is used to compute the $RT_{NDT}$ of thermally aged RPV steels and associated welds because there are no better procedures available.
The 2010 edition of RSE-M [95] contains predictive equations to estimate $RT_{NDT}$ after 40 years of operation. The equations are given for base metal, HAZ under cladding and weld metal.

Table 4.2.1-1. Reference transition shifts $\Delta RT_{NDT,\text{ageing}}$ for RPV materials due to thermal ageing [63]. Here $P$ means phosphorus.

<table>
<thead>
<tr>
<th>Base metal $\Delta RT_{NDT,\text{ageing}}$ [ºC]</th>
<th>HAZ $\Delta RT_{NDT,\text{ageing}}$ [ºC]</th>
</tr>
</thead>
<tbody>
<tr>
<td>300 ºC</td>
<td>325 ºC</td>
</tr>
<tr>
<td>P ppm</td>
<td>40 years</td>
</tr>
<tr>
<td>40</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>0</td>
</tr>
<tr>
<td>60</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>0</td>
</tr>
<tr>
<td>80</td>
<td>4</td>
</tr>
<tr>
<td></td>
<td>0</td>
</tr>
</tbody>
</table>

Chopra [122] has developed a procedure for estimating mechanical properties, i.e. $Cv$ [J/cm$^2$] and elastic-plastic fracture toughness, of thermally aged cast stainless steel (CASS) piping components. Once the value of $Cv$ at saturation is known the service time fracture toughness curve can be determined for known service history by using the correlations by Chopra, published originally in report NUREG/CR-4513, Rev. 1 [123] in 1994 and then as updated in report NUREG/CR-7185 [130] in 2015.

However, there are very few BWR RPV internals of CASS. For the BWR RPV base materials the thermal embrittlement correlation in RCC-M [63] and RSE-M [95] is arguably the most applicable.

### 4.2.2 Susceptibility to thermal embrittlement

Thermal embrittlement is not a significant degradation mechanism for RPV internals made of wrought steel or Ni-Cr-Fe alloys. Neither it is significant for the orificed fuel supports which are made of CASS because stress levels are low. These stress levels are not of sufficient magnitude to cause cracking of the orificed fuel support, irrespective of the delta ferrite content. Therefore, thermal embrittlement is not a significant degradation mechanism for any RPV internals [1].

Table 4.2.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to thermal embrittlement.

Table 4.2.2-1. Summary on susceptibility of materials of BWR RPV and its internals to thermal embrittlement [1,3,110,135]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td></td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>
4.3 Fatigue

Fatigue is defined as the structural deterioration that occurs as a result of repeated stress/strain cycles caused by fluctuating mechanical loads and temperatures or vibration loads. After repeated cyclic loading microstructural damage can accumulate, leading to macroscopic crack initiation at the most highly affected locations. Subsequent continued cyclic loading can lead to growth of an initiated crack [1].

4.3.1 Computational modelling of fatigue

There are several approaches for computational modelling of fatigue. Since the introduction of damage accumulation concept by Palmgren [136] and linear damage rule by Miner [137], both published several decades ago, a multitude of cumulative fatigue damage models have been developed. These models can be divided into six categories [138]:

- linear damage rules,
- non-linear damage curves and two-stage linearisation approaches,
- life curve modification methods,
- approaches based on crack growth concepts,
- continuum damage mechanics models, and
- energy based theories.

In the top level the fatigue degradation models can be divided into:

- procedures that model fatigue until a macroscopic flaw has initiated,
- procedures that model the growth of an initiated flaw.

4.3.2 Fatigue crack initiation models

Of the procedures that model fatigue until initiation of a macroscopic flaw, the linear damage rules are most commonly used. This approach is described in the following.

The Palmgren-Miner rule (or Miner’s rule) is a linear damage accumulation rule used to predict the number of load cycles to failure under variable amplitude loading. The Palmgren-Miner rule asserts that the damage fraction, $\Delta_i$, at any stress level, $S_i$, is linearly proportional to the ratio of $n_i$, the number of load cycles of operation under this stress amplitude to $N_i$, the total number of load cycles that would produce a failure at that stress level. The damage fraction is computed as follows [137]:

$$\Delta_i = \frac{n_i}{N_i}$$  \hspace{1cm} (4.3.2-1)

where $n_i \leq N_i$. If the stress amplitude is changed, a new partial damage is calculated for this new amplitude level, where the corresponding $N_i$ value is found from an applicable $S$-$N$ curve. These are commonly called fatigue end-of-life curves. The total accumulated damage, $D [\cdot]$, is then given by [137]:

$$D = \sum_i \Delta_i = \sum_i \frac{n_i}{N_i}$$  \hspace{1cm} (4.3.2-2)

and failure is assumed to occur when $D \geq 1$. Here failure is a quite strong expression as the criterion corresponds to initiation of a macroscopic flaw. However, in many cases most of the time in operation for a component under fatigue is spent in the phase before the flaw initiation.
The parameter $D$ is commonly called cumulative usage factor (CUF). The main drawbacks of the linear damage rule are its independence of load level and load sequence as well as lack of taking into account the interaction of the loads.

The fatigue end-of-life curves are used together with the Miner’s rule. These curves indicate how many stress cycles it takes to initiate fatigue cracks in components and grow them to a macroscopic size. These curves are material type specific and indicate the maximum allowable number of stress cycles for applied cyclic stress amplitudes. Design curves for RPV materials are given in ASME Section III, Appendix I [139] and in national standards, such as KTA 3204 [140]. The ASME III fatigue end-of-life curves [139] are entirely based on data obtained from tests in air, mainly at room temperature. These curves were developed by applying a factor of 2 on stress or 20 on number of stress cycles, whichever is lower, to the mean failure curve for small polished specimens.

The transferability of such test results to NPP conditions is limited. This is because:

- uniaxial test loading differs a lot from the actual loading conditions in the NPPs,
- tests done in air do not take into account the effect of the NPP environment,
- polished test specimen surfaces deviate a lot from the surfaces of actual components.

In the U.S., the main procedure to take into account the effect of NPP environment to the linear damage accumulation is presented in report NUREG/CR-6909 [141]. It provides an environmental fatigue correction factor ($F_{en}$) methodology that is considered acceptable for incorporating the effects of reactor coolant environments on fatigue usage factor evaluations of metal components for new reactor construction. The methodology reflects the earlier development on the subject carried out in Japan. Report [141] describes the environmental fatigue correction factor methodology of fatigue assessment for the four major material types, those being carbon steels, low-alloy steels, wrought and cast austenitic stainless steels, and Ni-Cr-Fe alloys, respectively. An update of report [141] was published in 2014, which is NUREG/CR-6909, Rev. 1 [144]. However, its status was a draft report for comments. ASME has also published Code Case N-792 [145] containing a $F_{en}$ procedure, which is almost the same as that in report [141]. The final edition of the NUREG/CR-6909, Rev. 1 was published in March 2018, see ref. [146].

However, the description of the environmental fatigue analysis methodology appears insufficient for computing the strains and consequent $F_{en}$ values for actual NPP piping components concerning typical load transients with time dependently varying loads. Most of the drawbacks concerning the $F_{en}$ methodology [141] are partly or fully overcome in the NB-3650M Fatigue Calculation Procedure, as developed by TVO and FEMdata [148]. This procedure takes into account e.g. three dimensional stress state, real strains, and time dependency in the application of the rainflow load cycle computation procedure.

Note, that this far the environmental fatigue correction factor methodology has been applied only to NPP piping. It is foreseen that it is also applicable to BWR RPV internals.

### 4.3.3 Fatigue crack propagation models

Most of the computational models for fatigue induced crack growth apply stress intensity factor, $K$ [MPa\(\sqrt{m}\)], which is a loading parameter from fracture mechanics. Most often crack opening mode is considered, i.e. mode I, in which case $K$ is denoted as $K_I$. For a more detailed description on $K$ and fracture mechanics, see Section 8.1.2. One of the earliest and most often used fatigue crack propagation models is the Paris-Erdogan equation [150]. It is an empirical equation that
relates the cyclic crack growth rate to \( K_I \) range \( \Delta K_I \). The Paris-Erdogan equation can be written as follows:

\[
d\alpha = C_{FA} \left[ \frac{\Delta K_I}{\Delta K_{I,adj}} \right]^{m_{FA}}
\]

(4.3.3-1)

where \( \alpha \) [mm] is crack depth, \( N \) [-] is the number of load cycles, \( \Delta K_I \) [MPa\( \sqrt{m} \)] is \( K_I \) range, i.e. \( K_{I,max} - K_{I,min} \), \( \Delta K_{I,adj} = 1.0 \text{ MPa}\( \sqrt{m} \)\), whereas \( C_{FA} \) [mm/(load cycle)] and \( m_{FA} \) [-] are material, temperature and environment specific constants. The values for both of these constants are determined as based on experimental data.

The Paris-Erdogan equation is limited by the following assumptions: the crack growth depends only on \( \Delta K_I \), the stress amplitude is constant and small enough for linear-elastic fracture mechanics to be applicable, and that the crack growth rate is independent of the previous load history. Failure occurs when the maximum value of \( K_I \) exceeds the fracture toughness or some predetermined critical crack depth is reached. Thus, the Paris-Erdogan equation describes crack growth at intermediate values of fatigue crack growth curve [10]. However, in case of large scale yielding Paris-Erdogan equation can remain valid. This is due to extensive isotropic hardening. This was demonstrated experimentally by Ljustell [152].

Other significant approaches based on the crack growth approach include:

- The fatigue damage propagation model proposed by Forman [152] covers both the intermediate and high \( \Delta K \) regions.
- McEvily [154] developed an equation that can be fit to the entire fatigue crack growth curve.
- According to the model by Elber [155] a crack only propagates while its flanks are separated, and is then driven by the effective stress intensity factor range, \( \Delta K_{eff} \).
- Wheeler [156] developed a model based on the yield zone concept for tensile overloads.
- A more advanced development is the model by Forman and Mettu [158], which follows a cycle-by-cycle integration method as using the sigmoidal crack growth rate relationship. This model is now included in the European Fitness For Service Network (FITNET) fitness-for-service procedure [159].

Of fatigue crack growth rate equations for metallic NPP component materials, data for the material and environment specific constants are most available for Paris-Erdogan equation (4.3.3-1). Due to this it is the most applicable equation, regardless of its limited scope. The more recent and advanced fatigue crack growth rate equations are more accurate, but it is often difficult to find applicable data for their material and environment specific constants. But if sufficient data is available, it is recommendable to use the more recent and advanced fatigue crack growth rate equations.

### 4.3.4 Susceptibility to fatigue

The primary degradation mechanism affecting BWR RPVs is fatigue. Susceptible areas include austenitic stainless steel safe-ends that have been sensitized by post-weld heat treatment, crevice areas where thermal sleeves are attached to Inconel or stainless steel safe-ends, and austenitic cladding on the inside corners and surrounding regions of the nozzles. The nozzle most severely affected by fatigue is the feedwater nozzle [165].

Table 4.3.4-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to fatigue.
Table 4.3.4-1. Summary on susceptibility of materials of BWR RPV and its internals to fatigue [1,3,165,172]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

4.4 SCC

SCC is the term given to crack initiation and sub-critical crack growth of susceptible steels and alloys under the influence of tensile stress and a corrosive environment. SCC is a complex phenomenon driven by the synergistic interaction of mechanical, electro-chemical and metallurgical factors. BWR RPV internals are potentially susceptible to two predominant forms of SCC, which are [1]:

- intergranular stress corrosion cracking (IGSCC), and
- irradiation assisted stress corrosion cracking (IASCC).

4.4.1 Computational modelling of IGSCC

There are basically three approaches to assess the propagation of SCC [3]:

1. Experimental and probabilistic analyses of past plant failures, which can lead to the definition of risk based inspection priorities and to the prediction of future frequencies of failure.
2. Analyses of laboratory data in order to develop empirical life prediction algorithms between the degradation rate and the interacting effects of various system parameters.
3. The development of mechanisms based SCC assessment algorithms in conjunction with good quality laboratory degradation rate data.

These three approaches have recently been reviewed, see ref. [173]. Some notable fracture mechanics based examples of the SCC assessment models are described in the following. They are adjusted to IGSCC analyses by using applicable material, temperature and environment specific constants. In the fracture mechanics based treatments for SCC, the variable of interest, being the already mentioned $K_I$, is measured against time.

The commonly used equation for intermediate stage SCC rate [174] can be written as:

$$\frac{da}{dt} = C_{SCC} \left( \frac{K_I}{K_{I,adj}} \right)^{n_{SCC}} \quad (4.4.1-1)$$

where $a$ [mm] is again crack depth, $t$ [year] is time and $K_{I,adj} = 1.0$ MPa\textcdot m, whereas $C_{SCC}$ [mm/year] and $n_{SCC}$ [-] are material, temperature and environment specific constants. In most cases, the $C_{SCC}$ and $n_{SCC}$ values applicable for NPP components have been defined with upper bound approach in relation to the underlying experimental data. This means that the model
curve is positioned so that most or all data points are below it. This is a very conservative approach. The popularity of this SCC equation is based on its simplicity and availability of values for $C_{SCC}$ and $n_{SCC}$.

Other significant models for computation of crack growth due to SCC include:
- EPRI MRP-55 model [175,176] is a more advanced intermediate stage rate equation for alloy Inconel 600 that also takes into account $K$ threshold.
- EPRI MRP-115 model [177,178] is a more advanced intermediate stage rate equation for alloys 182 and 82, which are used e.g. for welding of BWR components of austenitic stainless steel.
- A more recent procedure which uses a mechano-chemical model based on a slip formation/dissolution was presented by Saito and Kuniya [179]. This model consists of combined kinetics of the plastic deformation process as a mechanical factor and the slip dissolution-repassivation process as an environmental factor at the crack tip.

Of the SCC propagation equations for metallic NPP component materials, equation (4.4.1-1) is the most applicable one, because of its simplicity and the availability of the data for the material and environment specific constants. This equation has more recently been supported by the latest editions of the ASME Section XI [82], which now contain SCC propagation equations of this type for both BWR and PWR environments. For BWR environments the covered materials are:
- alloy 600 and associated weld material alloys 182 and 132, and
- austenitic stainless steels.

4.4.2 Computational modelling of IASCC

The computational modelling of IASCC resembles that of IGSCC as the applied equation is quite similar compared to the SCC rate equation (4.4.1-1). In this case, material, temperature and environment specific constants applicable to IASCC are used. It has been more challenging to determine experimentally the values for the constants in IASCC rate equation than for those in SCC rate equation, because the test specimens have to be irradiated. Over the years, the available experimental IASCC growth rate data has gradually increased.

IASCC equations estimating crack growth rate (CGR) in BWR environments for austenitic stainless steels have been developed from about 2000 onwards by using then available experimental data, see e.g. refs. [180,181,182], but substantially more measured CGR data are available now. The most recent update of the IASCC equation for austenitic stainless steels is by Eason and Pathania [184]. As compared to the underlying experimental data this equation corresponds to 75th percentile of the measured CGRs. Even though the upper bound approach has been relaxed to some extent, to avoid excessive conservatism, their IASCC equation is still conservative.

Eason and Pathania [184] have developed IASCC equations for both normal water chemistry (NWC) and hydrogen water chemistry (HWC) conditions of BWRs under operational temperature of $288 \degree C$. The equation for NWC conditions can be written as:

$$
\frac{da}{dt} = 2.84 \times 10^{-17} \left( \frac{\sigma_{0.2}}{\sigma_{adj}} \right)^{2.675} \left( \frac{K}{K_{1,adj}} \right)^{2.486} \left( \frac{1 \text{ mm}}{\text{ year}} \right)
$$

(4.4.2-1a)
and that for HWC conditions can be written as:

\[
\frac{da}{dt} = 1.35 \times 10^{-17} \left( \frac{\sigma_{0.2}}{\sigma_{\text{adj}}} \right)^{2.547} \left( \frac{K_I}{K_{I,\text{adj}}} \right)^{2.504} \left( \frac{1}{\text{mm/year}} \right)
\]

(4.4.2-1b)

where \(\sigma_{0.2}\) [MPa] is the yield strength of irradiated austenitic stainless steel, \(\sigma_{\text{adj}} = 1.0\) MPa, and otherwise the variables and constants with their dimensions are the same as for those in equation (4.4.1-1). The irradiated yield strength can be estimated from irradiation dose, material type, temperature, and cold work before irradiation (if any). The major uncertainty in application of all published IASCC equations is the apparent variability in CGR of different material heats under the same environment, dose, and loading conditions [184]. The causes of these large differences in CGR from heat to heat are not well understood. Part of the variability attributed to material susceptibility may arise from differences in experiments at various laboratories, because most heats have been tested at only one laboratory.

Of the IASCC propagation models for metallic NPP component materials, equation by Eason and Pathania [184] is arguably the most accurate one, because it is based on larger amount of underlying experimental data than the other models.

### 4.4.3 Susceptibility to IGSCC

Susceptibility to IGSCC varies with alloy composition and metallurgical condition. Given conditions of normal stress and BWR environment, several materials have shown susceptibility to IGSCC as a result of the material itself or due to its fabrication history. For RPV internals, these materials include austenitic stainless steel of type 304/316 as well as nickel-base alloys 600 and 182 [1]. Of these, the two former ones are base materials and the latter one is weld material.

The Generic Letter 88-01 [185] and report NUREG 0313, Revision 2 [186] present the position of the U.S. NRC on austenitic stainless steel piping potentially susceptible to IGSCC in BWRs. According to refs. [185,186] the following steels are susceptible to IGSCC:
- austenitic stainless steel with carbon content exceeding 0.035 %,
- CASS with carbon content exceeding 0.035 % and ferrite content less than 7.5 %.

Table 4.4.3-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to IGSCC.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
<tr>
<td></td>
<td>• austenitic stainless steels with carbon &gt; 0.035 %,</td>
</tr>
<tr>
<td></td>
<td>• cast austenitic stainless steels with carbon &gt; 0.035 % and ferrite &gt; 7.5 %,</td>
</tr>
<tr>
<td></td>
<td>• nickel based alloys Inconel 600, weld metal 182,</td>
</tr>
<tr>
<td></td>
<td>• for Alloy 600 materials: (K_a = 9) MPa(\text{m}) [175,176],</td>
</tr>
<tr>
<td></td>
<td>• for sensitized 304 steel: (K_a = 8) MPa(\text{m}) [187,188],</td>
</tr>
<tr>
<td></td>
<td>• for sensitized 316 steel: (K_a = 10.5) MPa(\text{m}) [189],</td>
</tr>
<tr>
<td></td>
<td>• for 316 weld metal: (K_a = 5.8) MPa(\text{m}) [190,191].</td>
</tr>
</tbody>
</table>
4.4.4 Susceptibility to IASCC

All susceptibility issues described in Section 4.4.3 for IGSCC apply also to IASCC [1]. Based on the available field and laboratory data, for IASCC neutron fluence ($E > 1$ MeV) threshold of approximately $5.0 \times 10^{24}$ n/m$^2$ exists for annealed types 304, 304L, 347 and 348 stainless steel components under high tensile stress, and approximately $2.0 \times 10^{25}$ n/m$^2$ for components under moderate or low tensile stress [192,193]. Welded and irradiated RPV internals, such as the core shroud, appear to have lower threshold fluence due to the presence and interaction of weld sensitization, high residual stresses and irradiation.

Table 4.4.4-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to IASCC.

Table 4.4.4-1. Summary on susceptibility of materials of BWR RPV and its internals to IASCC [1,185,186,192,193]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X: austenitic stainless steels, stablised and non-stabilised stainless steels, nickel based alloys Inconel 600, weld metal 182,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>for type 304, 304L, 347 and 348 steels neutron fluence threshold is $5.0 \times 10^{24}$ n/m$^2$ under high stress and $2.0 \times 10^{25}$ n/m$^2$ under low stress [192,193],</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>same material specific $K_{th}$ data as for IGSCC, see Table 4.4.3-1.</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td></td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td></td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
</tr>
</tbody>
</table>

4.5 General corrosion

General corrosion is characterized by uniform surface loss due to material oxidation. In aqueous solutions including reactor coolants, it is primarily an electro-chemical process involving an anodic oxidation reaction to form positively charged metal cations, which is balanced electrically by a cathodic reaction involving the reduction of oxygen in solution (if present) and/or reduction of hydrogen ions to molecular hydrogen gas [3].

4.5.1 Computational modelling of general corrosion

Several procedures using electro-chemical and thermo-dynamic models exist for computation of general corrosion rates in both carbon steels and austenitic stainless steels, see e.g. refs. [222,223]. However, such models are not suitable for mechanical analyses of e.g. wall thinning due to corrosion as the computed results mainly concern corrosion potential parameters, such as current density at the corrosion site. However, some models for general corrosion exist that are applicable to structural integrity analyses.

The straightforward equation for assessment of general corrosion propagation depth, $x$ [$\mu$m], can be written as [224]:

$$x(t) = C \left( \frac{t}{t_{adj}} \right)^n$$

(4.5.1-1)
where \( C [\mu m] \) is effective corrosion rate in the absence of a coating, \( t \) [year] is time since the start of the corrosion process, \( t_{adj} \) is 1.0 years and \( n [-] \) is power rule exponent for non-linear behaviour. \( C \) and \( n \) are experimentally determined material and environment specific constants.

Naus et al. [225] have developed a more advanced empirical model for corrosion depth in steel under general corrosion. Two general methods are recognized for estimating atmospheric corrosion resistance of low alloy steels [226] from test data. The first utilizes linear regression analysis of short-term data to predict long-term performance by extrapolation. The second determines a corrosion resistance index based on chemical composition of the steel.

The applicability of empirically defined models for general corrosion depends on the available measurement data. Once all available data for the analysis case has been collected, based on it one can choose an applicable model for general corrosion. It is difficult to identify the most recommendable model. The accuracy of the computational analysis results depends also on the accuracy of the available measurement data.

4.5.2 Susceptibility to general corrosion

Laboratory tests and operational experience have established that for RPV internals of austenitic stainless steels and nickel-base alloys general corrosion is not a significant degradation mode. These conclusions are based on the very low general corrosion rates which have been observed in BWR plants for all materials of RPV internals [1].

Table 4.5.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to general corrosion. Note that even though most/all metallic materials are susceptible to general corrosion, in practise its effect is practically insignificant to BWR RPVs and their internals. However, in the rare case of locally missing region of inner cladding the RPV base material can became susceptible to very slowly progressing general corrosion.

Table 4.5.2-1. Summary on susceptibility of materials of BWR RPV and its internals to general corrosion [1,3]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

4.6 E/C and FAC

E/C includes all forms of accelerated attack in which protective surface films and/or the metal surface itself are removed by combination of solution velocity and corrosion. FAC corresponds to erosion (or thinning) of component wall, involving the electrochemical aspects of general corrosion plus the effects of mass transfer and momentum transfer.
4.6.1 Computational modelling of E/C and FAC

There are several procedures and analysis codes available to computationally predict the propagation of FAC. Some of the analysis codes are commercially available, some are proprietary. Significant FAC analysis procedures and codes are described in the following. This is followed with describing a computation procedure to predict E/C.

The KWU-KR model [229] allows calculating the FAC rate of carbon steel components as a function of Keller’s geometry factor, flow velocity, fluid temperature, material chemical composition, fluid chemistry (pH at 25 °C and dissolved oxygen), exposure time, and, in the case of two-phase flow, steam quality. The thinning of the component wall is calculated as a function of time with this model. The wall corrosion, \( W_C(t) \) [cm], is the thickness of the layer that has corroded away, and is given as [229]:

\[
W_C(t) = \frac{\Delta \phi_R t}{\rho_{st}}
\]  

(4.6.1-1)

where \( \Delta \phi_R \) [\( \mu g/(cm^2h) \)] is FAC rate, \( t \) [h] is exposure time and \( \rho_{st} \) [\( \mu g/cm^3 \)] is density of steel. The FAC rate is computed according to ref. [229] using data on pH, oxygen content, liquid velocity, geometrical factor, total content of chromium and molybdenum in steel, and operating temperature. The KWU-KR model is mainly used for FAC analyses of pipes. More recently, a probabilistic development of this FAC model has been provided, see ref. [8].

Other significant models for computation of wall thinning due to FAC include:

- Extending the Keller and the Kastner correlations and the Berge model for FAC, Chexal and Horowitz designed and implemented an algorithm which is used in the CHECWORKS analysis code by EPRI [231].
- Sanchez-Caldera et al. developed a simple analytical model to predict the FAC rate [233].
- Electricité de France (EDF) [237] developed an analytical FAC model [237]. It covers both single and two phase flows. More recently, EDF has provided a probabilistic development of their FAC model, see ref. [238].

A steady state E/C model for the feedwater piping has been proposed by Gopika et al. [240]. In the model, oxide dissolution and mass transfer based on oxide dissolution are considered as main factors that influence wall thinning rate due to E/C.

Arguably, the FAC model in CHECWORKS analysis code [231] should be the most accurate one, as it is the most recent one. However, according to comparison results presented in report NUREG/CR-5632 [8] the EPRI model [231] is much more conservative than the KWU-KR model [229]. Keeping in mind that the KWU-KR model [229] is conservative too. All necessary data concerning the FAC models by EDF [237] are not publicly available. Thus, it is recommendable to use the KWU-KR model [229].

4.6.2 Susceptibility to E/C and FAC

Stainless steel and nickel-base alloys are generally resistant to E/C [1]. FAC occurs especially at areas of high turbulence that are often associated with geometrical discontinuities or abrupt changes in flow direction. FAC is most commonly observed in carbon steel used in feedwater, extraction steam and drain lines, and in copper-based alloys used in condensers and heat exchangers [3]. FAC of carbon steels also depends strongly on the steam quality of two-phase flows [231]. The range of temperatures normally associated with FAC in single-phase water extends to higher temperatures in two-phase flow [232].
Table 4.6.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to E/C and FAC.

**Table 4.6.2-1. Summary on susceptibility of materials of BWR RPV and its internals to erosion-corrosion and FAC [1,3,229].** Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>FAC is most commonly observed in carbon steel used in feedwater, extraction steam and drain lines, and in condensers and heat exchangers,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>FAC of carbon steels also depends strongly on the steam quality of two phase flows, it is at maximum between 40 and 80 %,</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>see Section 4.6.1 for assessment of propagation of FAC.</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td></td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
</tr>
</tbody>
</table>

4.7 Mechanical wear

Wear is the loss of material, generally measured as the rate of removal of surface material, caused by the relative motion between adjacent metal surfaces or by the action of hard, abrasive particles in contact with a metal surface [3].

4.7.1 Computational modelling of wear

Wear as a degradation mechanism has been modelled extensively. It is estimated that by now hundreds of computational models have been developed to describe wear [241]. A commonly used simple model has been developed by Archard [242]. With this model the volumetric amount of material loss due to wear, $V$ [mm$^3$], is computed as:

$$V = \frac{K' Q s}{H}$$  \hspace{1cm} (4.7.1-1)

where $K'$ [-] is empirically determined wear coefficient, $Q$ [N] is contact normal force, $H$ [N/mm$^2$] is the hardness of the softer body, and $s$ [mm] is the sliding distance. Obviously, the value of $K'$ varies for different material pairs. It is usually determined by carrying out sliding wear tests using the materials of interest [243].

The applicability of empirically defined wear models depends on the available measurement data. Once all available data for the analysis case has been collected, based on it one can choose an applicable wear model. It is difficult to identify the most recommendable model. The accuracy of the computational analysis results depends also on the accuracy of the available measurement data.

4.7.2 Susceptibility to mechanical wear

Mechanical wear has been identified as a potential degradation mechanism at specific locations in the RPV internals due to flow induced vibration (FIV) [1]. Wear is a concern for some steam dryer components, such as vessel lugs, feedwater end brackets, control rod drive mechanisms.
and BWR/6 shroud head studs [3]. This degradation mechanism is of minor importance concerning the capability of RPV internals to perform their safety functions [1]. As for FIV, TVO has already examined the issue thoroughly for OL1/OL2 components. This information is reported in ref. [24].

Table 4.7.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to mechanical wear.

Table 4.7.2-1. Summary on susceptibility of materials of BWR RPV and its internals to mechanical wear [1,24]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

vibration and loose parts can cause mechanical wear, for a detailed description on susceptibility to FIV, see ref. [24].

4.8 Modelling of interaction of degradation mechanisms

The interaction of degradation mechanisms is possible for the BWR RPV and its internals. The significant interacting degradation mechanisms described in Section 3.2.10 are irradiation-creep, corrosion-fatigue and creep-fatigue. As the operational temperatures of the BWR RPV and its internals are below the creep range, irradiation-creep and creep-fatigue are excluded here. As to stress relaxation the bolts in some joints do experience some, but as they are easily replaceable considering this degradation mechanism is excluded here. Thus, only the computational models for corrosion-fatigue are summarised here. The considered corrosion mode is SCC.

Many models which combine fatigue and stress corrosion effects have been suggested for predicting the corrosion-fatigue crack growth rate as a function of fatigue loading frequency. These models can be divided into three categories [25]:

1. superposition models,
2. competition models, and
3. models for environmentally modified material deformation and fatigue properties.

The superposition models and the competition models were developed with the assumption that an environmental fracture process is independent from the mechanical fatigue fracture process and that the latter process in a corrosive environment is identical with that in an inert environment. According to the superposition models, the environmental degradation and a pure fatigue fracture process occur simultaneously and independently on the same fracture surface, and have no interaction with each other.
4.9 Summary on susceptibility to degradation mechanisms

A summary on susceptibility to degradation mechanisms concerning BWR RPV and its internals is presented in the following. Obviously loads, geometry and environment affect susceptibility too, e.g. temperature, pressure, flow rate, material interfaces, abrupt changes in geometry and water chemistry. The summary is based on the data presented in Sections 4.1 to 4.7, and is presented here as tabulated, see Table 4.9-1. It is not feasible here to try to assemble the material type specifically expressed susceptibility to degradation mechanisms into importance or significance classes, as that aspect is mainly dependent on the location specific loading conditions and of the environment. For instance, in some location a component of austenitic stainless steel can severely experience IGSCC, whereas in some other location none at all.

Table 4.9-1. Summary on susceptibility of BWR RPV and its internals to relevant degradation mechanisms. Here, susceptibility is denoted with "X".

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Material types</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>austenitic stainless steels</td>
</tr>
<tr>
<td>Irradiation embrittlement</td>
<td>X</td>
</tr>
<tr>
<td>Thermal embrittlement</td>
<td></td>
</tr>
<tr>
<td>Fatigue (i)</td>
<td>X</td>
</tr>
<tr>
<td>IGSCC</td>
<td></td>
</tr>
<tr>
<td>IASCC</td>
<td></td>
</tr>
<tr>
<td>General corrosion (ii)</td>
<td></td>
</tr>
<tr>
<td>Erosion-corrosion, FAC</td>
<td></td>
</tr>
<tr>
<td>Mechanical wear</td>
<td></td>
</tr>
</tbody>
</table>

(i): All metallic NPP materials are in principle susceptible to fatigue, however, in most cases for BWR RPV and its internals the effect is negligible.
(ii): All metallic NPP materials are in principle susceptible to general corrosion, however, for BWR RPV and its internals the effect is negligible.
5 Susceptibility of BWR RPV and its internals to degradation

Before moving on to susceptibility of the BWR RPV and its internals to degradation, some main characteristics of the components in question are described briefly. Of BWR plants, the scope of this work concerns those designed by Asea-Atom (which is nowadays Westinghouse), focusing on the third generation.

The average main dimensions of BWR RPVs and a list of significant internals are already presented in Section 2. The suppliers and design background of the BWR RPVs as well as materials and some characteristics of Asea-Atom BWR RPVs and internals are summarised in the following.

Operating BWR RPVs have been fabricated by several suppliers. These are [117]: Combustion Engineering Inc., Babcock & Wilcox Company, CBIN, Ishikawajima-Harima Heavy Industries Company Ltd., Chicago Bridge & Iron Company, Babcock Hitachi and Rotterdam Dry-dock and Manufacturing (RDM). For the Siemens BWR RPVs main suppliers have been e.g. Breda, Uddcomb and RDM, and for Asea-Atom BWR RPVs Uddcomb.

The Asea-Atom RPVs have been designed according to Article NB-3000 of ASME Section III [147]. The fatigue analyses have been and are performed in accordance with ASME section III section NB-3216.2.

BWR RPVs use different materials for different components, such as shells, nozzles, flanges and studs. In addition, the choices in the materials of construction changed as the BWR product line evolved [117]. Typical materials for Asea-Atom RPV and internals are ferritic steel for RPV base material and main nozzles, nickel-base alloys for nozzle safe-ends, and austenitic stainless steels for internals. The materials used for internals have been selected due to their corrosion resistance, toughness, ductility, strength and fatigue characteristics in BWR environment. Typical materials for RPV internals of various BWR types are listed in ref. [1].

The BWR plants designed and built by Asea-Atom can be classified into four generations. The first generation is based on the technology of the 1960’s. The significant design change made for the third generation BWRs in the late 1970’s was that the internal main recirculation pumps with wet motors were introduced. The early generation plants have an external main recirculation system. The power of all reactors except Oskarshamn 1 has been upgraded at least 6-25 % from the original [253,254,255]. In 2015, the nominal power of OL1 and OL2 units was 890 MW [252]. Table 5-1 presents some characteristic data of Asea-Atom BWR units.

The internals determined as significant in TVO report [24] and IAEA-TECDOC-1471 [1] are considered in this work. These components have been selected from TVO units OL1/OL2, see Figure 5-1. From that, the presentation here is narrowed down to cover only the most important components. However, a description on susceptibility issues and other associated relevant information for all considered components is presented in Section 4 and Appendix C.

In NPPs, all systems, structures and machinery must be appointed to safety classes. This also affects the selection of components to be analysed. The classification shall primarily be based on deterministic methods supplemented, where necessary, by Probabilistic Risk Assessment (PRA) and expert judgement. The systems consist of defined structural and functional entities. The systems are further divided into structures and components. In Finland, the systems,
structures and components of a NPP shall be grouped into the Safety Classes 1, 2, and 3 and Class EYT (non-nuclear safety), as based on their structural strength, integrity and leak-tightness required for preventing the spreading of radioactive substances. These matters are defined in YVL Guide B.2 “Classification of systems, structures and components of a nuclear facility” [250] by STUK. The highest safety class is assigned to the most safety significant structures, components and machinery [251].

Table 5-1. Some characteristic data on Asea-Atom BWR units [253].

<table>
<thead>
<tr>
<th>Unit</th>
<th>Generation</th>
<th>Start of operation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Oskarshamn 1</td>
<td>1st</td>
<td>1972</td>
</tr>
<tr>
<td>Ringhals 1</td>
<td>1st</td>
<td>1976</td>
</tr>
<tr>
<td>Oskarshamn 2</td>
<td>2nd</td>
<td>1974</td>
</tr>
<tr>
<td>Barsebäck 1</td>
<td>2nd</td>
<td>1975</td>
</tr>
<tr>
<td>Barsebäck 2</td>
<td>2nd</td>
<td>1977</td>
</tr>
<tr>
<td>Olkiluoto 1</td>
<td>3rd</td>
<td>1979</td>
</tr>
<tr>
<td>Olkiluoto 2</td>
<td>3rd</td>
<td>1980</td>
</tr>
<tr>
<td>Forsmark 1</td>
<td>3rd</td>
<td>1980</td>
</tr>
<tr>
<td>Forsmark 2</td>
<td>3rd</td>
<td>1981</td>
</tr>
<tr>
<td>Forsmark 3</td>
<td>4th</td>
<td>1985</td>
</tr>
<tr>
<td>Oskarshamn 3</td>
<td>4th</td>
<td>1985</td>
</tr>
</tbody>
</table>

The safety classification of NPP systems shall be based on the specified safety functions and the significance of the systems that perform them in terms of the reliability of these safety functions, with due consideration to ensuring safety by defence-in-depth [250].

The safety classification of components shall be based on the function required of them to accomplish safety functions or to prevent the spreading of radioactive substances as well as on the structural strength, integrity and leak-tightness required of them. The safety class of a component is determined based on which of these justifications requires the highest safety class [250]. Safety Class 1 includes nuclear fuel as well as structures and components whose failure could result in an accident compromising reactor integrity and requiring immediate actuation of safety functions. Safety Class 1 includes also the RPV and those components of the primary circuit whose failure results in a primary circuit leak that cannot be compensated for by systems relating to normal plant operation [250]. Classification criteria ensuring structural resistance, integrity and leak-tightness to Classes 1, 2, and 3 and EYT are described in detail is Section 3.3 of ref. [250]. Seismic classification of NPP systems, structures and components is defined in Section 3.4 of ref. [250].

The fuel assembly is excluded from the analysis scope here as it is not a load bearing structure. As it is a significant internal structure, it is for completeness described in the following.

Among other things, the safety classification affects to [251]:
- control and supervision by the regulator,
- pre-inspection documentation to be sent to STUK,
- testing intervals and acceptance criteria.

The following issues are presented in this section:
- geometry, material regions, function and safety class of the selected components,
- susceptibility of the selected components to degradation mechanisms,
- summary of the presented information for all considered components.
<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>System</th>
<th>Main function</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Flange cooling spray piping</td>
<td>211.7</td>
<td>Cooling of RPV flange</td>
</tr>
<tr>
<td>2</td>
<td>Long nozzle pipes in cooling spray piping</td>
<td>211.7</td>
<td>Cooling of RPV flange</td>
</tr>
<tr>
<td>3</td>
<td>Evacuation pipe</td>
<td>211.8</td>
<td>Evacuate RPV steam, supply nitrogen</td>
</tr>
<tr>
<td>4</td>
<td>Spring beams and support brackets</td>
<td>211.9</td>
<td>Depressing RPV internals</td>
</tr>
<tr>
<td>5</td>
<td>Steam dryer</td>
<td>215</td>
<td>Separate water from steam</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>311</td>
<td>Steam outlet</td>
</tr>
<tr>
<td>7</td>
<td>Steam separator stand pipes</td>
<td>214</td>
<td>Separate water from steam</td>
</tr>
<tr>
<td>8</td>
<td>Steam separator pipe bundles</td>
<td>214</td>
<td>Separate water from steam</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator support legs</td>
<td>214</td>
<td>Support steam separator</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>312</td>
<td>Feed water inlet</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>213.1</td>
<td>Distribute feed water</td>
</tr>
<tr>
<td>12</td>
<td>Boron spray nozzles and piping</td>
<td>213.5</td>
<td>Supply water with boron to RPV</td>
</tr>
<tr>
<td>13</td>
<td>Core spray piping outside core shroud cover</td>
<td>213.3</td>
<td>Supply emergency cooling water to RPV</td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>213.3</td>
<td>Supply emergency cooling water to RPV</td>
</tr>
<tr>
<td>15</td>
<td>Fuel assembly</td>
<td>261</td>
<td>Supply fuel</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>222</td>
<td>Increase or decrease neutron flux</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>212.5</td>
<td>Support and guide control rods</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>212.2</td>
<td>Support core grid, Core shroud cover and fuel assembly</td>
</tr>
<tr>
<td></td>
<td></td>
<td>212.1</td>
<td>Support main circulation pumps</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>212.1</td>
<td>Support main circulation pumps</td>
</tr>
<tr>
<td>20</td>
<td>Main circulation pump nozzles</td>
<td>313</td>
<td>Circulate water to reactor</td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>212.1</td>
<td>Support Core shroud</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>212.6</td>
<td>Allow on-line monitoring</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>212.5</td>
<td>Support and guide control rods</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>211.1</td>
<td>Contain RPV internals</td>
</tr>
<tr>
<td>25</td>
<td>RPV bottom</td>
<td>211.1</td>
<td>Contain RPV internals</td>
</tr>
<tr>
<td>26</td>
<td>RPV support skirt</td>
<td>211.3</td>
<td>Support RPV</td>
</tr>
<tr>
<td>27</td>
<td>RPV flange</td>
<td>211.2</td>
<td>Support RPV-head</td>
</tr>
<tr>
<td>28</td>
<td>RPV-head</td>
<td>211.1</td>
<td>Contain RPV internals</td>
</tr>
<tr>
<td>29</td>
<td>RPV-head bolts</td>
<td>211.1</td>
<td>Fasten RPV-head</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>321</td>
<td>Cool down the reactor to shutdown condition</td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>323</td>
<td>Supply emergency cooling water to RPV</td>
</tr>
</tbody>
</table>

Figure 5-1. The OL1/OL2 RPV components considered in this work.
5.1 Function, characteristics and susceptibility of selected components

A description on the characteristics and degradation susceptibility of the OL1/OL2 RPV and its most important internals is presented in the following. These components are shown in Figure 5-1, see IDs 6, 10, 16, 18, 21 and 24. The considered characteristics are the geometry, material regions, operational function and safety class of the considered components. The degradation susceptibility issues are described after that.

Apart from the core shroud support skirt and the pump deck, which are welded to the reactor vessel, all internals are removable [252].

The following symbols are used to denote the dimensions of the presented components:
- \( L \) is length,
- \( D_o \) is outer diameter, \( D_i \) is inner diameter,
- \( R_o \) is outer radius, \( R_i \) is inner radius,
- \( t \) is wall thickness,
- \( h \) is height,
- \( b \) is width.

**Steam outlet nozzles (ID6)**

Steam system 311 supplies steam through the containment from the RPV into the turbine plant main steam system. The steam outlet nozzles connect the system 311 to the RPV [252]. The steam outlet nozzle is shown in Figure 5-1. The main dimensions and materials of the steam outlet nozzle are presented in Table 5-1a. The safety class of these components is 1 [251].

*Figure 5-1. Overall geometry of steam outlet nozzle [268].*
Table 5-1a. The cross-section dimensions and materials of steam outlet nozzle [269,270,271]. The nozzle dimensions are given at the region where it joins the pipe.

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>508</td>
<td>23.5</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td>5</td>
<td></td>
<td>SIS-2333</td>
</tr>
</tbody>
</table>

Those degradation mechanisms for which the steam outlet nozzle is susceptible to are presented in Table 5-1b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-1b. Those degradation mechanisms for which steam outlet nozzle is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle and welds, ferritic steel</td>
<td>material is moderately susceptible</td>
<td>low-cycle thermal fatigue is possible, significant WRSs</td>
</tr>
</tbody>
</table>

Discussion on those degradation mechanisms for which the steam outlet nozzle is not susceptible to:

- Irradiation embrittlement; nozzle material is susceptible, but neutron fluence is negligible.
- IGSCC; materials are not susceptible, located in steam dome.
- IASCC; neutron fluence is negligible, materials are not susceptible, located in steam dome.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the inner cladding material of the nozzle is not susceptible and the conditions are unfavourable. The nozzle is covered from the inside with cladding of austenitic stainless steel, which material is not susceptible to erosion-corrosion or FAC, in addition to which the contents inside is only dry steam.
- Mechanical wear; no possibility for contacting frictional movement with connecting components.

Feedwater nozzles (ID10)

The main functions of the feedwater system 312 is to bring water into the RPV with dividing the water flow evenly for all four feed water lines and keep the flow and temperature distributions steady [279]. A feedwater nozzle and a connecting safe-end are shown in Figure 5-2a. Inside the feedwater nozzle there is an insert tube, which thermally protects the nozzle, see Figure 5-2b. The insert tube also connects to the feedwater sparger, which is ID11. The end of the insert tube is located against the orifice ring of the safe-end. There is a few millimetres wide gap between the insert tube and both the safe-end and the nozzle. This gap allows the necessary ejector flow from the RPV side. The sparger and the insert tube need to be installed accurately so that the gap does not close anywhere and is of the same width around these two components. Some locations of the sparger can experience thermal fatigue. However, the sparger can be repaired or exchanged during an yearly outage. The main dimensions and materials of the feedwater nozzle are presented in Table 5-2a. The safety class of these components is 1 [251].
Figure 5-2a. Overall geometry of feedwater nozzle [280].

Figure 5-2b. Overall geometry of insert tube [281].

Table 5-2a. The cross-section dimensions and materials of feedwater nozzle and connecting safe-end [256,269,271,280,282]. The nozzle dimensions are given at the region where it joins the safe-end.

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_c$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>360</td>
<td>35</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td>5</td>
<td></td>
<td>SIS-2333</td>
</tr>
<tr>
<td>buffer</td>
<td>360</td>
<td>35</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>nozzle to safe-end weld</td>
<td>360</td>
<td>35</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>safe-end</td>
<td>360</td>
<td>35</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>
Those degradation mechanisms for which the feedwater nozzle and connecting safe-end are susceptible to are presented in Table 5-2b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-2b. Those degradation mechanisms for which feedwater nozzle and connecting safe-end are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle, safe-end and welds, ferritic steel and austenitic stainless steels</td>
<td>nozzle material is susceptible, fluence level above negligible</td>
<td>nozzle material is moderately susceptible</td>
<td>thermal fatigue is possible for safe-end, elsewhere low-cycle fatigue is possible</td>
<td>safe-end and weld materials are susceptible, water environment, fluence level above negligible, significant WRSs</td>
</tr>
</tbody>
</table>

Discussion on those degradation mechanisms for which the feedwater nozzle and connecting safe-end are not susceptible to:

- Irradiation embrittlement; safe-end, weld, buffer and cladding materials are not susceptible.
- Thermal embrittlement; safe-end, weld, buffer and cladding materials are not susceptible.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the inner cladding material of the nozzle is not susceptible.
- Mechanical wear; no possibility for contacting frictional movement with connecting components.

Control rods (ID16)

A control rod is removed from or inserted into the RPV core in order to increase or decrease the neutron flux. This in turn affects the thermal power, the amount of steam produced and, hence, the electricity generated. There are altogether 121 control rods in the RPV. The control rod drives provide rapid insertion of the control rods by means of high pressure water acting on a piston tube [252]. The control rods are divided into 14 scram groups of eight or nine rods each. The control rod structure is shown in Figure 5-3. The main dimensions and materials of the control rod are presented in Table 5-3a. The safety class of these components is 2 [251]. As the blades of the control rods are not load bearing components and because they are replaced regularly, they have been excluded from further analyses.

Figure 5-3. Overall geometry of control rod [290].
Table 5-3a. The main dimensions and materials of control rods [115].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>$b$ [mm]</th>
<th>$L$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>all together</td>
<td>6383</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>absorber cross</td>
<td></td>
<td>272</td>
<td></td>
<td></td>
<td>SIS 2352</td>
</tr>
<tr>
<td>absorber plate</td>
<td>8</td>
<td>132</td>
<td>3942</td>
<td></td>
<td>SIS 2352</td>
</tr>
<tr>
<td>rod</td>
<td>70</td>
<td></td>
<td>2406</td>
<td></td>
<td>SIS 2353</td>
</tr>
</tbody>
</table>

Those degradation mechanisms for which the control rods are susceptible to are presented in Table 5-3b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-3b. Those degradation mechanisms for which control rods are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>rod and plates, austenitic stainless steel</td>
<td>FIV and acoustic resonance may cause high-cycle fatigue [24]</td>
<td>material is susceptible, water environment, for top parts fluence above negligible, low WRSs</td>
</tr>
</tbody>
</table>

Discussion on those degradation mechanisms for which the control rods are not susceptible to:
- Irradiation embrittlement; material is not susceptible.
- Thermal embrittlement; material is not susceptible.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the material is not susceptible.
- Mechanical wear; only a small possibility for contacting frictional movement with connecting components.

Core shroud, Core shroud support (ID18)

The core shroud has the following functions [292]:
- Supporting the core grid, core shroud cover and fuel assembly.
- Convey clamping force from the spring beams to the RPV bottom.
- Separation of the main circulation flow.

The core shroud support has the following functions [292]:
- Supporting all the internals above itself.
- Convey clamping force from the spring beams to the RPV bottom.
- Separation of the main circulation flow.

The core shroud and its support are shown in Figure 5-4. The main dimensions and materials of the core shroud and its support are presented in Table 5-4a. The safety class of these components is 2 [251].
Figure 5-4a. Overall geometry of core shroud [294].
Figure 5-4b. Overall geometry of core shroud support [295,296]. For the overall geometry of this component and connection to other components, see Figure 5-1.

Table 5-4a. The dimensions and materials of core shroud and its support [294,297,299,300].

<table>
<thead>
<tr>
<th>Component</th>
<th>$h$ [mm]</th>
<th>$D_r$ [mm]</th>
<th>$t$ [mm]</th>
<th>$b$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>core shroud</td>
<td>4045</td>
<td>4340</td>
<td>25</td>
<td></td>
<td>SIS 2338</td>
</tr>
<tr>
<td>core shroud stand</td>
<td>4200</td>
<td>4120</td>
<td>25</td>
<td></td>
<td>SIS 2338</td>
</tr>
<tr>
<td>bracket</td>
<td></td>
<td></td>
<td></td>
<td>10</td>
<td>SIS 2333-02</td>
</tr>
</tbody>
</table>

Those degradation mechanisms for which the core shroud and its support are susceptible to are presented in Table 5-4b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-4b. Those degradation mechanisms for which core shroud and its support are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base materials and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible, acoustic resonance may cause high-cycle fatigue [24]</td>
<td>materials are susceptible, water environment, fluence level above negligible, low WRSs</td>
</tr>
</tbody>
</table>

Discussion on those degradation mechanisms for which the core shroud and its support are not susceptible to:
- Irradiation embrittlement; materials are susceptible but the fluence remains under the onset threshold for IE.
- Thermal embrittlement; materials are not susceptible.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the materials are not susceptible.
• Mechanical wear; no possibility for contacting frictional movement with connecting components.

Core shroud support legs (ID21)

Core shroud support legs connect the core shroud support into the RPV bottom. Therefore, support legs convey the clamping force and weight of the RPV internals into RPV [292]. There are altogether 18 core shroud support legs, in 6 groups of 3 support legs next to each other. One core shroud support leg is shown in Figure 5-5. The main dimensions and materials of the core shroud support legs are presented in Table 5-5a. The safety class of these components is 2 [251].

![Figure 5-5. Geometry of core shroud support legs [295,296]. The arrow in the figure points from the leg close-up to the location of the leg supporting the core shroud support from below.](image)

Table 5-5a. The dimensions and materials of core shroud support legs [298].

<table>
<thead>
<tr>
<th>Component</th>
<th>h [mm]</th>
<th>Dc [mm]</th>
<th>r [mm]</th>
<th>b [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>support leg</td>
<td>450</td>
<td>25</td>
<td>220</td>
<td></td>
<td>Inconel 600, SIS 2338</td>
</tr>
</tbody>
</table>

Those degradation mechanisms for which the core shroud support legs are susceptible to are presented in Table 5-5b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-5b. Those degradation mechanisms for which core shroud support legs are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible</td>
<td>materials are susceptible, water environment, significant WRSs</td>
</tr>
</tbody>
</table>
Discussion on those degradation mechanisms for which the core shroud and its support are not susceptible to:
- Irradiation embrittlement; materials are not susceptible, neutron fluence is negligible.
- Thermal embrittlement; materials are not susceptible.
- IASCC; neutron fluence is negligible.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the materials are not susceptible.
- Mechanical wear; no possibility for contacting frictional movement with connecting components.

Cylindrical RPV shell including welds (ID24)

The functions of the RPV are to safely contain the reactor core and internal components, act as a pressure boundary, and provide a physical barrier in the very unlikely case of more severe accident involving nuclear fuel. All major pipe nozzles are located above the top of the core, to ensure that the core is kept flooded in the event of a pipe rupture in the primary systems. The RPV hangs on top of the biological shield by means of a welded-on support skirt. The RPV support skirt is located near the primary system pipe connections, which arrangement minimizes the pipe stresses resulting from the thermal expansion of the vessel [252]. The cylindrical RPV shell is shown in Figure 5-6. The main dimensions and materials of the cylindrical RPV shell are presented in Table 5-6a. The safety class of this component is 1 [251]. The RPV shells have been manufactured from rolled plates (with longitudinal welds). There are both circumferential and longitudinal welds in the RPV.

Figure 5-6. Geometry of cylindrical RPV shell [271], see the arrow for location of the shell.
Table 5-6a. The cross-section dimensions and materials of cylindrical RPV shell [269,271].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV base material</td>
<td>5818</td>
<td>134</td>
<td>ASTM 533 Grade B, Class 1</td>
</tr>
<tr>
<td>RPV cladding</td>
<td>5</td>
<td></td>
<td>SIS 2333</td>
</tr>
</tbody>
</table>

Those degradation mechanisms for which the cylindrical RPV shell are susceptible to are presented in Table 5-6b. The exclusion of other relevant degradation mechanisms is discussed after that.

Table 5-6b. Those degradation mechanisms for which cylindrical RPV shell is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is susceptible, fluence level above negligible</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

Discussion on those degradation mechanisms for which the cylindrical RPV shell is not susceptible to:
- IGSCC; materials are not susceptible.
- IASCC; materials are not susceptible.
- Thermal embrittlement; cladding material is not susceptible.
- General corrosion; conditions are unfavourable.
- Erosion-corrosion, FAC; the inner cladding material of the RPV is not susceptible.
- Mechanical wear; no possibility for contacting frictional movement with connecting components.

5.2 Summary on component specific susceptibility

A summary on component specific susceptibility of BWR RPV and its internals to relevant degradation mechanisms is presented in the following. The summary is based on the detailed information presented in Section 5.1 and Appendix C. Many of these components are fabricated of more than one material. Here, the information is compressed so that the components are treated as a whole. The summary on the susceptibility to degradation mechanisms is presented in Table 5.2-1. For completeness, it includes also fuel assembly (ID15), but as it is not a load bearing structure the associated information is purposefully excluded. Moreover, ID15 is beyond the scope of this work as dealing with it would require resorting to particle physics. In most cases, the presented component specific susceptibility to degradation is quite or very low, e.g. in theory all metallic NPP components are susceptible to fatigue, but due to small number of load cycles and/or low load cycle amplitudes this degradation mode is very unlikely to initiate or propagate. Table 5.2-1 includes also the safety classification of the considered components [251].
Table 5.2-1. Summary on component specific susceptibility of BWR RPV and its internals to relevant degradation mechanisms. Here, susceptibility is denoted with "X".

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>Degradation mechanism</th>
<th>Safety class</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Flange cooling spray piping</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>2</td>
<td>Long nozzle pipes in cooling spray piping</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>3</td>
<td>Evacuation pipe</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>4</td>
<td>Spring beams and support brackets</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>5</td>
<td>Steam dryer</td>
<td>X</td>
<td>3</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>7</td>
<td>Steam separator stand pipes</td>
<td>X</td>
<td>3</td>
</tr>
<tr>
<td>8</td>
<td>Steam separator pipe bundles</td>
<td>X</td>
<td>3</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator supports</td>
<td>X</td>
<td>2</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>X X X X X</td>
<td>1</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>X X X</td>
<td>3</td>
</tr>
<tr>
<td>12</td>
<td>Boron spray nozzles and piping</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>13</td>
<td>Core spray piping outside core shroud cover</td>
<td>X</td>
<td>2</td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>X</td>
<td>2</td>
</tr>
<tr>
<td>15</td>
<td>Fuel assembly</td>
<td>- - - - - - - - - - - - -</td>
<td>1</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>20</td>
<td>Main circulation pump nozzles</td>
<td>X X X</td>
<td>1</td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>X X</td>
<td>2</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>X X X</td>
<td>2</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>X X X</td>
<td>1</td>
</tr>
<tr>
<td>25</td>
<td>RPV bottom</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>26</td>
<td>RPV support skirt</td>
<td>X</td>
<td>1</td>
</tr>
<tr>
<td>27</td>
<td>RPV flange</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>28</td>
<td>RPV-head</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>29</td>
<td>RPV-head bolts</td>
<td>X X</td>
<td>1</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>X X X X X X</td>
<td>1</td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>X X X X X X</td>
<td>1</td>
</tr>
</tbody>
</table>
BWR RPV and internal specific loads

BWR RPV and its internals are in contact with hot and moving liquid coolant during normal plant operations. The coolant saturation temperature corresponding to the system pressure is just below 290 ºC [252]. Internal components located in the vicinity of the core are also exposed to fast neutron fluxes ($E > 1.0$ MeV) and gamma irradiation. The operating environment inside a BWR RPV generates many loads that are considered to propagate ageing related or time dependent degradation mechanisms.

Abnormal operating events can impose much more severe conditions on reactor components. However, as such events are very unlikely to occur, they will not be investigated in this study where the emphasis is on the assessment of ageing effects under normal plant operations. These operations constitute most or all of NPP operating histories.

6.1 On loads in BWR environments

A general description of the loads in BWR environments is presented in the following. In terms of components, the scope concerns BWR RPV and its internals. However, many of the loads, such as transient plant operations, concern also piping systems and other structures.

To be able to understand the BWR loading conditions it is necessary to know about the process of normal operation. A common feature for all BWR plants is that the sub-cooled water enters the RPV through the feedwater nozzles, and after it has been heated, it exits the RPV as steam through the steam outlet nozzles at saturation temperature. During operation the nominal pressure is 70 bar. The steam produced in the reactor is directly utilized in the turbine. As to the internal configuration of the RPV, it includes an annular downcomer, where the sub-cooled feedwater mixes with the saturated liquid at the lower plenum region, while the core consists of bounded fuel elements, where the phase change to steam occurs. The steam dryer removes the remaining liquid from the heated water. Thus, only dry steam goes forward [342]. In Olkiluoto units OL1 and OL2, the feedwater temperature is 185 ºC, and the live steam temperature at the turbine is 283 ºC [252]. The average core inlet flow velocity is approximately 2.1 m/s [5].

6.1.1 Applied loads

The applied loads are mechanical and thermal. They are associated with normal stationary and transient plant operations, where typical examples of the latter type are start-up and shutdown. External mechanical loads include static differential pressure loadings, pre-loads in bolts, and hydro-dynamic forces produced by coolant flows inside the RPV.

Flow induced hydro-dynamic forces can be static or oscillatory. The static hydro-dynamic forces are usually referred to as lift and drag forces. Oscillatory hydro-dynamic forces are caused by flow separations. Pump induced pressure pulsations can also act as periodic external excitations to the RPV internals. In the BWR environment, oscillatory hydro-dynamic forces are a major concern because due to them a component can start to vibrate. FIV is a potential cause for fatigue degradation of BWR RPV internals.

Thermal loads from thermal transients induce temperature gradients through the component walls. This causes thermal stresses and strains due to differing thermal expansion properties of component materials, and by restricted thermal expansion. BWR RPV internals are designed to
accommodate differential thermal expansions. As a result, constraint induced steady-state thermal loads remain relatively low. Some internals are exposed to the mixing of fluid flows at different temperatures [343]. The mixing actions produce rapid temperature changes in the component surface. These thermal cyclic loads can lead to the development of alternating stresses and fatigue induced cracks.

6.1.2 Environmental loads

The major environmental loads imposed on a BWR RPV and its internals are induced by their contact with the hot and potentially corrosive coolant. The corrosiveness of the coolant is controlled primarily by the presence of dissolved oxygen in the reactor cooling water. Dissolved oxygen is a product of radiolytic reactions in the core. The level of dissolved oxygen in the BWR cooling water, being from 100 to 300 ppb, is sufficient to produce the electro-chemical driving force needed to promote SCC in sensitized austenitic stainless steel components [5]. The electro-chemical potential of this steel type is increased by the build-up of the dissolved oxygen content in the coolant water, which in turn increases susceptibility to corrosion. Type 304 stainless steel, the most common construction material of the internals, is susceptible to SCC in the sensitized condition. The presence of other impurities, such as chlorides, may accelerate the corrosion process.

The exposure to fast neutron fluxes \((E > 1 \text{ MeV})\) is a significant environmental stressor for the internals. The effects are most noticeable for components located close to the core.

The relatively high operating temperature can produce physical changes and decrease of fracture toughness in some reactor internal components, in particular those of CASS [344].

6.1.3 Manufacturing induced loads

The methods for fabricating the BWR RPV and its internals may impose loads on these components. Manufacturing induced loads can also promote the onset and propagation of degradation mechanisms. Welding and cold working are two common manufacturing methods used in fabrication of components, which also introduce stresses to them.

The conventional welding process sensitizes austenitic stainless steels, such as type 304, and makes them susceptible to corrosion. WRSs in welds and heat-affected zones (HAZs) contribute to the initiation and propagation of SCC [5]. For welds of a BWR RPV and its internals, there is seldom available applicable measured WRS data. In addition, the accuracy of the WRS measurement techniques varies. In order to obtain accurate enough WRS data for a specific weld, several measurements with more than one technique would have to be made. In addition, all these techniques are material destructive. This would be extremely expensive and time consuming, so this option is seldom resorted to. Instead, the WRSs can be computed with FE analyses by simulating the welding process, or they can be taken from fitness-for-service (FFS) handbooks. FE simulation of WRSs is quite difficult and computationally laborious, requiring user defined code development, as no commercially available general purpose FE code is capable for this purpose as such. As for WRS recommendations in the FFS handbooks, they are always conservative, and in several cases overly so. To give an impression of the WRSs in the NPP component welds, in the inner surface of pipes they are in axial direction usually tensional and often of the scale of the material yield strength, while decreasing towards the outer surface and often turning to compression there. A comparison of WRS recommendations in commonly used FFS handbooks applied to BWR component welds is presented in VTT research report VTT-R-02200-10 [345] by the author of this work.
Cold working introduces new manufacturing induced stresses. Plastic strains are accumulated in the component during the process, and excessive plastic strain accumulation can lead to the development of cracks. Contact with a hot and corrosive coolant may accelerate the crack initiation process. Evidence also suggests that cold working can induce formation of martensite in the component. Martensite may promote the material sensitization process and make a cold worked component more susceptible to SCC [346].

6.2 Load types and affected components

For design and analyses concerning BWR NPPs, the categorization of loads most often follows the ASME code. In the top level, the loading conditions for NPPs are categorised according to Article NCA-2000 of ASME Section III [147] to design, service and test loadings and limits. As the scope here concerns BWR NPPs in service, the categorization of service loadings is of most interest.

6.2.1 Mechanical and thermal loads

For most of the time the BWR plants are under 100 % normal operation. That is a quasi-stationary loading condition, and the associated values for pressure, temperature and average flow rate are already described in Section 6.1. A commonly used estimate for yearly time in operation is 8000 hours, which corresponds to approximately 11 months. If there are no exceptional plant events BWR plants are shutdown once a year for refuelling and inspections. For a detailed descriptions on inspections, see Appendix B.

Thus, the most common yearly transient load cases, i.e. thermal transients, are shutdown and start-up. For BWR RPVs there are also other anticipated load transients. Mainly they concern the feedwater system, which provides water to the downcomer of the RPV, but other systems produce some load transients to the RPV as well. During a load transient, temperature, pressure and flow rate alter as a function of time. All load transients start from and end to steady-state. For instance, during shutdown temperature and pressure decrease slowly from operational values to room temperature (RT) and 1 bar, whereas during start-up this is reversed, i.e. these load parameters return to the operational level. The load transients enter the BWR RPV through the main nozzles, most notably the feedwater nozzle. Most load transients are caused by the RPV downcomer taking place between the RPV and core shroud, where they distribute and mix with the surrounding fluid. When reaching the RPV bottom, the RPV downcomer has mostly evened out and is not capable to create thermal gradients to the adjacent components. For some load transients, the altering of temperature is fast enough to cause temperature gradients and consequent thermal stresses to the component wall. As for the RPV bottom, the main circulation pump nozzles and control rod drive nozzles located there produce some load transients too.

For the OL1 and OL2 units, 31 different anticipated significant load transients have been identified for the feedwater nozzles. Whereas for the OL1/OL2 core spray nozzles only 9 such load transients have been identified.

According to earlier editions of ASME Section III, the operating conditions are divided into the following five categories, depending on the severity of the thermal transient and the number of occurrences:

a) normal conditions,
b) upset conditions,
c) emergency conditions,
d) faulted conditions, and
e) testing conditions.

Later editions of ASME Section III [147] clarified this nomenclature, but basically retained the same allowable stresses. The corresponding more recent categories are:
a) Service Level A, corresponds to normal conditions,
b) Service Level B, corresponds to upset conditions,
c) Service Level C, corresponds to emergency conditions,
d) Service Level D, corresponds to faulted conditions.

For more detailed descriptions of these loading conditions, see Article NH-3000 of ASME Section III [147]. Normal conditions are those which exist during normal running of the plant. Upset conditions are deviations from the normal conditions but are anticipated to occur often enough that provisions for them must be made in the analysis. These transients are those that do not result in a forced outage, or if a forced outage occurs, the restoration of power does not require mechanical repair. Emergency conditions are deviations from the normal state, requiring shutdown, possibly requiring a repair and must be considered in order to ensure no gross loss of structural integrity occurs. Faulted conditions are deviations from normal with extremely low probability but may result in loss of integrity and operability of a system. Testing conditions are pressure overload tests or other tests on the primary system [117].

The load transients produce operational cycles, i.e. load cycles which start from and end to the same stationary state, i.e. reference state. This state can be 100 % normal operation or hot standby (HSB), as they are the normal operational conditions in which BWRs reside most of their operational lifetimes. The load cycles are composed either of one or in chronological order of two or more load transients, most often of two. A typical example of this kind of load cycle is to go from the operational conditions to room temperature and pressure with load transient shutdown, and after some time return back to the operational conditions with load transient start-up. In Asea-Atom BWR plants the duration of both of these transients is approximately six hours and the time between them in shutdown state can last days or weeks.

For the 3rd generation Asea-Atom BWR units, such as OL1 and OL2, the normal water level inside the RPV is approximately in the midway of steam separator stand pipes, see Figure 5-1. The same system pressure and temperature as in the water dome also prevail in the steam dome, with the difference that the loading in the latter is produced by the steam.

Welding of NPP components causes WRSs. This issue is already described in detail in Section 6.1.3.

Pre-tension of internals is another applied mechanical load in the 3rd generation Asea-Atom BWR units. The pre-tensioning is achieved with RPV-head spring beams (ID4). When after an outage the RPV-head is attached back to its place by fastening the altogether 60 RPV-head bolts, the spring beams press the RPV internals, thus creating to them compression by the pre-tensioning [350].

The dead weights of the RPV and its internals are calculated based on the dimensions given in the design drawings and on known material densities.

Of the RPV and its internals the components exposed to mechanical loads are:
- 100 % normal operation; IDs 1-31.
- Load transients; IDs 1-13, 18-25, 27, 28, 30, 31.
- WRSs; IDs 1-3, 6-14, 16-26, 28, 30, 31.

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### 6.2.2 Irradiation loads

Available information concerning approximate neutron fluence for some representative BWR RPV internals of GE, Japanese, Siemens and Asea-Atom BWR NPPs is shown in Table 6.2.2-1. The accuracy of this data varies considerably because part of it is measured and the rest is simulated. Moreover, the older the simulated fluence data, the less accurate it is. This is because the simulation procedures have developed considerably over the years of NPP era.

Table 6.2.2-1. Accumulated fluence for some representative BWR RPV internals after 40 years of plant operation [1]. The accuracy of this data varies considerably, as elaborated above.

<table>
<thead>
<tr>
<th>BWR type and component</th>
<th>Fluence</th>
<th>( E &gt; 0.1 \text{ MeV (n/m}^2) )</th>
<th>( E &gt; 1 \text{ MeV (n/m}^2) )</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>GE BWR/3</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core plate</td>
<td>-</td>
<td>2.0E+24</td>
<td></td>
</tr>
<tr>
<td>Core shroud</td>
<td>-</td>
<td>2.7E+24</td>
<td></td>
</tr>
<tr>
<td>Top guide</td>
<td>-</td>
<td>4.0E+24</td>
<td></td>
</tr>
<tr>
<td><strong>GE BWR/6</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core plate</td>
<td>-</td>
<td>1.7E+24</td>
<td></td>
</tr>
<tr>
<td>Core shroud</td>
<td>-</td>
<td>1.0E+25</td>
<td></td>
</tr>
<tr>
<td>Top guide</td>
<td>-</td>
<td>1.1E+25</td>
<td></td>
</tr>
<tr>
<td><strong>Japanese BWR/4</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Plate (top)</td>
<td>3.6E+24</td>
<td>2.1E+24</td>
<td></td>
</tr>
<tr>
<td>Core shroud (H4)</td>
<td>1.3E+25</td>
<td>7.4E+24</td>
<td></td>
</tr>
<tr>
<td>Top guide (bottom)</td>
<td>1.6E+26</td>
<td>7.2E+25</td>
<td></td>
</tr>
<tr>
<td><strong>Japanese BWR/5</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Plate (top)</td>
<td>5.9E+24</td>
<td>3.2E+24</td>
<td></td>
</tr>
<tr>
<td>Core shroud (H4)</td>
<td>1.9E+25</td>
<td>1.0E+25</td>
<td></td>
</tr>
<tr>
<td>Top guide (bottom)</td>
<td>1.1E+26</td>
<td>5.4E+25</td>
<td></td>
</tr>
<tr>
<td><strong>Siemens Reactors (Type 72)</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core plate</td>
<td>-</td>
<td>&lt; 2.0E+25</td>
<td></td>
</tr>
<tr>
<td>Core shroud</td>
<td>-</td>
<td>&lt; 3.0E+25</td>
<td></td>
</tr>
<tr>
<td>Top guide</td>
<td>-</td>
<td>&lt; 1.0E+26</td>
<td></td>
</tr>
<tr>
<td><strong>Asea-Atom BWR (Generation 3)</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core plate</td>
<td>-</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core shroud</td>
<td>-</td>
<td>2.2E+25</td>
<td></td>
</tr>
<tr>
<td>Top guide</td>
<td>-</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The RPV beltline region of the cylindrical wall and some RPV nozzles have to be considered for significant embrittlement ageing [117].

For the following RPV internals the cumulative fluence after 60 years will be less than 10E+21 n/m² \( (E > 1.0 \text{ MeV}) \), or the components are made of material (stainless steel or Alloy 182) that is less susceptible to neutron embrittlement [117]:

- attachment welds,
- nozzles,
- top head,
- bottom head,
- penetrations,
- vessel flange,
• closure studs,
• safe-ends.

Of the OL1/OL2 RPV and its internals the components exposed to irradiation loading are:
• IDs 1-25, 27, 28, 30, 31.

6.2.3 Dynamic loads
All loads can be divided into the two broad categories of static and dynamic [117]. The distinction between these two load categories is based primarily on the comparison of the time span of the load variation to the response time of the structure. The most significant dynamic loads for the BWR RPV and its internals are seismic loads and FIV, and they are described in the following.

The seismic activity in Scandinavia is low. Historically, there are only a few registered events which might have caused damage to any industrial facility [351]. Presently, there are both international and domestic codes/standards applicable to determining seismic loads for NPP structures.

SS-EN 1998-1:2004; Eurocode 8 [352] has been in force since 2009 and applies to design and construction of buildings and civil engineering works in seismic regions in Europe. According to ref. [352], structures in seismic regions are recommended to be designed and constructed to withstand a design seismic action associated with a reference probability of exceedance of 10 % in 50 years or a reference return period of 475 years. The hazard is described in terms of the value of the reference peak ground acceleration (PGA) which can be derived from zonation maps in National Annex.

IAEA Safety Guides (SGs) present certain basic requirements concerning design basis ground motions. Section 2 of IAEA SG-G-1.6 [353] states that two levels of ground motion hazard should be evaluated for a NPP, those being denoted as SL-1 and SL-2. Of those, SL-1 corresponds to a level with a mean annual exceedance frequency of 1.0E-02 while SL-2 corresponds to a mean annual exceedance frequency of 1.0E-03 to 1.0E-04, or a median annual exceedance frequency in the range of 1.0E-04 to 1.0E-05. Additionally, also for the SL-2 level the PGA should be at least 10 % of gravitational acceleration regardless of the actual seismic hazard. According to ref. [353] SL-1 operating basis earthquake (OBE) corresponds to a seismic hazard level with a mean annual exceedance frequency of 1.0E-02, as stated in Section 3.2.1. To present an example comparable to Finland, the curves of annual seismic hazard at 16 %, 37 %, 50 % (median), 63 % and 84 % fractiles and the mean for the region with the most severe hazard in Sweden are shown in Figure 6.2.3-1. Based on data in Figure 6.2.3-1, the mean PGA is approximately 0.15 m/s² = 0.015×g where g is the gravitational acceleration. However, for seismic conditions in Finland this data is considerably conservative.

According to Finnish Guide YVL B.2 [355] the systems, structures and components of nuclear facilities shall be assigned to three categories, S1, S2A and S2B, based on the seismic resistance requirements set for them. According to Guide YVL E.4 [354], the dynamic effects of a design basis earthquake shall be analysed by seismic analysis conforming to a standard applicable to seismic category S1 pressure equipment and their supports in accordance with the Guide YVL B.2 [355]. Best estimate methods can be applied to seismic category S2A pressure equipment. The loading condition data comprise the floor response resolved by analysing the dynamic behaviour of buildings. Its variations between different supporting points shall be analysed if significant additional stresses result from them. According to Guide YVL B.7 [356], the vertical
and horizontal PGA values used shall be justified on a site-specific basis. The required minimum value of the horizontal component is \(0.1 \times g\), as prescribed in the Guides IAEA NS-G-1.6 [353] and IAEA SSG-9 [357].

Fluid flow can induce structural and mechanical oscillations. FIV is the dynamic response of structures immersed in or conveying fluid flow. The significant excitation mechanisms for FIV are as follows [24]: fluid elastic instability, vortex shedding, turbulence buffeting, acoustic resonance, pressure pulsations and axial leakage.

Fluid instability is by far the most important vibration mechanism for tube bundles subjected to cross flow. Fluid elastic instability is usually not a problem for nuclear components in axial flow [359].

Vortex shedding is a periodic flow phenomenon. It exists only in a cross flow. In practical flow conditions, a pipe is usually inclined to the flow which reduces the effectiveness and strength of vortex shedding as an excitation mechanism [360].

Turbulence excitation, or turbulence buffeting, is caused by turbulence acting on a structure. Particularly, turbulence excitation is an important vibration excitation mechanism for a two-phase flow [361]. Accurate prediction of turbulence induced vibration is difficult because of the randomness of the phenomenon. Turbulence flows are the sum of the components that oscillate with many different frequencies. Therefore, the excitation frequency of the turbulence flow is also broad banded.

Acoustic resonance occurs usually in gas flows. In BWR main steam lines, acoustic resonance is produced by the interaction between the flow field and an unstable shear layer across the closed stub pipes of safety relief valves (SRVs) or main steam isolation valves (MSIVs). Fluctuating pressure or sound is generated by the unsteady motion of vortices. If the vortex shedding frequency is close to the resonance frequency, the former becomes locked at the latter [24].
Flow pulsations are oscillations (acoustic pulses) generated by pumps or fans. They may produce a general excitation mechanism for a BWR, particularly in the downcomer region and lower plenum due to pressure fluctuations generated by the main circulation pumps [362].

Instability caused by axial leakage flow usually occurs in very narrow channels subjected to the phenomenon while being bounded by flexible structural boundaries [363,364].

Excitation mechanisms of the FIV are usually studied in a single-phase flow with simple geometries. The results are usually based on semi-empirical coefficients and have limited validity. Therefore, it is difficult to analyse theoretically the potential risk of FIV to RPV internals [24].

Of the RPV and its internals, the components exposed to dynamic loads are:
- seismic loads; IDs 1-14, 16-31.
- FIV [24]; IDs 1-4, 7-9, 11-13, 16-18, 22, 23.

6.2.4 Process-chemical loads
The important parameters of the BWR primary coolant chemistry are conductivity, pH level, dissolved oxygen, sulphate and chloride. The BWR coolant is a high purity electrolyte. Therefore, conductivity is very low [1].

EPRI report BWRVIP-79 [367] presents guidelines for BWR primary coolant system water chemistry. The NWC guideline is followed in OL1 and OL2 units [1].

For German BWRs the VGB Guideline for the water in LWRs [368] specifies the qualitative feedwater and reactor water requirements. This guideline was revised in 1996. Experience of corrosion cracking in steels 1.4541 (pipe work) and 1.4550 (core shroud region) and the EPRI Guideline were taken into account for this revision. However, there are some discrepancies between this guideline and the EPRI Guideline. For example, the VGB Guideline does not specify HWC or HWC + NMCA operational conditions (and the related ECP estimation) due to the differently structured materials concept, i.e. use of stabilized stainless steels.

Of the RPV and its internals, the components exposed to process-chemical loads are:
- IDs 1-3, 5, 7-14, 16-25, 30, 31.

6.2.5 Acoustic loads
In some operating BWR plants, low order acoustic resonances within SRVs and safety valve (SV) standpipes locked in to flow induced shear layer instability modes over the standpipe openings generate extremely powerful acoustic pulsations within the main steam lines (MSLs). These pulsations subsequently propagate along the MSL and into the RPV, impacting the steam dryers [369].

According to report EPRI-BWRVIP-181A [370], the acoustic loads have been shown to be significant in certain cases. Then, the fluctuating pressure loads on the steam dryer, induced by acoustic excitation in the MSL safety relief valve inlet stand pipes may create an alternating stress that can exceed fatigue limits during normal operation.

Recent experience from several BWRs, concerning particularly those operating at extended power uprate (EPU) conditions with high steam flow velocities, have shown significant degradation in the steam dryer caused by acoustic loads [370].
Following a TVO report on OL1/OL2 RPV loads, also the feedwater spargers, control rods, control rod guide tubes, core shroud, core shroud support and pump deck are susceptible to acoustic loads.

Of the OL1/OL2 RPV and its internals, the components exposed to acoustic loads are:
- IDs 5, 11, 16-19, 23.

6.3 Summary on loads

A summary of various significant load types and affected components of a BWR RPV and its internals is presented in the following, see Table 6.3-1. The summary is based on the detailed information presented in Section 6.2. For completeness, this summary includes also the fuel assembly (ID15), but in terms of analyses it is beyond the scope of this work. In a number of cases, several load types can act on a component simultaneously, e.g. temperature, pressure and irradiation for the RPV wall. For many cases, the level of loading is very low, e.g. for parts of RPV and its internals located in the steam dome the effect of irradiation is in practise negligible.
Table 6.3-1. Summary on significant load types and affected components of OL1/OL2 RPV and its internals. Here, affected components are denoted with "X".

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>Load type</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Mechanical &amp; thermal</td>
</tr>
<tr>
<td>1</td>
<td>Flange cooling spray piping</td>
<td>X</td>
</tr>
<tr>
<td>2</td>
<td>Long nozzle pipes in cooling spray piping</td>
<td>X</td>
</tr>
<tr>
<td>3</td>
<td>Evacuation pipe</td>
<td>X</td>
</tr>
<tr>
<td>4</td>
<td>Spring beams and support brackets</td>
<td>X</td>
</tr>
<tr>
<td>5</td>
<td>Steam dryer</td>
<td>X</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>X</td>
</tr>
<tr>
<td>7</td>
<td>Steam separator stand pipes</td>
<td>X</td>
</tr>
<tr>
<td>8</td>
<td>Steam separator pipe bundles</td>
<td>X</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator support legs</td>
<td>X</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>X</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>X</td>
</tr>
<tr>
<td>12</td>
<td>Boron spray nozzles and piping</td>
<td>X</td>
</tr>
<tr>
<td>13</td>
<td>Core spray piping outside core shroud cover</td>
<td>X</td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>X</td>
</tr>
<tr>
<td>15</td>
<td>Fuel assembly</td>
<td>X</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>X</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>X</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>X</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>X</td>
</tr>
<tr>
<td>20</td>
<td>Main circulation pump nozzles</td>
<td>X</td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>X</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>X</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>X</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>X</td>
</tr>
<tr>
<td>25</td>
<td>RPV bottom</td>
<td>X</td>
</tr>
<tr>
<td>26</td>
<td>RPV support skirt</td>
<td>X</td>
</tr>
<tr>
<td>27</td>
<td>RPV flange</td>
<td>X</td>
</tr>
<tr>
<td>28</td>
<td>RPV-head</td>
<td>X</td>
</tr>
<tr>
<td>29</td>
<td>RPV-head bolts</td>
<td>X</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>X</td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>X</td>
</tr>
</tbody>
</table>
7 Screening process

For NPPs, the purpose of a screening process is to select the components for further analyses, especially for degradation potential analyses. The objective of these analyses is to assess the remaining lifetime of the components, and to find suitable means to prevent or mitigate the effects of ageing degradation.

The degradation potential analyses should be focused on those components that are the most significant ones from the safety point of view. This is realised by carrying out an applicable screening process. After the components are selected, the analysis methods for ageing detection and prediction are chosen. The suitability of a procedure depends on the selected components and on the available data.

There are a number of applicable screening procedures for NPP components. Two significant ones are the IAEA procedure [371] and the EPRI procedure [372].

The IAEA Safety Guide 50-SG-D1 [373] provides guidance in the ranking of the various safety functions that can be used to prioritise NPP components for ageing degradation assessments. U.S. NRC has developed a risk assessment approach with which plant components can be prioritised on the basis of their degradation induced contribution to the core damage frequency and/or public health risk [374]. This approach, which is based on Taylor expansion and use of risk measures, relates the change in individual component unavailabilities due to degradation to the change in the overall NPP risk. MRP-175 [30] provides screening criteria thresholds for evaluating each degradation mechanism. There is a distinct difference between threshold values for measuring the onset of degradation and screening values for evaluating the significance of degradation mechanisms. The following are the working definitions according to report MRP-134 [375]:

- **Threshold value**: The level of susceptibility when a degradation effect is first quantifiable.
- **Screening value**: The level of susceptibility when a degradation effect may be significant to functionality.

However, the application of the IAEA procedure [371] and the EPRI procedure [372] as such would require the availability and treatment of a large amount of component specific technical data as well as expert panel reviews. This would be partly beyond the scope of this work, and also beside the point, as the focus here is not on the screening process itself but on the susceptibility to and analyses concerning degradation of certain NPP components. In addition, here the scope of the considered components is already strictly limited. Thus, a more straightforward screening process was developed and applied.

7.1 Developed screening procedure

The screening procedure for OL1/OL2 RPV and its internals for ageing degradation analyses is described in the following. The scope of the screening is limited to concern the 31 components described in Section 5, see Figure 5-1. As the IAEA screening procedure [371] concerns all NPP components the screening process used here follows mainly the EPRI screening procedure [372], which is focused on RPV internals. A detailed full scale screening process was beyond the scope of this work, as such would involve much more resources as well as co-operation and work of several NPP experts with limitless access to plant specific data.
confidential technical documentation. However, to obtain expert judgement support for the performed screening process it was discussed with experts from TVO.

**Data compilation and review**

The individual components and the conditions under which each component operates are characterized in the following. The necessary data sources include Section 5 of this work, IMTs in BWRVIP-167NP [377] and screening criteria values in MRP-175 [30]. Additional information was solicited from TVO experts. The collected data was reviewed for accuracy and completeness.

The summary on necessary data categories and sources for the screening process of OL1/OL2 RPV and its internals is:

- material types; see Section 5,
- geometry and dimensions; see Section 5,
- susceptibility to degradation mechanisms; see Section 5 and BWRVIP-167NP [377],
- loads; see Section 6,
- neutron fluence; more accurate data for OL2 unit from proprietary ref. [378] is used,
- process chemistry; see Section 6,
- screening data; from MRP-175 [30] and NUREG-1801, Revision 2 [128], Chapter IV.

**Screening of components**

The screening approach applied here is a modification of that presented in MRP-191 [372]. In that screening process, stress and CUF data are used already at the first step. Here, the screening process is divided into two steps, denoted Step 1 and Step 2. Load data is used in Step 1 instead of stress and CUF data. As the stress and CUF analyses are laborious to perform, one purpose of the screening Step 1 is to identify the components that require those analyses. The number of the components to screen in according to Step 1 will therefore be larger than would result through the MRP-191 [372] screening process. The ensuing screening Step 2 includes the stress and CUF analyses. It is expected that more components will screen out in Step 2 due to component specific stress and/or CUF values being below the corresponding screening values given in MRP-175 [30]. A flow chart of the developed screening process is presented in Figure 7.1-1. The results from the screening Step 1 are presented in Table 7.2-1. The results from the screening Step 2 are presented in Section 8.3. All in all, the developed screening procedure with the two steps mostly covers the procedure presented in MRP-191 [372].

The IMTs in BWRVIP-167NP [377] are for U.S. BWR types and screening data in MRP-175 [30] are for PWR NPPs. The applicability of the data these two documents contain to OL1/OL2 RPV and its internals is carefully weighed and analysed.

For a component to screen in according to Step 1, several criteria are considered simultaneously. A component screens in according to Step 2 when one or more of the degradation mechanism specific screening values is reached or exceeded.
7.2 Results from preliminary screening

The results from the preliminary screening of OL1/OL2 RPV and its internals for ageing degradation analyses are presented in Table 7.2-1. It includes also the safety classification of the considered components [251].

For brevity, the detailed component specific screening analyses that were done are not described here. The main screening rules in screening process Step 2 are:

- When a component is clearly susceptible to one or more degradation mechanisms it usually screens in.
- When there is a clear susceptibility to one or more degradation mechanisms but the stresses are low and/or environment is unfavourable to the degradation mechanism in question the component often screens out.
- When there is a low susceptibility to one or more degradation mechanisms and the stresses are high and environment is favourable to the degradation mechanism in question the component often screens in.
- When there is a low susceptibility to one or more degradation mechanisms and the stresses are between low and high and environment is favourable to the degradation mechanism in question and the Safety class is 1 the component often screens in.
Table 7.2-1. The results from the screening process Step 1 for the considered OL1/OL2 components. The names of the components corresponding to IDs are given in Table 5-1. For fluence, data after 60 EFPY is used. Notations: high significance = Yes, low significance = Low, negligible = No, and blank cell means that the issue does not concern at all.

<table>
<thead>
<tr>
<th>ID</th>
<th>Degradation susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Irradiation embrittlement</td>
</tr>
<tr>
<td>1</td>
<td>Low</td>
</tr>
<tr>
<td>2</td>
<td>Low</td>
</tr>
<tr>
<td>3</td>
<td>Low</td>
</tr>
<tr>
<td>4</td>
<td>Yes</td>
</tr>
<tr>
<td>5</td>
<td>Low</td>
</tr>
<tr>
<td>6</td>
<td>Low</td>
</tr>
<tr>
<td>7</td>
<td>Low</td>
</tr>
<tr>
<td>8</td>
<td>Low</td>
</tr>
<tr>
<td>9</td>
<td>Low</td>
</tr>
<tr>
<td>10</td>
<td>Yes</td>
</tr>
<tr>
<td>11</td>
<td>Yes</td>
</tr>
<tr>
<td>12</td>
<td>Low</td>
</tr>
<tr>
<td>13</td>
<td>Low</td>
</tr>
<tr>
<td>14</td>
<td>Low</td>
</tr>
<tr>
<td>15</td>
<td>Low</td>
</tr>
<tr>
<td>16</td>
<td>Low</td>
</tr>
<tr>
<td>17</td>
<td>Low</td>
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<tr>
<td>18</td>
<td>Low</td>
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<tr>
<td>19</td>
<td>Low</td>
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<tr>
<td>20</td>
<td>Low</td>
</tr>
<tr>
<td>21</td>
<td>Low</td>
</tr>
<tr>
<td>22</td>
<td>Low</td>
</tr>
<tr>
<td>23</td>
<td>Low</td>
</tr>
<tr>
<td>24</td>
<td>Yes</td>
</tr>
<tr>
<td>25</td>
<td>Yes</td>
</tr>
<tr>
<td>26</td>
<td>Low</td>
</tr>
<tr>
<td>27</td>
<td>Yes</td>
</tr>
<tr>
<td>28</td>
<td>Yes</td>
</tr>
<tr>
<td>29</td>
<td>Low</td>
</tr>
<tr>
<td>30</td>
<td>Low</td>
</tr>
<tr>
<td>31</td>
<td>Yes</td>
</tr>
</tbody>
</table>

78
The supplementary remarks concerning the screening process Step 1 are:
- Otherwise the load classification would be more often “No”, but due to locally high WRSs and/or FIV the load class for some components changed from “No” to “Yes”.
- Some components with load classification as “No” have screened in because they may show locally high stresses due to geometry discontinuities, such as abrupt change of wall thickness and/or locations with small radius. The effect of the geometry discontinuities remains to be seen from the results of the FE stress/strain analyses described in Section 8.
- IDs 5, 7 and 8 have been screened out because their safety class is 3, while besides ID11 that of all other covered components is 1 or 2.
- ID11, i.e. Feedwater spargers, was screened in because it experiences more thermal load cycles than any other component in the RPV.

The characteristic values concerning the screening process Step 1 results are:
- No. of components with significant loads (excluding FIV and WRSs) = 8.
- No. of components with fluence after 60 EFPYs above screening value = 9.
- No. of components that screened in = 16.

The components that screened in through the screening process Step 1 are:
- Spring beams and support brackets,
- Steam outlet nozzles,
- Steam separator support legs,
- Feedwater nozzles,
- Feedwater spargers,
- Core spray piping inside core shroud cover,
- Control rods,
- Control rod guide tubes,
- Core shroud, Core shroud support,
- Pump deck,
- Core shroud support legs,
- Instrumentation guide tubes and nozzles,
- Control rod guide tubes and nozzles at RPV bottom,
- Cylindrical RPV shell,
- Shutdown cooling nozzles, and
- Core spray nozzles.

The components that screened in through the screening process Step 1 go next to the screening process Step 2. Those components that screened out through the screening process Step 1 are not considered in the screening process Step 2 or in any further analysis. The results from the screening process Step 2 are presented in Section 8.3. Those components that screen out through the screening process Step 2 are not considered in any further analysis. Those components that screen in through the screening process Step 2 go further to the degradation potential analyses.
8 Computational approaches, tools and analyses

The applied computational approaches and tools are both numerical and analytical. They concern mainly structural mechanics and fracture mechanics. They cover both deterministic and probabilistic procedures.

The final screening, which is the last part of this section, uses as input data the results from both the preliminary screening as well as those from the stress and strain analyses.

The degradation potential and criticality analyses are performed by applying mainly fracture mechanics. Degradation potential is considered in terms of crack growth, which is computed using the equations presented in Section 4. Degradation criticality corresponds to critical crack size, which is reached when the maximum value of the stress intensity factor for a crack equals the corresponding material fracture toughness value. The considered degradation modes are ductile and brittle fracture. All relevant degradation mechanisms are covered, see Table 3.1-1 for those.

8.1 Computational approaches and tools

Detailed descriptions of the existing computational approaches and tools can be found from handbooks and source references.

8.1.1 Structural mechanics

Structural mechanics concerns mathematical modelling of behaviour of structures, taking into account their geometry, material properties, loading and boundary conditions. Structural mechanics is a well established field of science. Representative handbooks on structural mechanics include those by Lubliner [379], Malvern [380] and Timoshenko [381]. Finnish notable handbooks on structural mechanics include those by Ylinen [382] and Jumppanen [383]. Analytical structural mechanics solutions for commonly used structures and components have been compiled as well, e.g. by Roark [384]. The following description covers only deterministic structural mechanics approach.

The structural models describe the geometry and material properties of the structure considered. The stress/strain hardening behaviour can be loading history dependent, with typical modelling options being isotropically or kinematically evolving yield surface, or their combination.

In a simple case, the structural model can be created and solved with a set of analytical equations. In case of NPP components, numerical structural models are often needed in addition, due to e.g. complex geometry and/or loading. These are typically prepared and solved by using FE codes. There are several advanced commonly used general purpose FE codes available, such as ANSYS [385] and Abaqus [386]. These FE codes also allow the use of finite elements of differing types in the same model, as well as provide tools for sub-modelling by which locally more dense element meshes can be realised. Other applicable numerical techniques exist as well, such as finite difference method. To achieve sufficient accuracy using numerical methods, dense enough element meshes are to be used, and in time dependent analyses small enough time increments.
The types of structural mechanics analyses performed for NPP components include:

- linear or non-linear time independent or dependent static stress/strain analyses,
- heat transfer analyses,
- combined sequential or coupled heat transfer and stress/strain analyses,
- dynamic eigenvalue and/or eigenfrequency analyses.

Typically, the analysis results obtained consist of time independent or dependent stress/strain fields and temperature fields. From these the local/global maximum and/or minimum values are often the most important ones.

The results from the stress/strain analyses, such as nominal stresses, local stresses and stress fluctuations, are often needed as input data in fracture mechanics based analyses. For instance, for fatigue induced crack growth analyses the time dependent stress/strain results are needed because the stress/strain load cycles are obtained from them. As for the dynamic analyses, the results are needed e.g. to assess the structural response to determined earthquake loads.

### 8.1.2 Fracture mechanics

Fracture mechanics concerns mathematical modelling of local degradation in structures, taking into account the geometry, material properties and stress/strain state. Fracture mechanics is a well established field of science, though much younger than structural mechanics. Representative handbooks on fracture mechanics include those by Anderson [387], Kanninen [388], Unger [389], Wei [390], Gdoutos [391] and Perez [392]. The following description covers both deterministic and probabilistic fracture mechanics approaches.

Fracture mechanics is divided into linear-elastic fracture mechanics (LEFM) and elastic-plastic fracture mechanics (EPFM). Materials with relatively low fracture resistance that fall below the plastic collapse strength can be analysed on the basis of linear-elastic concept through the use of LEFM. For other materials the use of EPFM is often necessary. Fracture mechanics deals with local crack like flaws, most often with sharp front. For LEFM analyses the parameter driving the crack growth is stress intensity factor, \( K \), while that for EPFM analyses is \( J \)-integral. Of the three cracking modes, those being opening Mode I, in-plane shear Mode II, and out-of-plane shear Mode III, Mode I is most often the limiting one.

In a simple case, the fracture mechanics model can be created and solved with a set of analytical equations. In case of NPP components, numerical structural models are often needed as well, for the same reasons as in many structural mechanics analyses. These are typically prepared and solved by using FE codes. There are several advanced commonly used general purpose FE codes available, of which the scope of the already mentioned ANSYS [385] and Abaqus [386] codes cover fracture mechanics too. There are also advanced special purpose fracture mechanics FE codes, such as FEACrack [393] and WARP3D [394].

The types of fracture mechanics analyses performed for NPP components include:

- linear or non-linear time independent brittle fracture analyses,
- linear or non-linear time dependent ductile fracture analyses.

Typically, the results consist of time independent or dependent \( K \) and/or \( J \)-integral values over the crack front. From these, the maximum and minimum values are often checked. Another useful parameter applied in EPFM is crack tip opening displacement (CTOD). Unlike in the case of linear-elastic materials, the crack tip may experience plastic deformation by blunting in strain hardening materials. CTOD takes that into account. There are other fracture mechanics parameters too that describe the growth potential of a crack.
Usually, in the fracture mechanics analyses concerning brittle fracture, a set of stationary cracks of different sizes are postulated for the same location, the critical size being reached when the maximum stress intensity factor value, $K_{\text{max}}$, equals the corresponding fracture toughness, $K_{\text{IC}}$.

In the fracture mechanics analyses concerning ductile fracture, a relatively small initial crack is usually postulated to the location of interest. Then, its growth is computed incrementally by first computing the $K$ or $J$ values over the crack front, then inserting those to an applicable crack growth equation, and resulting with a grown crack size. The same process is repeated until some pre-determined critical crack size is reached. The crack growth equations are degradation mechanism specific. For NPP components and materials, $K$ is most often used for describing the crack growth potential. This is both due to the simplicity of the associated crack growth equations and the availability of the necessary experimentally defined material and environment specific parameter data. Crack growth equations involving $J$ would be more accurate for describing crack growth under elastic-plastic and especially plastic conditions, but for metallic NPP materials the necessary material and environment specific parameter data is largely missing.

In deterministic fracture mechanics (DFM) analyses, all input data parameters have single values for each considered time instant. By contrast, in probabilistic fracture mechanics (PFM) analyses one or several of them have distributed values, often expressed by probability density functions (PDFs). A PDF describes the probabilities for a parameter to have specific probabilities over a realistic range of physical values, such as initial crack depth. Both DFM and PFM approaches have been described in more detail in ref. [395] by the author of this thesis. Thus, they are only briefly summarised here. PFM is based both on DFM and probabilistic mathematics. For probabilistic procedures applicable for PFM, see ref. [395].

A commonly used probabilistic procedure for PFM applications is Monte Carlo simulation (MCS). This procedure is simply a repeated process of generating deterministic solutions with a given model. Each solution corresponds to a set of deterministic values of the underlying random variables. The main element of a MCS procedure is the generation of random numbers from specified distributions [396]. In PFM these distributions are PDFs for selected input data parameters, while the model applies the fracture mechanics equations, though a FE model is possible too. One of the advantages of PFM is that it allows to take into account the inherent variation of several notable input data parameters. For instance, initial crack size, yield strength, fracture toughness as well as occurrence frequency and amplitude of load cycles can vary considerably. For PFM analyses, PDFs are produced for one or several parameters/properties. MCS also requires definitions for success and failure. In fracture mechanics, this often corresponds to crack growth reaching a pre-determined critical size. It can e.g. be maximum allowable size according to the adopted code/standard, leak (crack has just grown through component wall) or break (plastic collapse of the cross-section). The result from MCS is a probability of failure.

To summarize, the MCS procedure consists of the following steps [398]:
1. Given the predefined PDFs of the random variables involved in the analysis, generate a single value for each random variable.
2. Perform the deterministic analysis, and record if failure occurs.
3. Repeat steps 1 and 2 for $N$ times, where $N$ is the number of simulations.
4. Estimate the probability of failure (POF) as $\text{POF} = N_f / N$, where $N_f$ is the number of failures.
MCS offers several advantages [397]:

- The distributions of the model variables do not have to be approximated.
- Correlations and other inter-dependencies can be modelled.
- The level of mathematics required to perform a MCS is quite simple.
- Commercial software is available to automate the tasks involved in the simulation.
- Improved accuracy can be achieved by simply increasing the number of calculated iterations.
- MCS is widely recognised as a valid technique, so its results are likely to be accepted.
- The behaviour of the model can be investigated easily.

The main drawback with the plain MCS is the computational effort involved. To produce a reasonably accurate estimate of the failure probability at least 100/POF trials are required. For instance, for failure probabilities around 1.0E-04, this requires at least 1.0E+06 simulations to be performed [399].

A variety of techniques have been developed to reduce the number of necessary simulations. Generally speaking, these are called variance reduction techniques, and in favourable circumstances they can be very efficient. These techniques include importance sampling and stratified sampling.

8.1.3 Tools used in degradation potential analyses

The tools used in degradation potential analyses are described briefly in the following. In most cases, the fracture mechanics based analysis code VTTBESIT is used. This code comprises parts developed by VTT [400,401] and by IWM [402,403,404], the latter being the Fraunhofer-Institut für Werkstoffmechanik, in Germany. In some cases handbook solutions are used too, such as those in the R6 Method, Rev. 4 [407] and those by Zahoor [408].

VTTBESIT computes the $K_I$ values over the crack postulate fronts. The analysis code considers only mode I loading, i.e. crack opening mode. These computations are carried out by BESIT60 code module by IWM. This module is based on the weight/influence function method. Solutions are provided for "infinite" and semi-elliptic surface breaking crack postulates in straight plates and hollow cylinders [402,403,404]. VTTBESIT uses the BESIT60 solely for computation of $K_I$ values, and applies them as part of the necessary input data for crack growth analysis [400].

VTTBESIT calculates the crack growth as a sequence of increments until some pre-defined ending criterion is reached. Two crack growth models are provided in the analysis code: Paris-Erdogan equation [150] for fatigue induced crack growth and the rate equation [186] for SCC.

In addition to deterministic crack growth computations, another version of VTTBESIT exists for probabilistic crack growth analyses. Therein, Markov procedure is applied. Markov models are used widely in modelling reliability problems. For an introduction, see for example refs. [405] and [406]. Different discrete states in the Markov model correspond to different configurations of the considered system. In this application concerning component degradation and inspections, these different states correspond to crack growth through component wall. In the computations the wall thickness is divided as a function of crack depth into states according to detection probabilities and assumed repair policies.

The two VTTBESIT versions are described briefly in the following.
Deterministic VTTBESIT

The analysis flow of the deterministic VTTBESIT is [400,401]:
1. Reading the deterministic input data, including: initial crack size, crack orientation, material property data for selected crack growth equation, calculation period and loads in the form of stresses through component wall.
2. Crack growth analysis: The magnitude of crack growth at each time increment is calculated from the respective crack growth equation. The ending criterion of the analysis is that crack depth reaches some pre-defined value or the opposite surface surface of the wall.
3. Writing the time or load cycle dependent $K_I$ and crack growth results to a separate file.

Probabilistic VTTBESIT

The analysis flow of the probabilistic VTTBESIT is [409]:
1. Reading the input data, including: selection of PDF for initial crack dimensions, interval and quality of inspections, otherwise the input data is mostly the same as for deterministic VTTBESIT analyses.
2. Random picking of values for certain input data parameters from the specified PDFs: for both SCC and fatigue induced crack growth, for initial crack depth and length. Published PDFs applicable to NPP components include those developed by Khaleel and Simonen [410] and those provided by the PFM analysis code WinPRAISE [411].
3. Crack growth simulations: The magnitude of crack growth at each time increment is calculated from the respective crack growth equation. The ending criterion for a simulation is that crack depth reaches the opposite surface of the wall.
4. For each analysed circumferential piping weld, several thousands of simulations are computed with Latin hypercube simulation (LHS) procedure.
5. The degradation state to which the crack has grown is computed for each year of the selected time span in operation and for each simulation. These results are used in the ensuing probabilistic Markov process based degradation potential and risk analyses.
6. Writing the time or load cycle dependent crack growth simulation results to a separate file.

The applied discrete time Markov procedure for degradation potential and risk analyses is summarised as follows [412]:
1. Construction of transition probabilities from VTTBESIT simulation results, assembling them into a degradation matrix, and database analysis of crack initiation frequencies.
2. Model for inspection quality, as based on applicable probability of detection (POD) functions, which are in turn used to construct the inspection matrix transition probabilities. Published POD functions applicable to NPP components include those provided by the PFM analysis code WinPRAISE [411] and those published by Inspecta [413].
3. Markov model to calculate pipe leak/break probabilities and risks for chosen inspection programs, including the case of no inspections.

8.2 Stress, strain and CUF analyses

The stress/strain analyses for components that screened in according to screening Step 1 were carried out by ANSYS [385] and Abaqus [386]. Part of the FE models and most FE analyses are done by the author of this work. The rest of the FE models were prepared by VTT and Swedish analysis consultant FS Dynamics Sweden AB, with the latter having performed also part of the FE analyses. As there are altogether 15 components that were screened in according
to Step 1 the scope of the analyses is considerably large. However, the main focus in this work is on degradation potential analyses. As the stress/strain results are used only as input data in the Step 2 screening process and in the ensuing degradation potential analyses, they are only summarised here. For illustration, some representative examples are described. In addition, in the connection of degradation potential analyses, detailed stress/strain results are presented, where necessary.

The performed stress/strain analyses cover both stationary operational conditions, with constant values of internal pressure and temperature, as well as anticipated/experienced load transients, during which the pressure and temperature loads vary. As the stress/strain analyses need as input data temperature distributions over component walls heat transfer analyses were performed first. Thus, the heat transfer and stress/strain analyses were carried out as sequentially coupled.

The CUF analyses performed were based on stress analysis results. The CUF analyses are needed to find out the degree of fatigue degradation in the locations with highest stress fluctuations. These are typically discontinuities in the geometry. It is emphasised that the critical CUF value of 1 corresponds to initiation of a macroscopic crack, not a leak or break.

Both the stress and CUF results are needed as input data in the screening process Step 2.

### 8.2.1 Geometry and material properties

The 15 components considered are listed at the end of Section 7.2.

The geometry and material properties of the components considered are presented in Section 5.1 and Appendix C.

As mentioned earlier, the materials of BWR RPV and its internals are ferritic low-alloy steels, austenitic stainless steels and nickel-base alloys. To provide two examples, the material property data of ferritic steel ASTM 533 Grade B, Class 1 and austenitic stainless steel type 304 are given in Tables 8.2.1-1 and 8.2.1-2, respectively. The former steel is the RPV base material and the latter steel is used in some of the RPV internals.

#### Table 8.2.1-1a. Part 1 of material property data for ASTM 533 Grade B, Class 1 steel [418].

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
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<td>345</td>
<td>552</td>
<td>201</td>
</tr>
<tr>
<td>285</td>
<td>293</td>
<td>552</td>
<td>184</td>
</tr>
</tbody>
</table>

#### Table 8.2.1-1b. Part 2 of material property data for ASTM 533 Grade B, Class 1 [418].

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>Thermal conductivity [W/m°C]</th>
<th>Specific heat [J/kg°C]</th>
<th>Coefficient of thermal expansion [(1/°C)×1E-06]</th>
<th>Density [kg/m³]</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>40.8</td>
<td>437</td>
<td>11.5</td>
<td>7850</td>
</tr>
<tr>
<td>285</td>
<td>39.3</td>
<td>550</td>
<td>13.2</td>
<td>7850</td>
</tr>
</tbody>
</table>
Table 8.2.1-2a. Part 1 of material property data for type 304 steel [418].

<table>
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<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>207</td>
<td>517</td>
<td>195</td>
</tr>
<tr>
<td>285</td>
<td>132</td>
<td>437</td>
<td>176</td>
</tr>
</tbody>
</table>

Table 8.2.1-2b. Part 2 of material property data for type 304 steel [418].

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>Thermal conductivity [W/m°C]</th>
<th>Specific heat [J/kg°C]</th>
<th>Coefficient of thermal expansion [(1/°C)×1E-06]</th>
<th>Density [kg/m³]</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>14.8</td>
<td>483</td>
<td>15.3</td>
<td>7850</td>
</tr>
<tr>
<td>285</td>
<td>19</td>
<td>554</td>
<td>17.5</td>
<td>7850</td>
</tr>
</tbody>
</table>

8.2.2 Loads

The stresses and strains experienced by the 15 components that were screened in according to Step 1 are induced by various load cases and types of loads. These loads are summarised in the following, referring to information presented earlier, where necessary.

All considered components are exposed to stationary normal operation, under which the NPPs are most of the time. For a BWR RPV under those conditions, the pressure and temperature loads from the process fluid, i.e. water and steam, are given in Section 6.1.

A general description of applied loads, environmental loads and manufacturing induced loads in BWR environment is presented in Section 6.1. Of these loads, manufacturing induced WRSs are described in more detail in the following.

For the BWR RPV wall, the FE simulated residual stresses in the interface region between the base material and inner cladding are from VTT report VTT-R-06020-14 [419]. As for WRSs in the welds of other considered components, applicable and reasonably conservative recommendations from the FFS handbooks are used. These were taken from the Swedish SSM handbook [420] and the SINTAP Procedure final report [87].

Of the relevant load types, the mechanical and thermal loads as well as irradiation are described in more detail in the following. The load types affecting the BWR RPV and its considered internals are listed in Table 6.3-1.

The typical deviations from the normal operation that the BWR RPV and its internals experience are the anticipated load transients. The pre-tension of the RPV internals provided by the spring beams is in effect always when the RPV-head is in its place. For OL1/OL2 RPV, 16 different load transients have been defined [421]. These occur in the RPV downcomer between the RPV and the core shroud, as caused by water flow from the feedwater nozzles. The RPV main nozzle experiencing the largest number of load cases is the feedwater nozzle, for which 35 different load transients have been defined [422]. The average yearly number of experienced load transients in the RPV downcomer and in the feedwater nozzle is less than the mentioned values because many load transients occur very seldom.
According to TVO documentation [421] the load transients concerning the OL1/OL2 RPV and its internals are:

- 1) Cold start-up,
- 2) Hot start-up,
- 3) Power variations,
- 4) Turbine shutdown (TS), dumping allowed,
- 5) Reactor scram,
- 6) Failures, Tests and Hot stand-by (HSB),
- 7) TS · D and Isolations,
- 8) Scram, special cases,
- 9) Hot shutdown,
- 10) Cold shutdown,
- 11) Cold shutdown, blowdown with the relief system (314) valves,
- 14) Loss of off-site power,
- 15) Test of the feedwater system (312) isolation valves,
- 16) Pumping with the auxiliary feedwater system (327) via system 312,
- 17) 314-Blowing,
- 21) Heating of steam lines after HSB.

Many of the above mentioned load transients actually present transient groups, to each of which belongs several more specifically defined load transients. Most RPV internals experience only a very small number of load transients because being inside the core shroud they are also shielded by it. Thus, most RPV internals experience only the normal operation, start-up and shutdown. These two main load transients are described in more detail in the following, see also Figures 8.2.2-1 and 8.2.2-2 that show the variation of temperature, pressure and flow rate as a function of time.

**Cold start-up [421]:** The entire reactor is heated with a maximum temperature increase of 40 °C/h. The starting temperature of the RPV is 20 °C and increases during 6 hours to the operational temperature of 286 °C at full pressure of 7.0 MPa. The main circulation pumps are set to minimum speed. It is normally not necessary to pump feedwater into the reactor, only the shutdown cooling flow circulates. This load case needs no detailed thermo-hydraulic analyses as the transient is slow with steady temperature rise no more than 40 °C/h. Therefore, the temperature gradients in the RPV wall and all internals are insignificant.

**Cold shutdown [421]:** Cooling of the reactor from hot stand-by with a temperature decrease of maximum 40 °C/h. The temperature in the RPV begins at 286 °C and falls during 6 hours to minimum temperature of 20 °C. Down to approximately 180 °C the decrease of the temperature is due to dumping of steam to the turbine condenser. Thereafter, the shutdown coolers in the shutdown cooling system are taken into operation. A small amount of feedwater is also required in order to maintain a steady level in the RPV.
There is not much RPV fluence data publicly available for BWR units. For Chinsan BWR units, which are comparable to OL1 and OL2, computed fluence data has been reported, see ref. [423]. The Chinsan BWR units 1 and 2 are owned by Taiwan Power Company (TPC) and they started commercial operation in 1978 and 1979, respectively. The inner diameter and wall thickness of their RPVs is almost the same as those ones of OL1 and OL2, differing only by a few cm. The fluence computations have been made with DORT and ANISN computer programs, with results available for 32 and 64 EFPY. These durations quite well correspond to 40 and 80 calendar years of time in operation, respectively. The maximum fluence experienced by the RPV is at
the beltline region, i.e. the region nearest to the fuel. The computed maximum fluence accumulated by the RPV beltline welds of Chinsan BWR units after 32 and 64 EFPY is presented in Table 8.2.2-1. The horizontally orientated RPV beltline weld W-1102-3 is located 6264 mm above the RPV bottom. The operation of the plant affects the accumulated fluence, especially the lengths of the yearly maintenance outages. Thus, the amount of accumulated fluence is unique to each NPP unit. Therefore, the data in Table 8.2.2-1 cannot be applied to OL1 and OL2 as such, but instead it gives an approximation of the scale of their fluences. In the irradiation embrittlement computations to be performed the actual proprietary OL1/OL2 specific fluence data is used [447]. In BWR units, the effect of irradiation embrittlement to stresses and strains is negligible.

Table 8.2.2-1. The computed maximum fluence accumulated by the RPV beltline welds of Chinsan BWR units after 32 and 64 EFPY, from ref. [423].

<table>
<thead>
<tr>
<th>Reactor</th>
<th>Weld no.</th>
<th>Maximum neutron fluence ($\times10^{19}$ n/cm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>32 EFPY</td>
</tr>
<tr>
<td></td>
<td></td>
<td>64 EFPY</td>
</tr>
<tr>
<td>Unit 1</td>
<td>W-1001-07</td>
<td>0.0372</td>
</tr>
<tr>
<td></td>
<td>W-1001-08</td>
<td>0.0745</td>
</tr>
<tr>
<td></td>
<td>W-1001-09</td>
<td>0.0502</td>
</tr>
<tr>
<td></td>
<td>W-1001-10</td>
<td>0.036</td>
</tr>
<tr>
<td></td>
<td>W-1001-11</td>
<td>0.0254</td>
</tr>
<tr>
<td></td>
<td>W-1001-12</td>
<td>0.0447</td>
</tr>
<tr>
<td></td>
<td>W-1102-03</td>
<td>0.0507</td>
</tr>
<tr>
<td>Unit 2</td>
<td>W-1001-07</td>
<td>0.0418</td>
</tr>
<tr>
<td></td>
<td>W-1001-08</td>
<td>0.0845</td>
</tr>
<tr>
<td></td>
<td>W-1001-09</td>
<td>0.0564</td>
</tr>
<tr>
<td></td>
<td>W-1001-10</td>
<td>0.0333</td>
</tr>
<tr>
<td></td>
<td>W-1001-11</td>
<td>0.0237</td>
</tr>
<tr>
<td></td>
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<td>0.0431</td>
</tr>
<tr>
<td></td>
<td>W-1102-03</td>
<td>0.0479</td>
</tr>
</tbody>
</table>

As described in more detail in Section 6.2.3, in Finland the seismic loads have to be taken into account according to Guides YVL B.2 [355], YVL B.7 [356] and YVL E.4 [354]. The loading condition data contains the floor response, as resolved by analysing the dynamic behaviour of the buildings. The variations in the floor response between different supporting points are to be analysed if the results show significant additional stresses. However, in Finland severe earthquakes are estimated to occur very seldom. Due to this their effect to stress/strain analyses and ensuing degradation potential analyses of BWR RPV and internals is excluded here.

The process-chemical loads are described in Section 6.2.4. Of the relevant degradation mechanisms, they mostly affect SCC, other types of corrosion and environmentally accelerated fatigue. The effect of process-chemical loads on stresses and strains is negligible.

Severe acoustic loads occur seldom. It is difficult to predict their time of occurrence and to model their effect. Due to this, they are excluded from the stress/strain analyses.

8.2.3 FE models

New FE models were prepared for 12 of the 15 components that screened in according to screening Step 1. No new FE models were prepared for ID22 Instrumentation guide tubes, ID30 Shutdown cooling nozzles nor ID31 Core spray nozzles, because the existing FE models were sufficient for the analysis purposes. The FE models prepared earlier by VTT concern the RPV
and its main nozzles, whereas those prepared by FS Dynamics (FSD) include also most of the
RPV internals. In those cases that both VTT and FSD had prepared a FE model of the same
component, it was possible to compare results. Due to the relatively large number of prepared
FE models, only examples of them are presented here, see Figures 8.2.3-1 to 8.2.3-3. However,
the general size of all used FE models are presented as the number of nodes and elements, see
Table 8.2.3-1.

Figure 8.2.3-1. Global FE model of the whole RPV and its main nozzles as well as most of the
internals, by FS Dynamics [424].
Figure 8.2.3-2. On the left side, local FE model of the feedwater nozzle by VTT [425], including connecting safe-end and pipe as well as part of the RPV wall. On the right side, detail of the local FE model.

By scope, FE models of three types were prepared:

- Global model, consisting of the whole RPV and its main nozzles as well as most of the internals. Coarser element meshes are used.
- Local FE models, consisting of one component or several connecting components, with suitable boundary conditions in the surfaces along which they are cut from other structures. Denser element meshes are used.
- FE sub-models, consisting of one component and parts of connecting components, with boundary conditions in the surfaces along which they are cut from the global model. Locally very dense element meshes are used, where necessary.
Table 8.2.3-1. For prepared FE models, the used element types and approximate number of nodes and elements.

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>FE model: type, by whom</th>
<th>Element types</th>
<th>No. of elements</th>
<th>No. of nodes</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>Spring beams and support brackets</td>
<td>local, by VTT</td>
<td>4-node tetrahedron</td>
<td>130600</td>
<td>28300</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>local, by VTT</td>
<td>8-node hexahedron</td>
<td>33400</td>
<td>38600</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator supports</td>
<td>sub-model, by FSD</td>
<td>8-node hexahedron, 6-node wedge</td>
<td>1146200</td>
<td>1205300</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>local, by VTT (ii)</td>
<td>8-node hexahedron, pentahedron</td>
<td>57000</td>
<td>64200</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>local, by FSD</td>
<td>tetrahedron, hexahedron</td>
<td>183000</td>
<td>520200</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>global, by FSD</td>
<td>hexahedron, wedge, solid shell, thin shell, beam, link, spring, pipe, mass</td>
<td>272500</td>
<td>326700</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>global, by FSD</td>
<td>hexahedron, wedge, solid shell, thin shell, beam, link, spring, pipe, mass</td>
<td>272500</td>
<td>326700</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>sub-model, by FSD</td>
<td>8-node hexahedron, 6-node wedge</td>
<td>872400</td>
<td>966900</td>
</tr>
<tr>
<td></td>
<td></td>
<td>sub-model, by FSD</td>
<td>8-node hexahedron, 6-node wedge</td>
<td>256600</td>
<td>290900</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>sub-model, by FSD</td>
<td>8-node hexahedron, 6-node wedge</td>
<td>256600</td>
<td>290900</td>
</tr>
<tr>
<td>ID</td>
<td>Component</td>
<td>FE model: type, by whom</td>
<td>Element types</td>
<td>No. of elements</td>
<td>No. of nodes</td>
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</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>sub-model, by FSD</td>
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<td>501000</td>
</tr>
<tr>
<td></td>
<td></td>
<td>local (i), by VTT</td>
<td>6-node rectangular, 8-node rectangular</td>
<td>9800</td>
<td>23000</td>
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<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>global, by FSD</td>
<td>hexahedron, wedge, solid shell, thin shell, beam, link, spring, pipe, mass</td>
<td>272500</td>
<td>326700</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>local, by VTT</td>
<td>20-node hexahedron</td>
<td>9800</td>
<td>48000</td>
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<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>sub-model, by FSD</td>
<td>8-node hexahedron</td>
<td>1920000</td>
<td>1934400</td>
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<td></td>
<td>local (i), by VTT (ii)</td>
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<td>12800</td>
<td>31300</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>local, by VTT</td>
<td>8-node hexahedron</td>
<td>52000</td>
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<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>local, by VTT</td>
<td>8-node hexahedron</td>
<td>44900</td>
<td>52100</td>
</tr>
</tbody>
</table>

1) Most FE models are three dimensional while (i) denotes the axially symmetric two dimensional FE models. The author of this work has participated in creating most of the FE models by VTT.
2) Those FE models that are solely by the author of this work are denoted with (ii).

The size of the elements used in the FE models varies. At largest it is approximately 500 mm in the global RPV model down to approximately 1 mm in the geometrical discontinuities of some sub-models.

### 8.2.4 Computational analyses

For ID24 Cylindrical RPV shell computation was carried out with FE analyses.

In the FE computations, the heat transfer problem is solved first, and after that the stress/strain problem. The heat transfer results were saved to separate files, from which they were read as a part of the input data to the stress/strain analyses. For all considered components as well as their joints and interfaces the stress free temperature in the stress/strain analyses was chosen as 286 ºC. For justification of this, see e.g. ref. [415].

Most properties of the materials of all considered components are temperature dependent. This was taken into account both in the heat transfer and stress/strain analyses. In the FE analyses the applied material model was elastic-plastic. In the analyses with analytical equations for cases where maximum stresses remained lower than the material yield strength the applied material model was linear-elastic. The locally confined WRSs were added by superposition to the computed total stresses. For a few cases this was a conservative assumption as material plasticity would have distributed some part of the peak stress values to the vicinity of the peak location. However, it is computationally laborious and costly to simulate WRSs with FE analyses. This is why the mentioned conservative assumption was applied. It is assessed that this assumption is not overly conservative.

All considered components experience the operational conditions, whereas the exposure to the load transients varies a lot between different components. The feedwater nozzles and RPV wall experience much more load transients than the rest of the considered components. In the FE analyses, the load transients are introduced as temperature and pressure against component surfaces. In the heat transfer analyses, the heat transfer coefficient was used for incorporating
the temperature load from the fluid to the exposed surfaces. As the outer surface of the OL1/OL2 RPV is thermally insulated, the applied value for the heat transfer coefficient is zero.

The applied FE codes produce warnings when the accuracy tolerances are exceeded. This occurred in some occasions for such parts of the load transients when the temperature and/or pressure change rate was higher. When warnings resulted in the preliminary analyses shorter time increments were defined for the analysis part in question, and the whole analysis was run again. This process was continued until the FE runs ceased to give warning messages. In the FE analyses by the author of this work the default tolerances of the FE code Abaqus [386] were used.

The number of the performed FE analyses was considerably large. In addition to analysing 15 components, each load transient requires a separate analysis. Because of this, the number of separate FE analyses for the RPV wall was more than 10, and that for the feedwater nozzle was more than 20. Even these counts are less than the total number of load transients that has been defined for these components. However, due to some load transients being very moderate, e.g. with small variation of pressure and temperature loads, they were dropped from the scope. On the other hand, their numbers of occurrence was taken into account for the fatigue analyses by taking their effect to correspond to that of the most similar load transient analysed.

Another way to reduce unnecessary computational work in the FE analyses was to apply sub-modelling approach. If all considered components would have been modelled accurately enough in the global model, its size would have increased to several millions of nodes. This is because several geometry details of all considered components require locally denser element meshes. To do analyses with such a FE model would take very long time. This was overcome by building-up sub-models for most of the considered components. The approach involves modelling the overall response with the global model and the detailed analyses of the selected components using sub-models. The sub-model regions are located within the global model, but are modelled there only coarsely. The global model and each sub-model are coupled in the FE analyses by imposing the displacements obtained from the global model as boundary conditions to a sub-model along its perimeter. Both the heat transfer and stress/strain FE analyses were performed for the global model and each of the sub-models.

For normal operational conditions the average FE analysis duration was less than an hour. For load transients with more variation in the pressure and temperature loads, one FE analysis could take from two to three days by applying the global model.

The CUF analyses are based on the results obtained in the stress/strain analysis. These analyses needed to be carried out for all locations with significant stress fluctuation. This necessitates FE analyses for all components and load transients, and the search through all stress results to find the stress fluctuation locations. As the CUF results are only needed as input data for the final screening of the considered components, a more straightforward conservative approach was resorted to. This concerns using existing confidential results by EQE International [430] that consider only the most severe anticipated load transient that the OL1/OL2 RPV and internals can experience. This load transient is Automatic Depressurisation (ADS), during which the temperature and the pressure drop within approximately 750 s from their operational values of 286 °C and 70 bar to shutdown values of 20 °C and 1 bar, respectively, see confidential ref. [428]. Note, that this far ADS has not occurred in the OL1/OL2 units, but it is has been defined for the of quickest possible safe shutdown. To avoid overly conservatism, the CUF values were scaled for other load transients according to differences in stress ranges at the inner surfaces of the components. The accuracy of this approach can vary as the relationship between
the applied stresses and load cycles to failure can be non-linear. For the feedwater nozzles and feedwater spargers accurate recently computed CUF data for several load transients was available in confidential analysis reports [416, 417].

The stress/strain and CUF analyses for anticipated ADS in the OL1/OL2 RPV have been carried out earlier by EQE International [429,430]. Three dimensional FE analyses were done for the OL1/OL2 RPV and internals [429] and the CUF analyses for the locations with the highest stress fluctuations [430]. The scope of the analyses included all components considered in this work. The FE models by EQE International concern mainly fatigue analyses and are accurate enough for those. But in this work the analysis scope is wider, covering several more degradation mechanisms and locations of interest. The CUF procedure in ref. [430] is described in the following.

The methods and criteria in Article NB-3000 of ASME Section III [147] were applied for the CUF analysis. The determination of the stress ranges and alternating stress intensities, their counting with an applicable procedure, and the cumulative fatigue computation according to Miner’s rule were carried out for the locations with stress fluctuation. Most often, a suitable modification of the rainflow procedure [432,433] is used for the counting of the stress cycles. The applied fatigue end-of-life curves were taken from ASME Section III [147]. ASME Level B Service limits for stress intensity were applied in the stress assessment. Typically, this involves checking if the through-wall linearized stress is less than three times the design stress, \( S_m \). The material type specific \( S_m \) values are given in ASME Section II [418]. As mentioned above, in terms of loads the analyses by EQE International [429,430] consider only ADS. The scaling of these results for other load transients is also explained above.

8.2.5 Stress analysis results

The results from the stress/strain analyses for components that screened in according to screening Step 1 are summarised in the following. This concerns only stress results as the strains are not needed in the ensuing screening Step 2 and degradation potential analyses. However, the strain results are used for checking if there will be some large deformation locally.

In the stress analysis results, the locations of most interest are those with locally higher stresses, especially from the viewpoint of the ensuing degradation potential analyses. Moreover, all locations with tensile stresses are of interest as they favour the crack growth. However, with low tensile stresses, the potential for crack growth is small. Compressive stresses slow down or arrest the crack growth.

Figures 8.2.5-1 and 8.2.5-2 illustrate the distributions of stresses through component wall and/or at surfaces. Stresses through wall are presented in Figures 8.2.5-3 and 8.2.5-4. These are picked from specifically defined planes through component wall. Where applicable, the fluctuation of stresses is given too in the form of stress ranges. Most often, the considered loading cases are operational normal conditions, start-up and shutdown. Finally, the stresses are summarised for all considered components in Table 8.2.5-1. The stress results have been taken from the element mesh nodes, when not specified otherwise. When Cartesian right-handed coordinate system has been used, its axes are denoted by \( x \), \( y \) and \( z \). Cylindrical coordinate system is used for cylindrical components.
Figure 8.2.5-1a. For a cut surface through wall of ID24 Cylindrical RPV shell: axial stresses [Pa] under 100 % normal operation in cylindrical RPV coordinate system, from ref. [434]. The inner cladding is on the left side coloured with light blue (dark blue region is the inner surface of the cladding). WRSs in the interface between the cladding and base material are taken into account.

Figure 8.2.5-1b. For a cut surface through wall of ID24 Cylindrical RPV shell: circumferential stresses [Pa] under 100 % normal operation in cylindrical RPV coordinate system, from ref. [434]. The inner cladding is on the left side coloured with light blue (dark blue region is the inner surface of the cladding). WRSs in the interface between the cladding and base material are taken into account.
Figure 8.2.5-2a. For a cut surface through wall of ID10 Feedwater nozzles: circumferential stresses [MPa] under 100 % normal operation in cylindrical nozzle coordinate system, from ref. [425]. The grey region near the left corner id the inner corner of the nozzle, the green stripe surrounding it is the inner cladding.

Figure 8.2.5-2b. For a cut surface through wall of ID10 Feedwater nozzles: circumferential stresses [MPa] under 100 % normal operation in the nozzle to safe-end weld in cylindrical nozzle coordinate system, from ref. [425]. The weld is in the middle of the figure, the maximum stress region in red is a the outer surface side, and “1” in the figure is axial coordinate direction.
Figure 8.2.5-3a. For ID24 Cylindrical RPV shell: axial stresses for transient Start-up at different times, and axial stress range for a load cycle consisting of transients Start-up and Shutdown, from the analyses of ref. [437]. WRSs in the interface between the cladding and base material are not taken into account. The origin of the coordinate system is inner surface.

Figure 8.2.5-3b. For ID24 Cylindrical RPV shell: circumferential stresses for transient Start-up at different times, and circumferential stress range for a load cycle consisting of transients Start-up and Shutdown, from the analyses of ref. [437]. WRSs in the interface between the cladding and base material are not taken into account. The origin of the coordinate system is inner surface.
Figure 8.2.5-4a. For ID24 Cylindrical RPV shell: axial stresses for transient Start-up at different times, and axial stress range for a load cycle consisting of transients Start-up and Shutdown, from ref. [434]. WRSs in the interface between the cladding and base material are taken into account according to ref. [419]. The origin of the coordinate system is inner surface.

Figure 8.2.5-4b. For ID24 Cylindrical RPV shell: circumferential stresses for transient Start-up at different times, and circumferential stress range for a load cycle consisting of transients Start-up and Shutdown, from ref. [434]. WRSs in the interface between the cladding and base material are taken into account according to ref. [419]. The origin of the coordinate system is inner surface.
In Table 8.2.5-1:
- Overall stresses basically mean nominal stresses, i.e. stresses prevailing in the regions with regular geometry away from discontinuities.
- The maximum stresses are associated with discontinuities.
- The shown results correspond to the components specific most severe load transients.
- The shown results are principal stresses, the directions of which vary.

Table 8.2.5-1. Summary of the stress analysis results for components that screened in according screening process Step 1. Notations: high significance = Yes, low significance = Low, negligible = No, with WRSs = (*).

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>Overall stresses [MPa]</th>
<th>Maximum stress [MPa]</th>
<th>WRSs Yes/Low/No</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>Spring beams, Support brackets</td>
<td>-200…200</td>
<td>380</td>
<td>Low</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>0…200</td>
<td>360</td>
<td>Yes</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator supports</td>
<td>-10…5</td>
<td>10</td>
<td>Low</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>-100…150</td>
<td>280</td>
<td>Yes</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>50-200</td>
<td>350</td>
<td>Low</td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>40…80</td>
<td>80</td>
<td>Low</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>-20…10</td>
<td>10</td>
<td>Low</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>-10…5</td>
<td>5</td>
<td>Low</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud</td>
<td>-7…0</td>
<td>0</td>
<td>Low</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>-100…100</td>
<td>180</td>
<td>Yes</td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>-100…0</td>
<td>100</td>
<td>Yes</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>-10…100</td>
<td>150</td>
<td>Low</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>-100…150</td>
<td>300</td>
<td>Yes</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>-30…150 (*)</td>
<td>210 (*)</td>
<td>Low</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>50…100</td>
<td>200</td>
<td>Yes</td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>0…200</td>
<td>320</td>
<td>Yes</td>
</tr>
</tbody>
</table>

**8.2.6 Results from CUF analysis**

The background information here is:
- the CUF results by EQE International for the OL1/OL2 RPV and internals under load transient ADS [429,430], and
- the CUF results by FS Dynamics for the Feedwater sparger under all significant load transients [431].

Here, the considered times in operation and associated numbers of load cycles correspond to 40, 60 and 80 years in the OL1/OL2 units. To get the CUF values for all mentioned times in operation, linear interpolation and extrapolation was applied, where necessary.

As the CUF analyses [429,430] do not cover all structural details considered in this work, some approximations are done. For these details this involved taking the CUF value from a similar structural detail. For instance, the CUF value given in ref. [430] for the spring beams was also applied to the support brackets.
Another issue is the scaling of the CUF analysis results for the load transient ADS [429,430] to correspond to the other considered load transients. This was simply done by scaling the CUF value from ref. [430] for the other considered load transients according to the load cycle specific maximum stress range taking place in the inner surface of the component in question. This was computed for all load transients that each considered component is budgeted to experience. The component specific total CUF values were then obtained by summing up the load transient specific CUF values for the considered time spans, i.e. 40, 60 and 80 years of operation. To further clarify what has been done by who, all CUF values are taken from the analysis results by the EQE International [429,430], whereas the further applying and interpolation or extrapolation of them has been carried out by the author of this work.

When the value of CUF reaches 1.0 or exceeds it, a fatigue crack growth (FCG) analysis is done. The CUF results are presented in Table 8.2.6-1.

Table 8.2.6-1. Summary of CUF results for components that screened in according to screening process Step 1.

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>CUF [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>after 40 years</td>
</tr>
<tr>
<td>4</td>
<td>Spring beams, Support brackets</td>
<td>0.1</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>0.1</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator supports</td>
<td>0.1</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>12.8</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>38.5</td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>3.8</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>3.8</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>6.6</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>7.7</td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>8.0</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>0.2</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>0.2</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>9.6</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>10.2</td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>7.7</td>
</tr>
</tbody>
</table>

8.3 Final screening of considered components

The final screening of the OL1/OL2 RPV and its internals for ageing degradation analyses is described in the following. The scope of this Step 2 screening is limited to concern those 15 components that screened in according screening process Step 1. To obtain expert judgement support for the performed screening process, it was discussed with some experts in TVO. The developed screening approach is described in Section 7.1. As defined therein, a component screens in according to Step 2 when one or more of the specified screening values are reached or exceeded.
As there is a quite large amount of applicable screening value data, it is not shown here. This data is often specific to material type, stress level and/or temperature. Here, the main background information document for the screening value data is MRP-175 [30].

Summaries of the stress and CUF data used in the screening process Step 2 are shown in Table 8.2.5-1 and Table 8.2.6-1, respectively. The fluence data is from a proprietary ref. [378] for the OL2 unit. Table 8.3-1 shows the results from the screening process Step 2 for the considered components. The degradation data and the corresponding screening data values are described in more detail in the component specific degradation potential and criticality analyses in Section 8.4.

Table 8.3-1. The results from the screening process Step 2 for the considered components. The abbreviations for the degradation mechanisms are explained in Table 3.1-1 in Section 3.1. Notations: screening criterion value is denoted with "SCR".

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>Screening against screening criteria</th>
<th>Screening result (In/Out)</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>Spring beams, Support brackets</td>
<td>IE ≥ SCR: No TE ≥ SCR: No CUF ≥ SCR: No IGSCC ≥ SCR: No IASCC ≥ SCR: No GC ≥ SCR: No E/C, FAC ≥ SCR: No MW ≥ SCR: No</td>
<td>Out</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>No Yes No No No No No In</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>Steam separator support legs</td>
<td>No No No No No No No Out</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>Yes Yes Yes No No No No In</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>No Yes Yes No No No No Out</td>
<td></td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>No No Yes No No No No Out</td>
<td></td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>No No Yes No Yes No No No In</td>
<td></td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>No No Yes No Yes No No No In</td>
<td></td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>No No Yes No No No No In</td>
<td></td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>No No Yes Yes No No No In</td>
<td></td>
</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>No No Yes Yes No No No In</td>
<td></td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>No No No Yes No Yes No No In</td>
<td></td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>No Yes Yes Yes No No No No In</td>
<td></td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell</td>
<td>Yes Yes Yes No No No No In</td>
<td></td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>Yes Yes Yes Yes No No No In</td>
<td></td>
</tr>
<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>Yes Yes Yes Yes No No No In</td>
<td></td>
</tr>
</tbody>
</table>

Supplementary remarks concerning the screening process Step 2:
- ID 11 Feedwater spargers were eventually screened out because it is replaceable and its safety class is only 3.
The components that screened in according to the screening process Step 2 are:
- Steam outlet nozzles,
- Feedwater nozzles,
- Control rods,
- Control rod guide tubes,
- Core shroud, Core shroud support,
- Pump deck,
- Core shroud support legs,
- Instrumentation guide tubes and nozzles,
- Control rod guide tubes and nozzles at RPV bottom,
- Cylindrical RPV shell,
- Shutdown cooling nozzles,
- Core spray nozzles.

12 components screened in according to the screening process Step 2 out of the 16 components that screened in according to the screening process Step 1.

8.4 Degradation potential and criticality analyses

The degradation potential and criticality analyses for the components that screened in according to the Step 2 are described in the following. However, this is limited only to the 6 most important ones, see Section 5.1 for these components. These analyses are also carried out for the rest of the components that screened in according to the Step 2, which is reflected in Sections 9 to 11.

These analyses concern the assessment of time dependent propagation of the degradation in the susceptible components. This corresponds to the degradation potential of these components. The degradation criticality corresponds to assessment of the critical flaw size for the structural detail in question. Typically, considered critical states are leak and break. Due to high safety demands, maximum allowable flaw sizes have been defined for NPP components in codes and norms. For instance, according to ASME Section XI [81] for narrow cracks in pipes and nozzles the maximum allowable depth through wall is 75 % of the wall thickness, whereas for longer cracks it is less than that. The considered initial flaws can be either existing, i.e. detected in inspections and then measured, or postulated. In both cases the purpose is to show with degradation potential analyses that a flaw either does not grow, or grows so slowly that it will not reach the critical size within the planned time in operation.

For NPPs the typical design lifetime is 40 years. Extension can be applied for the time in operation, e.g. in the U.S. more than 70 NPPs have been granted to continue their time in operation from 40 to 60 years [438,439]. In Europe, Borssele in The Netherlands is one of the first NPPs whose originally planned operational time of 40 years has been extended to 60 years [440]. Thus, in addition to ageing management of NPP operation, the degradation potential and criticality analyses are also needed for license renewal purposes. In that connection, these analyses are called time-limited ageing analyses (TLAAs).

STUK has defined the requirements concerning the lifetime extension of Finnish NPPs in Guide YVL A.8 “Ageing management of a nuclear facility” [444]. Therein, it is stated that: "The licensee shall possess a technically reasoned estimate of the service life for systems, structures and components (SSCs) susceptible to ageing. The service life can be extended if the operability
of the SSCs can be verified over a service life longer than the original estimate. The procedures based on strength analyses are discussed in Guide YVL E.4 [354].” As for actual procedures concerning strength and crack analyses, YVL E.4 [354] refers to ASME Sections III [147] and XI [81] but also allows using other procedures, once they have been accepted by STUK. In ref. [445] STUK describes in more detail their position on the conduct of license renewal of Finnish NPPs, as follows: “In applying for a renewal of the operating licence, the procedure to be followed is in general the same as in applying for an operating licence for a new NPP. The renewal of the operating licence, however, always involves also a periodic safety review (PSR) of the facility. PSR practice in Finland follows the IAEA’s guidance (IAEA Safety Guide NS-G-2.10) [446].”

The U.S. NRC defines TLAAs in Code of Federal Regulations 10 CFR Part 54 [441] as analyses that meet the six criteria presented below. The regulations require the licensee to provide an evaluation of the analyses that meet all of the criteria.

According to the U.S. NRC, TLAAs are those calculations and analyses to be provided by the licensee that [441]:
1) Involve systems, structures and components within the scope of license renewal, as delineated in Section 54.4(a);
2) Consider the effects of ageing;
3) Involve time-limited assumptions defined by the current operating term, for example, 40 years;
4) Were determined to be relevant by the licensee in making a safety determination;
5) Involve conclusions or provide the basis for conclusions related to the capability of the system, structure and component to perform its intended functions, as delineated in Section 54.4(b); and
6) Are contained or incorporated by reference in the current licensing basis.

From above, the item number 6) is a very specific issue for the U.S. NPPs, as for them it is not possible to add requirements after the licensing. This is not the case in most European countries.

IAEA and has also published significant documents concerning operating NPPs beyond their designed lifetimes, i.e. LTO, most notably Safety Reports Series No. 57 “Safe Long Term Operation of Nuclear Power Plants” [442]. A relevant NRC report for planning of LTO is NUREG-1800, Rev. 2 [443]. IAEA Safety Report No. 57 [442] also contains the same six criteria for TLAAs as 10 CFR Part 54 [441]. In addition, IAEA Safety Report No. 57 [442] states that for safe LTO to be allowed the TLAAs need to demonstrate that the safety analyses meet one of the following criteria:
(i) The analysis remains valid for the intended period of LTO;
(ii) The analysis has been projected to the end of the intended period of LTO; or
(iii) The effects of ageing on the intended functions of the structure or component will be adequately managed for the intended period of LTO.

As the degradation potential and criticality analyses here are performed for extended time in operation, they can also be called TLAAs. The TLAAs are degradation mechanism specific. For each considered component, the number of required TLAAs corresponds to the number of different affecting degradation mechanisms. The required TLAAs for the considered components are identified in Table 8.4-1. A representative part of them are presented after that. However, all required TLAAs were done. For each considered component, at least one TLAA is performed, for some of them two or more. This is carried out so that component specifically at least the most severe degradation mechanism is considered. For the RPV and connecting
The Steam outlet nozzles are affected by only one degradation mechanism: thermal embrittlement (TE). This concerns only the ferritic base material of the nozzle and associated weld HAZs. The TLAA for TE is performed up to 40, 60 and 80 years of operation.

The steps of the TE TLAA for the nozzle base material and associated HAZs are:
- collection of necessary input data,
- assessment of thermal ageing induced shift of $RT_{NDT}$,
- computation of thermally aged $RT_{NDT}$,
computation of fracture toughness values \( K_{lc} \) and \( K_{la} \) according to Appendix G of ASME Section XI [81] and ref. [83], corresponding to thermally aged \( RT_{NDT} \),

- computation of the decrease of the upper shelf value of both \( K_{lc} \) and \( K_{la} \) as a function of fluence with the procedure from ref. [83],
- selection of necessary crack postulates,
- computation of \( K_I \) values over the fronts of the selected crack postulates,
- determination of critical crack size corresponding to thermally aged \( RT_{NDT} \),
- conclusions concerning the TLAA results.

The necessary input data for the TE TLAA are:
- temperature under 100 % normal operation, which is [252]: \( T_{100\%} \) = 286 °C,
- initial \( RT_{NDT} \) = -23 °C, which is an average of the reported data [448],
- chemical composition of the nozzle base material is obtained from FSAR report [449],
- geometry, material and loading data are taken from Section 5, and supplemented with data from design drawings, material specifications and loading reports, where necessary,
- stress distributions through the nozzle wall during 100 % normal operation and most severe load transients are obtained from VTT report VTT-R-06249-06 [450],
- the considered total time in operation is 80 years.

The data on thermal ageing induced shift of \( RT_{NDT} \), denoted by \( \Delta RT_{NDT,\text{aging}} \), for the base metal and HAZ of the nozzle is taken from Table 4.2.1-1. According to FSAR report [449] the amount of phosphorus in the nozzle base material exceeds 80 ppm. In Table 4.2.1-1 the lowest temperature for which the \( \Delta RT_{NDT,\text{aging}} \) data is given is 300 °C. This is 14 °C higher than \( T_{100\%} \) of 286 °C. Therefore, using the data given for 300 °C is slightly conservative. The extrapolated used \( \Delta RT_{NDT,\text{aging}} \) data at 286 °C is:
- for nozzle base material after 40 and 60 years \( \Delta RT_{NDT,\text{aging}} \) = 1.5 °C, whereas after 80 years it is assumed that \( \Delta RT_{NDT,\text{aging}} \) = 1.5 °C,
- for nozzle weld HAZ after 40 and 60 years \( \Delta RT_{NDT,\text{aging}} \) = 0 °C, whereas after 80 years it is assumed that \( \Delta RT_{NDT,\text{aging}} \) = 0 °C.

As the effect of TE at 286 °C is negligible for the HAZ, TE is considered only for the base metal of the nozzle. The \( RT_{NDT} \) of thermally aged RPV steel and associated welds, \( RT_{NDT,\text{aged}} \), is computed according to 2012 edition of RCC-M [63], see equation (4.2.1-1). The results for the base metal of the nozzle are:
- after 40 and 60 years \( RT_{NDT,\text{aged}} \) = -21.6 °C,
- after 80 years \( RT_{NDT,\text{aged}} \) = -21.6 °C.

Based on these results, at 286 °C the effect of TE saturates after 40 years in operation. The critical fracture toughness for the lower bound crack initiation value \( K_{lc} \) and for the lower bound crack arrest value \( K_{la} \) are computed according to Appendix A of ASME Section XI [81], see equations (4.1.1-7). The results for the base metal of the nozzle are presented in Table 8.4.1-1 and Figure 8.4.1-1. The accuracy of this simple procedure may not be very good, but it is used here to compute the \( RT_{NDT} \) of thermally aged RPV steels and associated welds because there are no better procedures available.
Table 8.4.1-1. The computed critical fracture toughness values for the base metal of the nozzle.

<table>
<thead>
<tr>
<th>Temperature</th>
<th>Initial state</th>
<th>After 80 years</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T$ [°C]</td>
<td>$K_{ic}$ [MPa√m]</td>
<td>$K_{ia}$ [MPa√m]</td>
</tr>
<tr>
<td>20</td>
<td>220</td>
<td>98</td>
</tr>
<tr>
<td>286</td>
<td>220</td>
<td>220</td>
</tr>
</tbody>
</table>

Figure 8.4.1-1. The computed critical fracture toughness values for inner surface of the base material of the Steam outlet nozzle. In the legend, ini is initial state and 80y the aged state after 80 years in operation.

The effect of TE to fracture toughness after 80 years in operation remains small. The computations were made with best estimate approach. For temperatures above 20 °C the upper shelf $K_{ic}$ decreases less than 20 MPa√m.

When making a structural integrity analysis according to codes and standards safety factors have to be used. These are mainly related to material properties and primary stresses. Here, ASME code Section XI [81] is applied. According to Article IWB-3600 of ref. [81] for ferritic steel components with wall thickness exceeding 4 inches, i.e. 102 mm, the maximum $K_I$ value, $K_{I,max}$, of a crack under normal operational conditions is required to be less than $K_{ia}$ divided by the specified safety factor, as follows:

$$K_{I,max} < \frac{K_{ia}}{\sqrt{10}} = K_{ia, SF} \quad (8.4.1-1)$$

In terms of crack size, the upper bound of the allowable (end-of-life) crack depth is 70 % of the wall thickness. The safety factor treatment for $K_{ic}$ of the base material of the nozzle results as follows:

- at initial state at 286 °C $K_{ia, SF} = 70$ MPa√m, and at 20 °C $K_{ia, SF} = 31$ MPa√m,
• after 80 years at 286 °C $K_{Ia, SF} = 64 \text{ MPa} \sqrt{m}$, and at 20 °C $K_{Ia, SF} = 29 \text{ MPa} \sqrt{m}$.

In addition, a safety factor is given for $K_I$ caused by mechanical loads, which for RPV nozzles concerns mainly pressure. According to Appendix G of ref. [81], for ferritic base materials of the RPV and the nozzle the following requirement shall be fulfilled:

$$2K_{I_m} + K_{I_t} < K_I$$

(8.4.1-2)

where $K_{I_m}$ and $K_{I_t}$ are $K_I$ values caused separately by mechanical and thermal loads, respectively. For the RPV and its nozzles no safety factors have been defined for primary stresses. The requirement (8.4.1-2) is applied in the following as it specifically concerns RPV nozzles.

The maximum postulated defect for the Steam outlet nozzle is taken from Appendix G of ASME Section XI [81]. Therein, it is defined that this reference defect is a sharp surface breaking crack with faces perpendicular to maximum stresses, and for section thicknesses from 102 to 305 mm it has depth and length of 1/4 and 3/2 times the section thickness, respectively. The maximum stresses take place in the inner corner of the nozzle. Thus, the considered crack postulate is located there, and opens to inner surface. The symmetry axis of this semi-elliptic crack postulate goes diagonally through the nozzle wall with origin at the inner surface of the nozzle blend point. In this direction, the wall thickness is 210 mm. Thus, the depth and length of the considered crack postulate are 52.5 and 315 mm, respectively. The maximum stresses are orientated in RPV hoop direction, and are shown along the diagonal line through nozzle wall in Figure 8.4.1-2. These stresses correspond to 100 % normal operation.

![Figure 8.4.1-2. For the corner of the Steam outlet nozzle: hoop stresses through wall along diagonal line with origin at the inner surface of the nozzle blend point under 100 % normal operation [450].](image)
For the considered crack postulate the maximum values of $K_{lm}$ and $K_{lt}$ are computed using the BIE/IF equation for corner cracks in nozzles from report BWROG-TP-11-022, Rev. 1 [451]. It can be written as:

$$K_I = \sqrt{\frac{\pi a}{1000}} \left[ 0.706C_0 + 0.537 \left( \frac{2a}{\pi a_{adj}} \right) C_1 + 0.448 \left( \frac{a}{2a_{adj}} \right)^2 C_2 + 0.393 \left( \frac{4a}{3\pi a_{adj}} \right)^3 C_3 \right] \times 6.895E-03$$

(8.4.1-3)

where the unit of $K_I$ is MPa$\sqrt{m}$, that of crack depth $a$ is mm, $a_{adj} = 1.0$ mm, whereas $C_0$, $C_1$, $C_2$ and $C_3$ are coefficients of third order polynomial curve fit for the stress distribution. When using equation (8.4.1-3), the input data is first converted to these units, and then the computed results are converted back to SI units. The $K_{lm}$ and $K_{lt}$ reach their maximum values at the deepest point, i.e. crack tip. For the considered loading conditions, the results are:

- $K_{lm} = 78.3$ MPa$\sqrt{m}$, and
- $K_{lt} = 7.0$ MPa$\sqrt{m}$.

Now, after 80 years in operation the requirement of equation (8.4.1-2) is computed as:

$$2K_{lm} + K_{lt} = 2 \times 78.3 + 7.0 = 163.6 \text{ MPa}\sqrt{m} < K_{lc} = 202 \text{ MPa}\sqrt{m}$$

Thus, the requirement of equation (8.4.1-2) is fulfilled. The same result holds also after 40 years in operation as $\Delta R T_{NDT, ageing}$ does not change after that. The actual structural integrity margin is considerably larger than what the result shows as a safety factor has been used and the $K_I$ computation procedure is to some extent conservative. Further, the applied crack postulate is very large. In reality, more than 10 times smaller crack would be detected in the inspections using advanced NDT techniques. In addition, the crack growth would at most be extremely slow as it can only be propagated by fatigue, and at the steam dome the number of yearly load cycles is very low and the values of the heat transfer coefficient between steam and the nozzle are low. So, possible thermal gradients over the wall and ensuing thermal stresses are very small.

### 8.4.2 TLAAs for Feedwater nozzles

The Feedwater nozzles are affected by altogether four degradation mechanisms: irradiation embrittlement (IE), TE, FCG and IGSCC. The accumulated irradiation fluence is even after 80 years in operation of a quite moderate scale. However, considering IE is included here. The empirical models for the assessment of IE are based on data from surveillance specimens that have been inside operating RPVs, i.e. the specimens have been under both irradiation and temperature loads. This means that the effect of TE is included in these models. Thus, there is no need to do a separate TE analysis. The TLAAs for IE is performed up to 80 years of operation, whereas TLAAs for IGSCC and FCG are performed up to 60 years of operation. These analyses concern the inner corner of the nozzle and the nozzle to safe-end weld of Inconel 182.

The steps of the IE TLAAs for the base metal of the nozzle are:

- the steps that are the same as those in the TE TLAAs for the Steam outlet nozzles are not repeated here,
- using $T_{41J}$ data as input data, compute $T_0$ with ASTM E 1921 [86] procedure,
using $T_0$ as input data, compute irradiation embrittlement adjusted $RT_{T0}$ (equivalent to $RT_{NDT}$) according to Appendix G of ASME Section XI [81],

- using $RT_{T0}$ as input data, compute irradiation embrittlement adjusted $K_{lc}$ and $K_{la}$ values,
- selection of necessary crack postulates,
- computation of the decrease of the upper shelf value of both $K_{lc}$ and $K_{la}$ as a function of fluence with the procedure from ref. [83],
- computation of $K_I$ values over the fronts of the selected crack postulates,
- determination of critical crack size corresponding to irradiation embrittlement adjusted $RT_{T0}$,
- conclusions concerning the TLAA results.

The steps of the IGSCC and FCG TLAA for the nozzle to safe-end weld are:

- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- deterministic computation of crack growth for 60 years of plant operation,
- determination of critical crack sizes,
- selection of inspection intervals and inspection quality,
- probabilistic computation of crack growth for 60 years of plant operation,
- conclusions concerning the TLAA results.

**TLAA for IE in the base material of the nozzle**

The necessary input data for the IE TLAA are:

- experienced fluence after 80 years of plant operation,
- initial $RT_{NDT} = -23 \, ^\circ C$, which is an average of the reported data [448],
- temperature data $T_{41/J}$ from measurements done to surveillance specimens from confidential ref. [447],
- chemical composition of the base material of the nozzle is obtained from FSAR report [449],
- geometry, material and loading data are taken from Section 5, and supplemented with data from design drawings, material specifications and loading reports, where necessary,
- stress distributions through the nozzle wall corner during 100 % normal operation and most severe load transients are obtained from VTT report VTT-R-04821-07 [425].

The irradiation embrittlement adjusted $RT_{T0}$ for the base metal of the nozzle is computed with equation (4.1.1-1) and the procedure from ref. [83]. The decrease of fluence through the wall is computed by equation (4.1.1-4). According to Appendix G of ASME Section XI [81], the depth of the reference crack for an inner nozzle corner is 1/4 times the section thickness. In this case, this depth is 61 mm.

According to resulting data for OL2 unit in ref. [378], the accumulated fluence for the inner surface of the corner of the Feedwater nozzle after 80 years in operation is $1.08E+18 \, \text{n/cm}^2$. According to FSAR report [449], the amounts of copper and nickel in the base material of the nozzle are 0.15 and 0.58 weight-%, respectively.

The computation results for the nozzle corner are as follows:

- After 80 years in operation, $RT_{T0} = -9.3 \, ^\circ C$ at inner surface and $RT_{T0} = -10.2 \, ^\circ C$ at wall depth of 61 mm.
Based on these results the values for $K_{IC}$ and $K_{IA}$ are computed according to Appendix A of ASME Section XI [81], see equations (4.1.1-9). The results for the base material of the nozzle are presented in Table 8.4.2-1 and Figure 8.4.2-1.

**Table 8.4.2-1. The computed critical fracture toughness values for the inner surface of the base material of the nozzle.**

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>$K_{IC}$ [MPa(\sqrt{m})]</th>
<th>$K_{IA}$ [MPa(\sqrt{m})]</th>
<th>$K_{IC}$ [MPa(\sqrt{m})]</th>
<th>$K_{IA}$ [MPa(\sqrt{m})]</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>220</td>
<td>98</td>
<td>158</td>
<td>72</td>
</tr>
<tr>
<td>286</td>
<td>220</td>
<td>220</td>
<td>203</td>
<td>203</td>
</tr>
</tbody>
</table>

*Figure 8.4.2-1. The computed critical fracture toughness values for the inner surface of the base material of the Feedwater nozzle. In the legend, ini is initial state and 80y the aged state after 80 years in operation.*

In lower temperatures, the effect of IE to fracture toughness after 80 years in operation is quite significant. The computations were made with best estimate approach. For temperatures above 30 °C the upper shelf $K_{IC}$ decreases less than 20 MPa\(\sqrt{m}\).

The procedure used for the structural integrity analysis is the same as that applied in Section 8.4.1-1, including equations (8.4.1-2) and (8.4.1-3). The procedure is according to ASME code Section XI [81], and it concerns ferritic steel components with wall thickness exceeding 4 inches, i.e. 102 mm.

Firstly, values are computed for $K_{IM}$ and $K_{IE}$. The considered maximum postulated defect for the Feedwater nozzle is a semi-elliptic surface crack in the inner nozzle corner with depth and length of 1/4 and 3/2 times the section thickness, respectively. The crack faces are perpendicular to RPV hoop direction and the symmetry axis of the crack goes diagonally through the nozzle wall with origin at the inner surface of the nozzle blend point. In this direction, the wall
thickness is 245 mm. Thus, the depth and length of the considered crack postulate are 61 and 368 mm, respectively. The maximum stresses are orientated in RPV hoop direction, and are shown along the diagonal line through nozzle wall in Figure 8.4.2-2. These stresses correspond to the maximum gradient of thermal stresses experienced during the load transient 14bn. Then, the temperature of the RPV water is 286 °C, whereas the temperature at the inner and outer surfaces of the nozzle corner are 286 and 280 °C, respectively. The maximum values of $K_{im}$ and $K_{it}$ are computed with equation (8.4.1-3), resulting in:

- $K_{im} = 79.0 \text{ MPa} \sqrt{\text{m}}$, and
- $K_{it} = 1.4 \text{ MPa} \sqrt{\text{m}}$.

Now, after 80 years in operation the requirement of equation (8.4.1-2) is computed under operational temperature of 286 °C as:

$$2K_{im} + K_{it} = 2 \times 79.0 + 1.4 = 159.4 \text{ MPa} \sqrt{\text{m}} < K_{lc} = 203 \text{ MPa} \sqrt{\text{m}}$$

Thus, the requirement of equation (8.4.1-2) is fulfilled. The actual structural integrity margin is considerably larger than what the result shows, as a safety factor has been used and the $K_{t}$ computation procedure is to some extent conservative. Further, the applied crack postulate is very large. In reality more than 10 times smaller crack would be detected in the inspections using advanced NDT techniques. In addition, the crack growth would at most be extremely slow, as it can only be propagated by fatigue, and for the thermally shielded nozzle the number of load cycles is well in the low-cycle regime and the thermal gradients remain low.

![Figure 8.4.2-2. For the corner of the Feedwater nozzle: maximum hoop stresses experienced during the load transient 14bn through wall along a diagonal line with origin at the inner surface of the nozzle blend point [425].](image-url)
TLAA for IGSCC in nozzle to safe-end weld

The necessary input data for the IGSCC TLAA are:

- geometry data, see Section 5.1,
- material property data for weld material Inconel 182, see Table 8.4.2-2,
- pressure and temperature loads under 100 % normal operation, see Section 8.2.2,
- stresses are taken from VTT report VTT-R-04821-07 [425],
- WRSs are according to Inspecta Technology recommendations [452] and VTT report [453],
- the considered flaw postulates are both circumferentially and axially orientated semi-elliptic cracks opening to inner surface,
- in deterministic analyses the depth of the initial crack varies from 1.0 to 22.0 mm and the corresponding length from 6.0 to 132.0 mm,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- best estimate parameter values are used in the SCC growth rate equation, see Table 8.4.2-3,
- for deterministic IGSCC cases, see Table 8.4.2-4,
- the probabilistic analysis is done for a circumferentially orientated semi-elliptic crack opening to inner surface,
- in probabilistic analyses the depth and length of the initial crack are according to NURBIT recommendations [454,455], where the initial depth is 1.0 mm and the initial length varies according an exponential probability density function,
- in probabilistic analyses the initiation frequency of SCC induced cracks is taken as 4.08E-04 per year per weld [454],
- the considered inspection intervals are 3, 5 and 10 years as well as the case of no inspection,
- quality of inspections follows definitions by Inspecta Technology [457],
- the assumed yearly time in operation is 8000 hours, which is approximately 11 months,
- the considered total time in operation is now 60 years.

Table 8.4.2-2. Relevant material property values of alloy Inconel 182 [418].

<table>
<thead>
<tr>
<th>Temperature [°C]</th>
<th>Young’s modulus [GPa]</th>
<th>Yield strength [MPa]</th>
<th>Tensile strength [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>214</td>
<td>241</td>
<td>586</td>
</tr>
<tr>
<td>286</td>
<td>199</td>
<td>220</td>
<td>586</td>
</tr>
</tbody>
</table>

Table 8.4.2-3. Values of parameters CSCC and nSCC used in the SCC equation (4.4.1-1) for alloy Inconel 182, data is from ref. [456]. The dimensions used in the crack growth equation are: $[\frac{da}{dt}] = \text{mm/year}, [K_i] = \text{MPa}\sqrt{\text{m}}$.

<table>
<thead>
<tr>
<th>CSCC</th>
<th>nSCC</th>
<th>Environment</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.71E-07</td>
<td>4.96</td>
<td>water</td>
</tr>
</tbody>
</table>
Table 8.4.2-4. List of deterministic IGSCC analysis cases.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Orientation</th>
<th>Initial crack depth [mm]</th>
<th>Initial crack length [mm]</th>
<th>WRSs according to</th>
</tr>
</thead>
<tbody>
<tr>
<td>1a</td>
<td>axial</td>
<td>1.0</td>
<td>6.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>1b</td>
<td>axial</td>
<td>2.0</td>
<td>12.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>1c</td>
<td>axial</td>
<td>4.0</td>
<td>24.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>1d</td>
<td>axial</td>
<td>5.0</td>
<td>30.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>2</td>
<td>axial</td>
<td>1.0</td>
<td>6.0</td>
<td>FE results by VTT</td>
</tr>
<tr>
<td>3a</td>
<td>circumferential</td>
<td>5.0</td>
<td>30.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>3b</td>
<td>circumferential</td>
<td>10.0</td>
<td>60.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>3c</td>
<td>circumferential</td>
<td>20.0</td>
<td>120.0</td>
<td>Inspecta Technology</td>
</tr>
<tr>
<td>3d</td>
<td>circumferential</td>
<td>22.0</td>
<td>132.0</td>
<td>Inspecta Technology</td>
</tr>
</tbody>
</table>

The anticipated(typical yearly load transients are not taken into account in the computational analyses as IGSCC concerns only stationary operational conditions. In the IGSCC analyses, equation (4.4.1-1) is used. Of the applied WRSs, the recommendations by Inspecta Technology [452] give higher tensional stresses through wall than the FE simulation results by VTT [453]. The latter WRSs are considered to be more realistic, as unlike the former WRSs they do not contain any conservative assumptions. The deterministic IGSCC computations are performed with deterministic VTTBESIT, whereas the corresponding probabilistic computations are performed with probabilistic VTTBESIT.

The critical end-of-life (EOL) crack sizes for the considered analyses are computed according to definitions in Section XI of the ASME code [81]. For cylindrical parts of nozzles and pipe components the EOL crack sizes are determined according to Appendix C of ASME XI [81]. Therein, the safety factors for primary membrane and bending stresses are given in Article C-2600. Using thus treated stresses the EOL crack sizes are computed with procedures described in Articles C-5000 to C-7000, depending on the case in question. Of these, Article C-6000 presents an EPFM based procedure and Article C-7000 a LEFM based procedure.

The results from the deterministic IGSCC analysis, cases 1a to 1e and 2, are presented in Figure 8.4.2-3, whereas the results in case 3 are presented in Figure 8.4.2-4. Both of these figures include the EOL crack sizes.

The results from the probabilistic crack growth analyses are presented in Figure 8.4.2-5. This concerns only the circumferentially orientated crack postulate, because for an axially orientated crack postulate there is no applicable estimate for the probability density function of the initial crack size distribution. The considered failure state is a crack grown through wall and producing a leak.
Figure 8.4.2-3. Results from the deterministic IGSCC analysis, axially orientated cases 1a to 1e and 2, showing time dependent crack growth.

Figure 8.4.2-4. Results from the deterministic IGSCC analyses, hoop orientated cases 3a to 3d, showing time dependent crack growth.
Figure 8.4.2-5. Leak probability results for a circumferentially orientated crack, i.e. case 3, after 60 years in operation.

TLAA for FCG in nozzle to safe-end weld

The necessary input data for the FCG TLAA are:
- geometry and material property data are the same as for the IGSCC TLAA,
- pressure and temperature loads caused by anticipated/typical load transients, see Section 8.2.2,
- stress cycles are taken from VTT report VTT-R-04821-07 [425], whereas their order and number of occurrence are taken from VTT report VTT-R-07746-07 [458], which data is summarised in Table 8.4.2-5 below,
- WRSs are not needed in the analyses as they remain constant as a function of time,
- the considered flaw postulates are both circumferentially and axially orientated semi-elliptic cracks opening to inner surface,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- in deterministic analyses the depth of the initial crack varies from 1.0 to 10.0 mm and the corresponding length from 6.0 to 60.0 mm,
- for reasonably conservative parameter values used in the FCG rate equation, see Table 8.4.2-6,
- for deterministic FCG analyses, see Table 8.4.2-7,
- as the resulting crack growth for FCG is much slower than for IGSCC probabilistic analyses are not performed,
- the considered total time in operation is now 60 years.
Table 8.4.2-5. Anticipated load cycles for the Feedwater nozzles during a typical year of plant operation, according to refs. [422,458]. Stress range data is from analyses reported in ref. [425]. Full names of load transients are given in Section 8.2.2.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>load cycle</th>
<th>No. of occurrences</th>
<th>Maximum stress range [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Axial</td>
</tr>
<tr>
<td>1</td>
<td>10) + 1)</td>
<td>6</td>
<td>160</td>
</tr>
<tr>
<td>2</td>
<td>4a)</td>
<td>1</td>
<td>&lt; 30</td>
</tr>
<tr>
<td>3</td>
<td>6sb)</td>
<td>14</td>
<td>70</td>
</tr>
<tr>
<td>4</td>
<td>15)</td>
<td>5</td>
<td>&lt; 30</td>
</tr>
<tr>
<td>5</td>
<td>2c) + 5a)</td>
<td>3</td>
<td>118</td>
</tr>
<tr>
<td>6</td>
<td>3)</td>
<td>13</td>
<td>&lt; 30</td>
</tr>
<tr>
<td>7</td>
<td>2c) + 9c)</td>
<td>2</td>
<td>118</td>
</tr>
<tr>
<td>8</td>
<td>4b)</td>
<td>1</td>
<td>95</td>
</tr>
<tr>
<td>9</td>
<td>16c)</td>
<td>3</td>
<td>157</td>
</tr>
<tr>
<td>10</td>
<td>16b)</td>
<td>1</td>
<td>157</td>
</tr>
<tr>
<td>11</td>
<td>2c) + 7a)</td>
<td>2</td>
<td>78</td>
</tr>
<tr>
<td>12</td>
<td>6b)</td>
<td>3</td>
<td>&lt; 30</td>
</tr>
<tr>
<td>13</td>
<td>2b) + 9b)</td>
<td>1</td>
<td>137</td>
</tr>
<tr>
<td>14</td>
<td>14a)</td>
<td>1</td>
<td>95</td>
</tr>
<tr>
<td>15</td>
<td>6sa)</td>
<td>1</td>
<td>70</td>
</tr>
<tr>
<td>16</td>
<td>2b) + 5b)</td>
<td>1</td>
<td>137</td>
</tr>
<tr>
<td>17</td>
<td>ADS + 1)</td>
<td>1</td>
<td>160</td>
</tr>
<tr>
<td></td>
<td>All together</td>
<td>59 per year</td>
<td>160</td>
</tr>
</tbody>
</table>

Table 8.4.2-6. Values of parameters $C_{FA}$ and $n_{FA}$ used in the FCG equation (4.3.3-1) for alloy Inconel 182, data is from ref. [420]. The dimensions used in the crack growth equation are: $[\frac{da}{dN}] = \text{mm/cycle}$, $[\Delta K_I] = \text{MPa}\sqrt{\text{m}}$.

<table>
<thead>
<tr>
<th>$C_{FA}$</th>
<th>$n_{FA}$</th>
<th>Environment</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.55E-08</td>
<td>3.30</td>
<td>water</td>
</tr>
</tbody>
</table>

Table 8.4.2-7. List of deterministic FCG analysis cases.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Orientation</th>
<th>Initial crack depth [mm]</th>
<th>length [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>4a</td>
<td>axial</td>
<td>1.0</td>
<td>6.0</td>
</tr>
<tr>
<td>4b</td>
<td>axial</td>
<td>5.0</td>
<td>30.0</td>
</tr>
<tr>
<td>4c</td>
<td>axial</td>
<td>10.0</td>
<td>60.0</td>
</tr>
<tr>
<td>5a</td>
<td>circumferential</td>
<td>1.0</td>
<td>6.0</td>
</tr>
<tr>
<td>5b</td>
<td>circumferential</td>
<td>5.0</td>
<td>30.0</td>
</tr>
<tr>
<td>5c</td>
<td>circumferential</td>
<td>10.0</td>
<td>60.0</td>
</tr>
</tbody>
</table>

The considered loads for the computational analyses are the anticipated/typical yearly load transients. Firstly, load cycles are composed of them. As the reference state is the 100 % normal operation conditions, load cycles correspond to such changing of load parameters that start from it and return back to it. In some cases load transients as such are also load cycles. In many cases, load cycles consist of two load transients in consecutive order, meaning that the first transient ends to a state where the values of the load parameters deviate from those under 100 % normal operation, while the second transient starts from those conditions and ends to the 100 % normal operation. Anticipated load cycles for a typical year of plant operation was assembled, see Table
8.4.2-5. The corresponding computed stress cycles through the wall are used in the analyses. This loading sequence is repeated 60 times to simulate 60 years of plant operation. In the computational FCG analyses, equation (4.3.3-1) is used. The deterministic FCG computations are performed with deterministic VTTBESIT.

The results from the deterministic FCG analyses to Case 4 are presented in Figure 8.4.2-6, including the EOL crack sizes, which are the same as those obtained for the IGSCC TLAA above. In Case 4 no crack growth takes place.

![Figure 8.4.2-6. Results from the deterministic FCG analyses, axially orientated cases 4a to 4c, showing time dependent crack growth.](image)

A summary of the TLAA s performed for the Feedwater nozzles is presented in the following.

The first case is the IE analysis for the reference crack in the nozzle corner, which crack is defined according to Appendix G of ASME Section XI [81]. According to the results from the IE analysis, the maximum $K_I$ values along the crack front remain well below the corresponding fracture toughness values, even after 80 years of plant operation. The actual structural integrity margin is considerably larger than what the results show as the computation procedure includes several conservative assumptions, which are described in detail in the presentation of the results above. Thus, even after 80 years of plant operation, there is a large structural safety margin for the Feedwater nozzle corner against IE.

As can be seen from Figures 8.4.2-3, 8.4.2-4 and 8.4.2-6, the crack growth for both IGSCC and FCG is very slow for 60 years of plant operation for all but the largest crack postulates. Even for the largest crack postulates the crack growth is relatively slow. In all cases, the crack would be found in the inspections well before it has grown near to any critical size. In many cases, the crack growth is negligibly small. The crack growth rate was faster for axially orientated crack cases, which are loaded by circumferentially orientated stresses. These stresses are in general.
considerably higher in tension than the corresponding axially orientated stresses, which in turn provide the loading for circumferentially orientated cracks. The size of the largest crack postulates are of exaggerated scale, and they would be found in the inspections before any growth would have occurred. The purpose to include them in the analyses is to provide a limiting case, showing how large initial size is needed so that a crack would grow to EOL size and through wall within 60 years.

As can be seen from Figure 8.4.2-5, the leak probabilities due to IGSCC remain extremely low for 60 years in operation for a circumferentially orientated crack postulate. This reflects similar results obtained from the deterministic analyses for this crack postulate.

Probabilistic analyses were not performed for FCG because the resulting crack growth was much slower than for IGSCC.

The obtained crack growth results are also conservative. Firstly, it was assumed that initial cracks exist/nucleate from the start of the plant operation. In reality, it usually takes several years, even more than a decade, for a crack to nucleate from dormant microscopic size to macroscopic size capable to grow. Secondly, the EOL crack sizes are computed according to Section XI of the ASME code [81] using safety factors given therein for load induced primary stresses.

8.4.3 TLAAs for Control rods

The Control rods are affected by two degradation mechanisms: FCG and IASCC. As there is neither data nor models to assess the FIV and acoustic resonance loads, considering FCG is excluded here. This is reasonable, because after almost 40 years of operation no FIV or acoustic resonance induced high-cycle fatigue degradation has been detected from the OL1/OL2 Control rods. The accumulated irradiation fluence after 60 years in operation is of significant scale in this case, so IASCC is considered. The TLAA for IASCC is performed up to 60 years of operation. This analysis concerns the Control rod part under most severe irradiation, i.e. the top part just before the blades. As the stresses across the Control rod cross-section are very low, see Table 8.2.5-1, it is first checked if the IASCC propagation threshold is exceeded before continuing the TLAA.

Additional load cycles are caused to the Control rods from acceleration and deceleration in a reactor scram in the operational and cold shutdown conditions as well as from blowdown of steam into the condensation pool via system 314. As the number of these loading events is very limited they could cause only very small scale low-cycle fatigue. In addition, the Control rods are replaced after a certain number of service years, e.g. those that enter the reactor core are replaced latest after 6 years of use. The maximum acceleration of a Control rod during a reactor scram is 700 m/s² [426] and the corresponding tensional stress is 24 MPa. The maximum bending moment for a Control rod during a blowdown of steam is 1985 Nm [427] and the corresponding maximum tensional stress is 59 MPa.

The steps of the IASCC TLAA for the Control rod are:

- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- check if the IASCC threshold is exceeded, if not the analysis stops, if yes the analysis continues as follows,
- deterministic computation of crack growth for 60 years of plant operation,
- determination of critical crack sizes.
The necessary input data for the IASCC TLAA are:
- geometry data, see Section 5.1,
- material property data for base material SIS 2353,
- pressure and temperature loads under 100 % normal operation, see Section 8.2.2,
- stresses are taken from FS Dynamics report [424],
- the considered flaw postulate is a circumferentially orientated semi-elliptic crack opening to the inner surface,
- in deterministic analyses the depth and length of the initial crack varies, starting from 7.0 and 14.0 mm, respectively,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- the water chemistry of OL1 and OL2 units is considered, which is NWC,
- IASCC growth rate equation (4.4.2-1a) by Eason and Pathania [184],
- fluence data is taken from analysis results to OL2 unit [378],
- the assumed yearly time in operation is 8000 hours, which is approximately 11 months.

The anticipated/typical yearly load transients are not taken into account in the computational analyses as IASCC concerns only stationary operational conditions. The analysed part of the Control rod does not contain welds. The deterministic $K_I$ computations are performed with applicable solution for a round bar from the Murakami handbook [459]. It is not possible to use VTTBESIT here for the IASCC computation as its scope does not include round bars.

Susceptibility to IASCC is affected by the following characteristics according to Section 4.4.4:
- All susceptibility characteristics described in Section 4.4.3 for IGSCC also apply to IASCC [1].
- Based on available field and laboratory data, neutron fluence ($E > 1\text{ MeV}$) threshold of approximately $5.0 \times 10^{24} \text{ n/m}^2$ exists for annealed types 304, 304L, 347 and 348 stainless steel components under high tensile stress, and approximately $2.0 \times 10^{25} \text{ n/m}^2$ for components under moderate or low tensile stress [192, 193]. Welded and irradiated RPV internals, such as the core shroud, appear to have lower threshold fluence due to the presence and interaction of weld sensitization, high WRSs and irradiation.

The above mentioned two threshold values are used more often in unit of n/cm$^2$ as $5.0 \times 10^{20}$ and $2.0 \times 10^{21}$ n/cm$^2$, respectively. According to the fluence analysis result data [378], the maximum fluence of the Control rod exceeds these threshold values earlier than five years in operation. Thus, susceptibility to IASCC exists. Another issue is the threshold for propagation of an initial crack that has been nucleated by IASCC.

The rod material SIS 2353 corresponds to stainless steel TP 316L in the U.S. standards. For this material the IGSCC growth threshold $K_{I,th}$ is for sensitized state [189]: $K_{I,th} = 10.5\text{ MPa} \sqrt{\text{m}}$.

The $K_I$ values for the circumferential crack postulate computed by the Murakami handbook [459] solution cover here a crack depth range from 7.0 to 35.0 mm, while the considered aspect ratio was kept as 1.0. The outer diameter of the Control rod is 70 mm, see Section 5.1. Thus, the largest considered crack size is almost half of the cross-section area. The geometry variables of the Murakami handbook solution are shown in Figure 8.4.3-1 below. For this case, the crack depth is taken to equal $a_{el}$, i.e. in depth direction $a_{el}$ starts from -90$^\circ$ surface point and extends to point A, whereas $a_{el}/b_{el}$ is kept as 1.0. The $K_I$ values are computed at the crack tip and crack edge points.
For axial loading the $K_I$ solution is [459]:

$$K_{I,F} = \frac{K_{I,F}}{\sigma_F \sqrt{\pi a}}$$  \hspace{1cm} (8.4.3-1)

where $K_{I,F}$ is $K_I$ [MPa√m] for pure tension, $\sigma_F$ [MPa] is tensional membrane stress, $a$ [m] is crack depth and $K_{I,F}$ [-] is the dimensionless stress intensity parameter, the values for which are given in the associated table in ref. [459]. Solving equation (8.4.3-1) for $K_I$ gives the following expression:

$$K_{I,F} = \frac{\tilde{K}_{I,F}}{\sigma_F \sqrt{\pi a}}$$  \hspace{1cm} (8.4.3-2)

which was used in the $K_I$ value computations. The $K_I$ results for the crack tip and crack edge points as well as the associated $K_{I,th}$ values are presented in Figure 8.4.3-2 below. Therein, for convenience units of mm are used for $a$.

As can be seen from Figure 8.4.3-2, even for the largest considered crack size covering almost half of the cross-section area the $K_I$ values remain well below the $K_{I,th}$ for the material in question. This is due to low prevailing stresses under 100 % normal operation. Cracks, which are much smaller than the maximum size considered here, would be detected in the inspections using modern NDT techniques. It is concluded that IASCC does not cause crack growth in the Control rod during 60 years of plant operation.
8.4.4 TLAAs for Core shroud, Core shroud support

The Core shroud and Core shroud support are affected by one degradation mechanism: FCG. For IASCC, the experienced fluence after 60 years in operation is below the screening value, and for both IASCC and IGSCC the stresses are below the screening value. The fluence data is taken from the recent confidential report [378]. The TLAA for FCG is performed up to 60 years of operation. This analysis concerns the weld near the top of the Core shroud wall.

The steps of the FCG TLAA for the top weld of the Core shroud wall are:

- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- deterministic computation of crack growth for 60 years of plant operation,
- determination of critical crack sizes.

The necessary input data for the FCG TLAA are:

- geometry data, see Section 5.1,
- material property data for weld material Inconel 182, see Table 8.4.2-2,
- pressure and temperature loads caused by anticipated/typical load transients, see Section 8.2.2,
- stress cycles are taken from VTT report VTT-R-04821-07 [425] and from some supplementary computations made with analysis code DIFF [414], see Table 8.4.5-1 below,
- the order and number of occurrence of load cycles are taken from VTT report VTT-R-07746-07 [458],
- WRSs are not needed in the analyses as they remain constant as a function of time,
- because hoop stresses are much higher than axial stresses, the considered flaw postulate is an axially orientated semi-elliptic crack opening to inner surface,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- in deterministic analyses the depth of the initial crack varies from 1.0 to 10.0 mm and the initial length from 6.0 and 60.0 mm,
for reasonably conservative parameter values used in the FCG rate equation, see Table 8.4.2-6,
- for deterministic FCG analysis cases, see Table 8.4.4-2 below.

Table 8.4.4-1. Anticipated load cycles for a typical year of plant operation, according to refs. [422,458]. Stress range data is mainly from analyses concerning ref. [425]. Full names of load transients are given in Section 8.2.2.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>load cycle</th>
<th>No. of occurrences</th>
<th>Maximum hoop stress range [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>10) + 1)</td>
<td>5</td>
<td>63</td>
</tr>
<tr>
<td>2</td>
<td>2) + 5a)</td>
<td>3</td>
<td>61</td>
</tr>
<tr>
<td>3</td>
<td>2) + 5b)</td>
<td>1</td>
<td>62</td>
</tr>
<tr>
<td>4</td>
<td>2) + 6b)</td>
<td>3</td>
<td>61</td>
</tr>
<tr>
<td>5</td>
<td>2) + 7a)</td>
<td>1</td>
<td>64</td>
</tr>
<tr>
<td>6</td>
<td>2) + 7d)</td>
<td>1</td>
<td>60</td>
</tr>
<tr>
<td>7</td>
<td>2) + 9)</td>
<td>4</td>
<td>61</td>
</tr>
<tr>
<td>8</td>
<td>2) + 14b)</td>
<td>1</td>
<td>92</td>
</tr>
<tr>
<td>9</td>
<td>4)</td>
<td>2</td>
<td>32</td>
</tr>
<tr>
<td>10</td>
<td>6sa)</td>
<td>1</td>
<td>48</td>
</tr>
<tr>
<td>11</td>
<td>ADS + 1)</td>
<td>1</td>
<td>271</td>
</tr>
<tr>
<td>Altogether</td>
<td></td>
<td>23 per year</td>
<td>271</td>
</tr>
</tbody>
</table>

Table 8.4.4-2. List of deterministic FCG analysis cases.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Orientation</th>
<th>Initial crack depth [mm]</th>
<th>length [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1a</td>
<td>axial</td>
<td>1.0</td>
<td>6.0</td>
</tr>
<tr>
<td>1b</td>
<td>axial</td>
<td>2.0</td>
<td>12.0</td>
</tr>
<tr>
<td>1c</td>
<td>axial</td>
<td>4.0</td>
<td>24.0</td>
</tr>
<tr>
<td>1d</td>
<td>axial</td>
<td>5.0</td>
<td>30.0</td>
</tr>
<tr>
<td>1e</td>
<td>axial</td>
<td>10.0</td>
<td>60.0</td>
</tr>
</tbody>
</table>

The considered load cycles for the computational analyses are composed of the anticipated/typical yearly load transients, see Table 8.4.4-1. The corresponding stress cycles through the wall are used in the analyses. This loading sequence is repeated 60 times to simulate 60 years of plant operation. In the computational FCG analyses, equation (4.3.3-1) is used. The deterministic FCG computations are performed with deterministic VTTBESIT.

The maximum EOL crack depth for the considered analyses is as defined in Section XI of the ASME code [81]. The results from the deterministic FCG analyses are presented in Figure 8.4.4-1.
As can be seen from Figure 8.4.4-1 the FCG rate is at most very slow for 60 years of plant operation, even for the largest initial crack postulate. The maximum crack growth through wall within 60 years is less than 0.1 mm for this crack postulate with initial depth and length of 10 and 60 mm, respectively. The crack growth for FCG remains well under maximum EOL crack depth for 60 years of plant operation.

8.4.5  TLAAs for Core shroud support legs

The Core shroud support legs are affected by two degradation mechanisms: FCG and IGSCC. The TLAAs for IGSCC and FCG are performed up to 60 years of operation. These analyses concern the welds connecting the Core shroud support legs to the RPV bottom. The material of this weld and the buffer below it is Inconel 182, which is susceptible to IGSCC. The buffers below the support legs have undergone PWHT together with the RPV, but the bottom and top welds of the support leg have not.

The steps of the IGSCC and FCG TLAAs for the Core shroud support legs are:

- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- deterministic computation of crack growth for 60 years of plant operation,
- determination of critical crack sizes.

TLAA for IGSCC in the bottom weld of the Core shroud support leg

The necessary input data for the IGSCC TLAA are:

- geometry data, see Section 5.1,
- material property data for weld material Inconel 182, see Table 8.4.2-2,
- pressure and temperature loads under 100 % normal operation, see Section 8.2.2,
• all stresses, including also FE simulated WRSs, are taken from VTT report VTT-CR-07610-13 [463],
• the considered flaw postulates are, in RPV coordinates, radially orientated semi-elliptic cracks opening to inner surface of the bottom weld of the support leg,
• in deterministic analyses the depth of the initial crack varies from 1.0 to 12.0 mm and the corresponding length from 6.0 to 72.0 mm,
• it is assumed that initial cracks exist/nucleate from the start of the plant operation,
• best estimate parameter values used in the SCC growth rate equation, see Table 8.4.2-3,
• for deterministic IGSCC analysis cases, see Table 8.4.5-1 below,
• the assumed yearly time in operation is 8000 hours, which is approximately 11 months.

Table 8.4.5-1. List of deterministic IGSCC analysis cases for the bottom welds of the Core shroud support leg. The crack growth directions through wall for crack postulates 1 to 4 are shown in Figure 8.4.5-1.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Initial crack depth [mm]</th>
<th>Length [mm]</th>
<th>Orientation in RPV coordinates</th>
<th>Direction</th>
</tr>
</thead>
<tbody>
<tr>
<td>1a</td>
<td>1.0</td>
<td>6.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>1b</td>
<td>5.0</td>
<td>30.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>1c</td>
<td>10.0</td>
<td>60.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>1d</td>
<td>12.0</td>
<td>72.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>2a</td>
<td>1.0</td>
<td>6.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>2b</td>
<td>2.0</td>
<td>12.0</td>
<td>radial</td>
<td>through leg bottom weld</td>
</tr>
<tr>
<td>3a</td>
<td>1.0</td>
<td>6.0</td>
<td>radial</td>
<td>through buffer and RPV</td>
</tr>
<tr>
<td>3b</td>
<td>2.0</td>
<td>12.0</td>
<td>radial</td>
<td>through buffer and RPV</td>
</tr>
<tr>
<td>3c</td>
<td>3.0</td>
<td>18.0</td>
<td>radial</td>
<td>through buffer and RPV</td>
</tr>
<tr>
<td>4a</td>
<td>1.0</td>
<td>6.0</td>
<td>radial</td>
<td>through buffer and RPV</td>
</tr>
<tr>
<td>4b</td>
<td>2.0</td>
<td>12.0</td>
<td>radial</td>
<td>through buffer and RPV</td>
</tr>
</tbody>
</table>

Figure 8.4.5-1. For the support leg of the Core shroud, the crack growth directions through the walls for crack postulates 1 to 4.
As mentioned earlier, in terms of loading IGSCC concerns only stationary operational conditions. Equation (4.4.1-1) is used in the computational IGSCC analyses. Of the prevailing stresses, locally high tensional WRSs govern the stress field at and near the surfaces of the support leg bottom weld. As the tensional stresses are at maximum in hoop direction the considered crack postulates are orientated in radial direction in the RPV coordinate system. The deterministic IGSCC computations are performed with deterministic VTTBESIT.

Due to the geometry and rigid boundary conditions of the Core shroud support legs it is not feasible to determine critical EOL crack sizes for it. For instance, even a relatively large local through wall crack on one leg would not compromise the overall structural integrity of it, let alone that of the other legs.

The results from the deterministic IGSCC analyses are presented in Figure 8.4.5-2. Therein, in the legends twall is wall thickness and buffer/RPV is the material interface between the buffer and the RPV bottom.

![Figure 8.4.5-2a](image_url)

*Figure 8.4.5-2a. Results from the deterministic IGSCC analyses, for crack postulates 1a to 1d, showing time dependent crack growth.*
Figure 8.4.5-2b. Results from the deterministic IGSCC analyses, for crack postulates 2a and 2b, showing time dependent crack growth.

Figure 8.4.5-2c. Results from the deterministic IGSCC analyses, for crack postulates 3a to 3c, showing time dependent crack growth.
Figure 8.4.5-2d. Results from the deterministic IGSCC analyses, for crack postulates 4a and 4b, showing time dependent crack growth.

**TLAA for FCG in the bottom weld of the Core shroud support leg**

The necessary input data for the FCG TLAA are:
- geometry and material property data are the same as for the IGSCC TLAA,
- pressure and temperature loads caused by anticipated/typical load transients, see Section 8.2.2,
- stress cycles are taken from VTT report VTT-CR-07610-13 [463], whereas their order and number of occurrence are taken from VTT report VTT-R-07746-07 [458], which data is summarised for the support legs in Table 8.4.5-2 below,
- note, that due to location of the support legs at the RPV bottom the RPV downcomer load transients have mostly evened out, and the only affecting significant load cycles are Shutdown followed by Start-up and ADS followed by Start-up,
- WRSs are not needed in the analyses as they remain constant as a function of time,
- the considered flaw postulates are the same as in the TLAA for IGSCC, see Table 8.4.5-1,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- for reasonably conservative parameter values used in the FCG rate equation, see Table 8.4.2-6.

*Table 8.4.5-2. Applied load cycles for the Core shroud support legs during a typical year of plant operation, according to refs. [422,458]. Stress range data is from analyses reported in ref. [425]. Full names of load transients are given in Section 8.2.2.*

<table>
<thead>
<tr>
<th>Case no.</th>
<th>load cycle</th>
<th>No. of occurrences</th>
<th>Maximum hoop stress range [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>10) + 1)</td>
<td>6</td>
<td>220</td>
</tr>
<tr>
<td>2</td>
<td>ADS + 1)</td>
<td>1</td>
<td>220</td>
</tr>
</tbody>
</table>
The stress cycles through the wall are used. The yearly loading sequence consisting of stress cycles is repeated 60 times to simulate 60 years of plant operation. In the computational FCG analyses equation (4.3.3-1) is used. The deterministic FCG computations are performed with deterministic VTTBESIT.

According to the FCG analyses, the maximum crack growth through wall after 60 years of plant operation is for Case 1d, 0.27 mm. As this value is practically negligible, no additional result is shown here.

A summary of the IGSCC and FCG TLAAs performed to the Core shroud support leg bottom weld is presented in the following.

As can be seen from Figure 8.4.5-2, the crack growth due to IGSCC is mostly negligible for Case 1, and only when the initial depth exceeds 10 mm there is considerable crack growth, i.e. for Case 1d. However, the crack growth is so slow that it would likely be detected well before the crack grows through the wall. For Case 2, the crack growth due to IGSCC is faster. Still, there would be enough time to detect the crack in the yearly inspections before it grows through wall. To be on the safe side, it was assumed that the initial cracks exist from the start of plant operation. In reality, the incubation time for IGSCC to nucleate is several years, usually more than 10 years. For FCG, there are only a few yearly load cycles, and consequently even for the largest initial crack postulate the ensuing crack growth is extremely slow, the maximum value being less than 0.3 mm after 60 years of plant operation.

8.4.6 TLAAs for Cylindrical RPV shell including welds

The Cylindrical RPV shell is affected by altogether three degradation mechanisms: IE, TE and FCG. As mentioned earlier, the effect of TE is included in the empirical models for IE. Thus, there is no need to do a separate TE analysis. The TLAAs for IE and FCG are performed up to 80 years of operation. These analyses concern the cladded RPV shell with regular cylindrical geometry. The reasons to perform the analyses up to 80 years of operation, instead of up to 60 years as is the case for most of the other considered components, include that in practise the RPV is not replaceable. When having continued the time in operation from 40 to 60 years, the results from the present analyses would be of interest if TVO would be plan to continue the time in operation by 20 years more.

The steps of the IE TLAA for the RPV shell base material are as follows:

- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- using $T_{A1J}$ data as input data, compute $T_0$ with ASTM E 1921 [86] procedure,
- using $T_0$ as input data, compute irradiation embrittlement adjusted $RT_{T0}$ (equivalent to $RT_{NDE}$) according to Appendix G of ASME Section XI [81],
- using $RT_{T0}$ as input data, compute irradiation embrittlement adjusted $K_{lc}$ and $K_{la}$ values,
- selection of necessary crack postulates,
- computation of the decrease of the upper shelf value of both $K_{lc}$ and $K_{la}$ as a function of fluence with the procedure from ref. [83],
- computation of $K_I$ values over the fronts of the selected crack postulates,
- determination of critical crack size corresponding to irradiation embrittlement adjusted $RT_{T0}$,
- conclusions concerning the TLAA results.
The steps of the FCG TLAA for the RPV shell are as follows:
- the steps that are the same as those in the TE TLAA for the Steam outlet nozzles are not repeated here,
- deterministic computation of crack growth for 80 years of plant operation,
- determination of critical crack sizes.

TLAA for IE in the base material of the RPV shell using the procedure in R.G 1.99 [18]

The necessary input data for the IE TLAA are:
- accumulated fluence after 80 years of plant operation,
- initial $RT_{NDT} = -23 \, ^\circ C$, which is an average of the reported data [448],
- chemical composition of the RPV shell base material is obtained from FSAR report [449],
- geometry, material and loading data are taken from Section 5, and supplemented with data from design drawings, material specifications and loading reports, where necessary,
- stress distributions through RPV shell wall during 100 % normal operation and most severe load transients are obtained from FS Dynamics report [424] and VTT report VTT-R-06020-14 [419].

The irradiation embrittlement adjusted $RT_{NDT}$ for the RPV shell base metal is computed with equation (4.1.1-1). To be able to do that, $\Delta RT_{NDT}$ and material specific margin are computed first with equations (4.1.1-2) and (4.1.1-3), respectively. The decrease of fluence through wall is computed with equation (4.1.1-4). According to Appendix G of ASME Section XI [81], the depth of the reference crack for the RPV shell is 1/4 times the wall thickness. In this case, this depth is 34.8 mm.

According to result data for OL2 unit in ref. [378], the accumulated maximum fluence for the RPV shell inner surface after 80 years in operation is 1.58E+18 n/cm$^2$. According to FSAR report [449], the amounts of copper and nickel in the base material of the RPV shell are 0.15 and 0.58 weight-%, respectively.

Based on these results, the values for $K_{Ic}$ and $K_{Ia}$ are computed according to Appendix A of ASME Section XI [81] and Appendix G of ASME Section III [147], see equations (4.1.1-7a) and (4.1.1-7b), respectively. The results for the RPV shell base and weld materials are presented in Table 8.4.6-1 and Figure 8.4.6-1. In the legends Figure 8.4.6-1, “ini” is initial state and “80y” the aged state after 80 years in operation.

**Table 8.4.6-1. The computed critical fracture toughness values for the inner surface of the base and weld materials of the RPV shell.**

<table>
<thead>
<tr>
<th>Material</th>
<th>Temperature $T$ [°C]</th>
<th>Initial state</th>
<th>After 80 years</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$K_{Ic}$ [MPa√m]</td>
<td>$K_{Ia}$ [MPa√m]</td>
<td>$K_{Ic}$ [MPa√m]</td>
</tr>
<tr>
<td>base</td>
<td>20</td>
<td>220</td>
<td>102</td>
</tr>
<tr>
<td>base</td>
<td>286</td>
<td>220</td>
<td>220</td>
</tr>
<tr>
<td>weld</td>
<td>20</td>
<td>220</td>
<td>102</td>
</tr>
<tr>
<td>weld</td>
<td>286</td>
<td>220</td>
<td>220</td>
</tr>
</tbody>
</table>
In lower temperatures the effect of IE to fracture toughness after 80 years in operation is quite significant. The computations were made with best estimate approach. For temperature above around 50 °C the decrease of the upper shelf $K_{Ic}$ of the base material is less than 40 MPa$\sqrt{m}$.
The procedure used for the structural integrity analysis is the same as that applied in Section 8.4.1, including using equations (8.4.1-2) and (8.4.1-3). The procedure is according to ASME code Section XI [81], and it concerns ferritic steel components with wall thickness exceeding 4 inches, i.e. 102 mm.

Firstly, values are computed for $K_{Im}$ and $K_{It}$. The considered maximum postulated defect is a semi-elliptic surface crack in the RPV shell opening to inner surface with depth and length of $1/4$ and $3/2$ times the section thickness, respectively. The crack faces are orientated perpendicular to RPV hoop direction. The depth and length of the considered crack postulate are 34.8 and 208.5 mm, respectively. The maximum stresses are orientated in hoop direction, and are shown through the RPV shell wall in Figure 8.4.6-2. These stresses correspond to the maximum gradient of thermal stresses experienced during the load transient ADS. Then, the temperature of the RPV water is 129 °C, whereas the temperature at the inner and outer surfaces of the RPV are 145 and 253 °C, respectively. The maximum values of $K_{Im}$ and $K_{It}$ are computed with deterministic VTTBESIT. The results are:

- $K_{Im} = 26.6 \text{ MPa} \sqrt{m}$, and
- $K_{It} = 34.4 \text{ MPa} \sqrt{m}$.

![Figure 8.4.6-2. For RPV shell: hoop stresses through wall corresponding to the maximum gradient of thermal stresses experienced during the load transient ADS [419,424].](image)

Now, after 80 years in operation the requirement of equation (8.4.1-2) is computed under operational temperature of 286 °C as:

$$2K_{Im} + K_{It} = 2 \times 34.4 + 26.6 = 95.3 \text{ MPa} \sqrt{m} < K_{It} = 185 \text{ MPa} \sqrt{m}$$

Thus, the requirement of equation (8.4.1-2) is fulfilled after 80 years in operation. The actual structural integrity margin is considerably larger than what the result shows, as a safety factor has been used and the $K_{It}$ computation procedure is to some extent conservative. Further, the applied crack postulate is very large, in reality more than 10 times smaller crack would be...
detected in the inspections using advanced NDT techniques. In addition, the crack growth would at most be extremely slow, as it can only be propagated by fatigue, and for the RPV shell the number of load cycles is well in the low-cycle regime and mostly the thermal gradients remain low.

**TLAA for IE in the base material of the RPV shell using the procedure in ASME XI 2015 Edition [81]**

The necessary input data for the IE TLAA are:

- the same as those needed for the IE TLAA for the RPV shell using the procedure in R.G. 1.99 [18].

The irradiation embrittlement adjusted $RT_{NDT}[^\circ C]$ for RPV shell base metal is computed as follows:

$$RT_{NDT} = RT_{NDT,initial} + \Delta RT_{NDT}$$

(8.4.6-1)

The irradiation embrittlement induced shift of $RT_{NDT}[^\circ C]$ is computed by:

$$\Delta RT_{NDT} = MF + CRP$$

(8.4.6-2)

Therein, the parameter $MF[^\circ C]$ is dependent on temperature, phosphorus, manganese and fluence, whereas the parameter $CRP[^\circ C]$ is dependent on nickel, copper, phosphorus and fluence, respectively. For computation of $MF$ and $CRP$, see ref. [81].

Again, the decrease of fluence through the wall is computed by equation (4.1.1-4) and the depth of the reference crack for the RPV shell is 34.8 mm. The accumulated maximum fluence for the RPV shell inner surface after 80 years in operation is as in the mentioned previous IE analysis. According to FSAR report [449] the amount of affecting elements in RPV shell base material are:

- phosphorus = 0.015 weight-%,
- manganese = 1.35 weight-%,
- copper = 0.15 weight-%,
- nickel = 0.58 weight-%.

The computation results for the RPV shell wall are:

- After 80 years in operation, $\Delta RT_{NDT} = 39.9 \, ^\circ C$ at inner surface, and $\Delta RT_{NDT} = 28.7 \, ^\circ C$ at wall depth of 34.8 mm.
- The irradiation embrittlement adjusted $RT_{NDT} = -23+39.9 = 16.8 \, ^\circ C$ at inner surface, and adjusted $RT_{NDT} = -23+28.7 = 5.7 \, ^\circ C$ at wall depth of 34.8 mm.

Based on these results, the values for $K_{ic}$ and $K_{ia}$ are computed by equations in Appendix A of ASME Section XI [81] and Appendix G of ASME Section III [147]. In this work these are equations (4.1.1-7a) and (4.1.1-7b). The results for the base material of the RPV shell are presented in Table 8.4.6-2 and Figure 8.4.6-3.
Table 8.4.6-2. The computed critical fracture toughness values for the inner surface of the base material of the RPV shell.

<table>
<thead>
<tr>
<th>Material</th>
<th>Temperature $T$ [ºC]</th>
<th>$K_{ic_{ini}}$ [MPa√m]</th>
<th>$K_{ia_{ini}}$ [MPa√m]</th>
<th>$K_{ic_{80y}}$ [MPa√m]</th>
<th>$K_{ia_{80y}}$ [MPa√m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>base</td>
<td>20</td>
<td>220</td>
<td>101</td>
<td>97</td>
<td>55</td>
</tr>
<tr>
<td>base</td>
<td>286</td>
<td>220</td>
<td>220</td>
<td>193</td>
<td>193</td>
</tr>
<tr>
<td>weld</td>
<td>20</td>
<td>220</td>
<td>101</td>
<td>103</td>
<td>57</td>
</tr>
<tr>
<td>weld</td>
<td>286</td>
<td>220</td>
<td>220</td>
<td>195</td>
<td>195</td>
</tr>
</tbody>
</table>

In lower temperatures, the effect of IE to fracture toughness after 80 years in operation is quite significant. For temperature above around 45 ºC the decrease of the upper shelf $K_{ic}$ of the base material is less than 30 MPa√m.

The procedure used for the structural integrity analysis is the same as that applied in Section 8.4.1-1, including using equations (8.4.1-2) and (8.4.1-3). The procedure is according to ASME code Section XI [81], and it concerns ferritic steel components with wall thickness exceeding 4 inches, i.e. 102 mm. Now, after 80 years in operation the requirement of equation (8.4.1-2) is computed under operational temperature of 286 ºC as:

$$2K_{ia_{80y}} + K_{lt} = 2 \times 34.4 + 26.6 = 95.3 \text{ MPa√m} < K_{ic} = 193 \text{ MPa√m}$$

Thus, the requirement of equation (8.4.1-2) is fulfilled after 80 years in operation. The actual structural integrity margin is considerably larger than what the result shows, as a safety factor has been used and the $K_l$ computation procedure is to some extent conservative. Further, the applied crack postulate is very large, in reality more than 10 times smaller crack would be detected in the inspections using advanced NDT techniques. In addition, the crack growth
would at most be extremely slow, as it can only be propagated by fatigue, and for the RPV shell the number of load cycles is well in the low-cycle regime and mostly the thermal gradients remain low.

![Figure 8.4.6-3b. The computed critical fracture toughness values for the inner surface of the weld material of the RPV shell.](image)

**TLAA for FCG in the base material of the RPV shell**

The necessary input data for the FCG TLAA are:

- geometry data, see Section 5.1,
- material property data for base material ASTM 533 Grade B, Class 1, see Table 8.2.1-1,
- pressure and temperature loads caused by anticipated/typical load transients, see Section 8.2.2,
- stress cycles are taken from VTT report VTT-R-04821-07 [425], whereas their order and number of occurrence are taken from VTT report VTT-R-07746-07 [458], which data is summarised in Table 8.4.6-3 below,
- WRSs are not needed in the analyses as they remain independent of time,
- the considered flaw postulate is an axially orientated semi-elliptic crack opening to inner surface because hoop stresses are much higher than axial stresses,
- it is assumed that initial cracks exist/nucleate from the start of the plant operation,
- in deterministic analyses, the depth of the initial crack varies from 5.0 to 20.0 mm and the initial length from 30.0 and 120.0 mm,
- for reasonably conservative parameter values used in the FCG rate equation, see Table 8.4.6-4 below,
- for deterministic FCG analyses, see Table 8.4.6-5 below.
Table 8.4.6-3. Anticipated load cycles for a typical year of plant operation, according to refs. [422,458]. Stress range data is from analyses concerning ref. [425]. Full names of load transients are given in Section 8.2.2.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>load cycle</th>
<th>No. of occurrences</th>
<th>Maximum hoop stress range [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>10) + 1)</td>
<td>5</td>
<td>155</td>
</tr>
<tr>
<td>2</td>
<td>2) + 5a)</td>
<td>3</td>
<td>106</td>
</tr>
<tr>
<td>3</td>
<td>2) + 5b)</td>
<td>1</td>
<td>106</td>
</tr>
<tr>
<td>4</td>
<td>2) + 6b)</td>
<td>3</td>
<td>108</td>
</tr>
<tr>
<td>5</td>
<td>2) + 7a)</td>
<td>1</td>
<td>100</td>
</tr>
<tr>
<td>6</td>
<td>2) + 7d)</td>
<td>1</td>
<td>101</td>
</tr>
<tr>
<td>7</td>
<td>2) + 9)</td>
<td>4</td>
<td>111</td>
</tr>
<tr>
<td>8</td>
<td>2) + 14b)</td>
<td>1</td>
<td>184</td>
</tr>
<tr>
<td>9</td>
<td>4)</td>
<td>2</td>
<td>101</td>
</tr>
<tr>
<td>10</td>
<td>6sa)</td>
<td>1</td>
<td>82</td>
</tr>
<tr>
<td>11</td>
<td>ADS + 1)</td>
<td>1</td>
<td>337</td>
</tr>
<tr>
<td>Altogether</td>
<td>23 per year</td>
<td></td>
<td>337</td>
</tr>
</tbody>
</table>

Table 8.4.6-4. Values of parameters $C_{FA}$ and $n_{FA}$ used in the FCG equation (4.3.3-1) for ASTM 533 Grade B, Class 1, data is from ref. [420]. The dimensions used in the crack growth equation are: $[da/dN] = \text{mm/cycle}$, $[\Delta K] = \text{MPa} \sqrt{\text{m}}$.

<table>
<thead>
<tr>
<th>$C_{FA}$</th>
<th>$n_{FA}$</th>
<th>Environment</th>
</tr>
</thead>
<tbody>
<tr>
<td>5.337E-06</td>
<td>1.95</td>
<td>water</td>
</tr>
</tbody>
</table>

Table 8.4.6-5. List of deterministic FCG analysis cases.

<table>
<thead>
<tr>
<th>Case no.</th>
<th>Orientation</th>
<th>Initial crack depth [mm]</th>
<th>Initial crack length [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1a</td>
<td>axial</td>
<td>5.0</td>
<td>30.0</td>
</tr>
<tr>
<td>1b</td>
<td>axial</td>
<td>10.0</td>
<td>60.0</td>
</tr>
<tr>
<td>1c</td>
<td>axial</td>
<td>15.0</td>
<td>90.0</td>
</tr>
<tr>
<td>1d</td>
<td>axial</td>
<td>20.0</td>
<td>120.0</td>
</tr>
</tbody>
</table>

The considered load cycles for the computational analyses are composed of the anticipated/typical yearly load transients, see Table 8.4.6-3. The corresponding stress cycles through wall are used in the analyses. This loading sequence is repeated 80 times to simulate 80 years of plant operation. In the computational FCG analyses equation (4.3.3-1) is used. The deterministic FCG computations are performed with deterministic VTTBESIT.

The maximum EOL crack depth for the considered analyses is as defined in Section XI of the ASME code [81]. The results from the deterministic FCG analyses are presented in Figure 8.4.6-4.
A summary of the TLAAs performed to the Cylindrical RPV shell is presented in the following.

As the IE result, the maximum $K_I$ values over the crack front remain well below the corresponding fracture toughness values after 80 years of plant operation. By comparing the results computed with the procedure in R.G 1.99 [18] and the procedure in ASME XI 2015 Edition [81], it is seen that the former procedure is more conservative. For adjusted $R_{T_{NDT}}$ at the inner surface and at wall depth of 34.8 mm, the former procedure gives results that are 5.8 and 12.3°C higher, respectively, than those given by the latter procedure. Quite clearly, the procedure in ASME XI 2015 Edition [81] is more accurate than the procedure in R.G 1.99 [18] as it is based on almost five times more measured data points and on much improved understanding of the physical phenomena in question. The approximated maximum fluence experienced by the OL2 RPV after 80 years in operation is $1.58E+18 \text{ n/cm}^2$ [378]. This stays well within the applicability range of the procedure in ASME XI 2015 Edition [81], see Section 4.1.1. With the procedure in ASME XI 2015 Edition [81] the resulting $K_{Ic}$ and $K_{Ik}$ values return to the maximum of 240 MPa$\text{m}$ at 6°C lower temperature values than with the procedure in R.G 1.99 [18].

The actual structural integrity margin against IE is considerably larger than what the results show, because the computation procedure includes several conservative assumptions. These are described in detail above. Thus, even after 80 years of plant operation there is a large structural safety margin for the RPV shell against IE.

As can be seen from Figure 8.4.6-4, the crack growth for FCG is slow in all cases for 80 years of plant operation, even for the largest initial crack postulate. The maximum crack growth through wall within 80 years is approximately 4.6 mm for this crack postulate with initial depth and length of 20 and 120 mm, respectively. The crack growth for FCG remains well under maximum EOL crack depth for 80 years of plant operation.
9 Assessment of structural risk for failure for the OL1/OL2 RPV and its internals

A structural risk assessment procedure is developed for BWR RPV and its internals. This is mainly based on the EPRI risk matrix procedure, see ref. [5]. In the following, it is applied to OL1/OL2 RPV and its internals. The structural risk assessment for all 31 considered components but ID15 is carried out as based on the assessed POF values and on the consequences of the possible component failure. The component specific POF values, i.e. failure potential values, are computed as based on the screening and TLAA results, and cover all significant affecting degradation mechanisms.

The failure potential classification of the considered components as based on the computed POF and crack growth data is such that those components that are not susceptible to any degradation mechanism causing crack growth or brittle fracture are assigned to the lowest class. Here the lowest class corresponds to negligible POF. Those components that are susceptible to one or more degradation mechanisms but result with extremely slow crack growth are assigned to the next higher class. Those components that are susceptible to one or more degradation mechanisms and result with some mm of crack growth during 60 years are assigned to the further next higher class. And those components that result with rapid crack growth are assigned to the highest class. Here the highest class corresponds to high POF values.

The applied consequence measure is conditional core damage probability (CCDP). TVO has provided CCDP data for the 30 components considered here, as documented in confidential ref. [465]. The considered consequence is the opening/loss of I3 isolation, resulting with loss of coolant. The classification of CCDP data is based on the numerical probabilistic safety objectives provided by STUK in YVL Guide A.7 [466]. Therein, it is stated that: "The design of a nuclear power plant unit shall be such that the mean value of the frequency of the reactor core damage is less than 1.0E-05 per year."

The following classification is used for the failure potential evaluation:

- 0 = No: degradation is very unlikely to occur.
- 1 = Low: degradation mechanisms may cause slow crack growth.
- 2 = Moderate: degradation mechanisms may cause moderate crack growth.
- 3 = High: degradation mechanisms may cause rapid crack growth and even component failure.

The following classification is used for the consequence evaluation:

- 0 = Very low: CCDP < 1.0E-08.
- 1 = Low: 1.0E-08 ≤ CCDP < 1.0E-06.
- 2 = Moderate: 1.0E-06 ≤ CCDP < 1.0E-05.
- 3 = High: CCDP ≥ 1.0E-05.

The risk matrix procedure is applied to assess structural risks for failure. This is nowadays a commonly used approach in assessment of structural risks for NPP components. For instance, EPRI risk matrix procedure [5] is widely used in the U.S. to assess structural risks for NPP piping components and systems. The developed risk matrix applied here is shown in Figure 9-1. Even though the classification system used for both degradation potential and consequences is quite sparse, it was feasible for the quantitative assessment of the structural risks. In this procedure, the component specific risks are computed by multiplying the degradation potential values with the associated consequence values. However, as here the dimensions of both
degradation potential and consequence classes are qualitative, the resulting risk values are qualitative too. The main benefit in this approach lies in the capability to rank the considered components in terms of structural risk for failure. Thus, the components with the highest structural risks can be identified.

The following classification is used for structural risks for failure:
- 0 = Very low.
- 1-2 = Low.
- 3-4 = Moderate.
- 6 = High.
- 9 = Very high.

![Figure 9-1. The developed risk matrix for assessment of structural risks for failure.](image)

The results from the component specific assessment of structural risks for failure are presented in Table 9-1 below.
Table 9-1. The results from the component specific assessment of structural risks for failure. The applied risk classification system is presented in Figure 9-1.

<table>
<thead>
<tr>
<th>ID</th>
<th>Component</th>
<th>Failure potential</th>
<th>Consequence of failure</th>
<th>Structural risk for failure</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Flange cooling spray piping</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>2</td>
<td>Long nozzle pipes in cooling spray piping</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>3</td>
<td>Evacuation pipe</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>4</td>
<td>Spring beams and support brackets</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>5</td>
<td>Steam dryer</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>6</td>
<td>Steam outlet nozzles</td>
<td>1</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>7</td>
<td>Steam separator stand pipes</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>8</td>
<td>Steam separator pipe bundles</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>9</td>
<td>Steam separator support legs</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>10</td>
<td>Feedwater nozzles</td>
<td>2</td>
<td>3</td>
<td>6</td>
</tr>
<tr>
<td>11</td>
<td>Feedwater spargers</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>12</td>
<td>Boron spray nozzles and piping</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>13</td>
<td>Core spray piping outside core shroud cover</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>14</td>
<td>Core spray piping inside core shroud cover</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>15</td>
<td>Fuel assemblies</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>16</td>
<td>Control rods</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>17</td>
<td>Control rod guide tubes</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>18</td>
<td>Core shroud, Core shroud support</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>19</td>
<td>Pump deck</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>20</td>
<td>Main circulation pump nozzles</td>
<td>1</td>
<td>2</td>
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</tr>
<tr>
<td>21</td>
<td>Core shroud support legs</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>22</td>
<td>Instrumentation guide tubes and nozzles</td>
<td>1</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>23</td>
<td>Control rod guide tubes and nozzles at RPV bottom</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>24</td>
<td>Cylindrical RPV shell including welds</td>
<td>1</td>
<td>3</td>
<td>3</td>
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<tr>
<td>25</td>
<td>RPV bottom</td>
<td>1</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>26</td>
<td>RPV support skirt</td>
<td>0</td>
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<td>27</td>
<td>RPV flange</td>
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<td>28</td>
<td>RPV-head</td>
<td>0</td>
<td>3</td>
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<tr>
<td>29</td>
<td>RPV-head bolts</td>
<td>0</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td>30</td>
<td>Shutdown cooling nozzles</td>
<td>1</td>
<td>3</td>
<td>3</td>
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<tr>
<td>31</td>
<td>Core spray nozzles</td>
<td>1</td>
<td>3</td>
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</tr>
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</table>
10 Summary

This work concerns the susceptibility of BWR RPV and its internals to various degradation mechanisms. The work consists of the following three main parts:

- literature survey and review,
- component specific survey on susceptibility to degradation mechanisms,
- computational analysis approaches, tools and examples.

The objectives of the work are described first. This is accompanied with the identification of the new developments. The main objective is to demonstrate that the operational lifetime of a BWR RPV can be safely extended to at least 80 years.

The literature survey on degradation mechanisms that can/could affect the BWR RPV and/or its internals is thorough. This includes an overview and detailed phenomenological descriptions of degradation mechanisms. Interaction of degradation mechanisms is considered as well. The relevant degradation mechanisms are:

- irradiation embrittlement,
- thermal embrittlement,
- fatigue,
- stress corrosion cracking,
- general corrosion,
- local corrosion,
- erosion-corrosion, flow accelerated corrosion,
- creep, and
- mechanical wear.

The observed cracking degradation history of the BWR RPVs and their internals is described component specifically in more detail with representative examples.

The work describes in detail the major practical aspects of the relevant degradation mechanisms that affect the BWR RPV and its internals, with evaluating the potential significance of their effects on the continued performance of safety functions of the considered components throughout the plant service life. For each significant degradation mechanism the following issues are described:

- computational modelling, and
- susceptibility.

This is followed with an investigation on the susceptibility of the BWR RPV and its internals to degradation. Of BWR plants the scope of this work concerns in particular those designed by Asea-Atom (nowadays Westinghouse), focusing on the third generation. The internals determined as significant in TVO report [24] and IAEA-TECDOC-1471 [1] were selected for more detailed consideration. These components are from TVO units OL1/OL2. Altogether 31 components were selected. These components include RPV, Steam separator support legs, Steam outlet nozzles, Feedwater nozzles, Control rods, Core shroud, Core spray nozzles and Control rod guide tubes at the RPV bottom, to point out some significant ones.

The following issues are described for the considered 31 OL1/OL2 components:

- main dimensions,
- material properties,
- susceptibility to degradation mechanisms, and
• immunity/resistance against degradation mechanisms.

The loads specific to BWR RPV and internals are described. This covers several kinds of loads. The considered components are in contact with hot and moving liquid coolant during normal plant operations. The coolant saturation temperature corresponding to the system pressure is just below 290 °C [252]. Internal components located in the vicinity of the core are also exposed to fast neutron fluxes \( E > 1.0 \text{ MeV} \) and gamma irradiation. The operating environment inside a BWR RPV generates many loads that are considered to propagate ageing related or time dependent degradation mechanisms. All loads covered here can be classified as applied loads, environmental loads and manufacturing induced loads. In a more detailed level, the covered loads can be divided into:
• mechanical and thermal loads,
• irradiation loads,
• dynamic loads,
• process-chemical loads, and
• acoustic loads.

The work describes the screening process for the considered components. For NPPs the purpose of a screening process is to select the components for further analyses, degradation potential analyses in particular. The objective of the degradation potential analyses is to:
• identify the degradation mechanisms to which the components are susceptible to,
• to assess the increase in failure occurrences,
• to assess the remaining lifetime of the components, and
• to find suitable means to prevent or mitigate the effects of ageing degradation.

The developed new screening process is described. It is based mainly on the EPRI approach [372]. It has the following two steps: preliminary screening and final screening. The first step does not involve structural mechanics or CUF analyses, whereas the more detailed second step does. Preliminary screening is performed for the considered 31 components. Based on the results, 16 components screened in. Only they are considered in further analyses.

Computational approaches, tools and analyses apply mainly structural mechanics and fracture mechanics. Temperature distributions, stresses and strains for the analysed components are computed with general purpose FE codes and/or with analytical equations, where applicable. In most degradation potential analyses, fracture mechanics based analysis code VTTBESIT is used. Both deterministic and probabilistic crack growth computation procedures are described and used. The performed stress, strain and CUF analyses are described. In many cases, the stress and CUF data has been extracted from existing results, which are in most cases computed by VTT. This was necessary to prevent the scope of the work becoming too large. Keeping in mind that the focus of the work is on the susceptibility and degradation potential issues. The degradation potential analyses use the results from the heat transfer and stress/strain analyses as part of the necessary input data. The summaries of the stress and CUF analysis results are presented, together with the associated references for cases where the data has been extracted from earlier analysis results.

The final screening of the considered components is described. In this screening step, the stress and CUF analysis results are used. Of the 16 components that screened in from the preliminary screening, 12 components screened in from the final screening. Several of these components are assessed to be susceptible to more than one degradation mechanism. These components include all those mentioned above in the connection of susceptibility of the BWR RPV and its internals to degradation.
The degradation potential and criticality analyses are described for the components that screened in according to the final screening. Of the degradation mechanisms, the TLAAs concern IE, TE, FCG, IGSCC and IASCC. The maximum component specific number of TLAAs is four, which concerns the Feedwater nozzles, Shutdown cooling nozzles and Core spray nozzles. The TLAAs for the RPV and connecting main nozzles span 80 years of operational lifetime, whereas those for all other components span 60 years. This is because, unlike most of the internals, the RPV is not replaceable. When having continued the time in operation from 40 to 60 years, the results from the present analyses would be of interest if it would be planned to continue the time in operation by 20 years more.

As the empirical models for the assessment of IE are based on data from surveillance specimens that have been inside operating RPVs, i.e. the specimens have been under both irradiation and temperature loads, the effect of TE is included in these models. Thus, when an IE TLAA is performed, there is no need to do a separate TE analysis. The computed fatigue induced crack growth is very slow for the components experiencing most severe cyclic loading. This allowed omitting the FCG TLAAs for some components with less severe cyclic loading. According to the TLAA results, the crack growth is in most cases very slow. In addition, the obtained crack growth results are also conservative. Firstly, it is assumed that initial cracks exist/nucleate from the start of the plant operation. In reality it usually takes several years, even more than a decade, for a crack to nucleate from dormant microscopic size to macroscopic size capable to grow. Secondly, the EOL crack sizes are computed according to Section XI of the ASME code [81] using safety factors given therein for load induced primary stresses. In a couple of cases the computed crack growth is of significant scale. For instance, according to the TLAA results for the Core shroud, IGSCC can propagate quite rapidly in the support leg bottom weld. However, these results are conservative. Concerning all performed TLAAs, crack growth is in all cases so slow that any growing crack would be found in the inspections well before it reaches any critical size. In most cases, the resulting crack growth is negligible.

As the main objective of this work concerns the BWR RPV, the TLAA results concerning it are summarised in the following. This component is affected by altogether three degradation mechanisms: IE, TE and FCG. As mentioned earlier, the effect of TE is included in the empirical models for IE. According to the IE TLAA results, the maximum $K_I$ values for the considered reference crack in the RPV shell remain well below the corresponding fracture toughness values after 80 years of plant operation. The actual structural integrity margin is considerably larger than what the results show, because the computation procedure includes safety factors and several conservative assumptions. Thus, even after 80 years of plant operation there is a large structural safety margin for the RPV against IE. FCG in the RPV shell is slow in all cases for 80 years of plant operation, even for the largest initial crack postulate. The maximum crack growth through wall within 80 years is approximately 4.6 mm for this crack postulate with initial depth and length of 20 and 120 mm, respectively. FCG remains well under maximum EOL crack depth for 80 years of plant operation.

Finally, the developed structural risk assessment procedure is described. This procedure is mainly based on the EPRI risk matrix procedure, see ref. [372]. It is applied to the OL1/OL2 RPV and its internals. It uses as input data the computed TLAA results and CCDP data. For most components, the computed risk class was low, for a few of them it was moderate and for one of them it was high.
11 Conclusions

The main purposes of this work are to:
- present a thorough survey on the susceptibility of BWR RPV and all its significant internals to degradation mechanisms,
- carry out a screening process to these components,
- do computational degradation potential analyses for those components that screened in,
- do a structural risk assessment analysis for all considered components,
- describe and apply new analysis developments,
- demonstrate that the operational lifetime of the RPV and connecting main nozzles can be safely extended to even 80 years, and
- demonstrate that the operational lifetime of the internals of the RPV can be safely extended to at least 60 years.

It is concluded that the survey and review on the susceptibility of the BWR RPV and its internals to various degradation mechanisms is thorough. All possible degradation mechanisms affecting NPP components are taken into account. Then, through review, those significant for the BWR RPV and its internals are described further in detail. This includes the available modelling procedures. Mitigation, inspection and monitoring of degradation are covered too.

It is then concluded that for the BWR RPV and its internals the scope of the considered components is sufficient. All significant components are covered. Only the fuel assemblies are excluded from the analyses, because they are not load bearing, while the scope of this work is limited to load bearing components. However, the irradiation emanating from the fuel assemblies and affecting all nearby load bearing components is taken fully into account in the respective TLAAAs.

The conclusion of the developed process for component screening is that it is technically sound and detailed enough. It is in line with the screening process by EPRI [372]. The screening process is successfully applied to the OL1/OL2 RPV and its internals.

Based on the TLAA results, it is concluded that the operational lifetime of the internals of the OL1/OL2 RPV can be safely continued from 40 to at least 60 years. Most importantly, it is concluded that the operational lifetime of the OL1/OL2 RPV and connecting main nozzles can be safely continued from 40 to even 80 years. According to the conservative TLAA results the degradation in terms of crack growth is in most cases very or extremely slow. In the few cases with faster crack growth the cracks would be detected in the inspections well before they grow to any significant size.

According to the results from the structural risk assessment for the OL1/OL2 RPV and its internals, for all components but one the computed risk class is moderate or lower. For the Feedwater nozzle the risk class is high. However, the associated degradation potential is very small. The relatively high risk result for this component is governed by the high consequence measure value. It is concluded that for the OL1/OL2 RPV and its internals the overall structural risks are small and even in the maximum risk case acceptable. It is concluded that the developed risk matrix procedure is applicable to BWR RPVs and their internals.

The susceptibility of the BWR RPV and its internals should be considered further. Even though this issue is covered quite extensively nowadays, there is room for improvement and further
investigations. For instance, the assessment for the onset of crack growth due to fatigue and SCC would benefit from more accurate and reliable threshold data. This would require more laboratory testing and increased understanding of the underlying physical phenomena. There are also gaps in assessing the susceptibility of joint action of degradation mechanisms. For instance, the existing measured data concerning joint action of fatigue and corrosion is too scarce. Thus, more laboratory tests should be carried out, which would also help to better understand the underlying physical phenomena.

The models of the degradation mechanisms affecting BWR RPV and its internals should be developed further. As more laboratory test data concerning irradiation embrittlement becomes available, especially concerning longer test runs, the computational irradiation embrittlement models should be updated accordingly. The computational models for fatigue induced crack growth, SCC and IASCC contain empirically defined parameters. As more laboratory test data becomes available, these models should be updated accordingly. The existing computational models for general corrosion, local corrosion and interaction of degradation mechanisms are relatively crude and/or contain gaps. More laboratory tests should be done to obtain the missing underlying physical data, based on which these models should be improved and/or the missing parameter data assessed. Probabilistic modelling of degradation mechanisms would benefit from the development of more robust and straightforward models. This would allow less laborious and costly analyses.

From the viewpoint of all actual NPP components, the laboratory testing procedures would benefit from further improvement. The tests should better and more accurately correspond to the actual plant conditions. For instance, fatigue tests are typically done with small round bars under uniaxial mechanical loading. These tests could be improved by doing them to piping components loaded from the inside with flowing water having altering temperature, as corresponding to operational NPP conditions.

The techniques/procedures concerning mitigation, inspection and monitoring of the degradation mechanisms would benefit from further development. This concerns more the commercial enterprises providing these services, and they are actually doing development more or less continuously. However, they in turn need results from research efforts to better understand how to carry out the needed further development.

Modelling of loads experienced by the BWR RPV and its internals could be improved. This would require both more measurement data as well as further development and use of applicable analysis tools. In the structural mechanics based analyses, design load transients are often used. They are simplified and conservative presentations as compared to the actual load transients. More measured data on load transients should be recorded and collected. This could be supplemented with more accurately modelled loading data, where necessary, computed e.g. with thermal-hydraulics and computational fluid mechanics (CFD) analysis tools. Using more accurate load data, more accurate and realistic structural mechanics and degradation propagation analysis results would be obtained.

The applied numerical analysis tools would benefit from further development. The overall development is obviously done by the commercial enterprises providing these analysis tools. Concerning research efforts, the development issues could include user defined models, routines and special element types tailored to NPP components. An example of this would be development of modelling capabilities for a crack growing in two or more materials. This condition corresponds to e.g. the RPV nozzle to safe-end welds.
References


44. Erve, M., et al., Core Shroud Cracking in BWR-NPP and Respective Preventive Measures Taken for NPP Isar 1. Proceedings of the 22nd MPA Seminar, Stuttgart, Germany, October 10 and 11, 1996, pp. 10.1-10.29.
45. General Electric Services Information Letter (SIL) 289R1S2, Cracking In Core Spray Piping, January 5, 1990.


82. ASME Boiler and Pressure Vessel Code, Section XI. American Society of Mechanical Engineers (ASME), New York, 2017 Edition.


140. Safety Standards of the Nuclear Safety Standards Commission (KTA), Parts 3204. Germany.


152. Ljustell, P. Fatigue crack growth experiments and analyses - from small scale to large scale yielding at constant and variable amplitude loading. Doctoral thesis no. 81, KTH School of Engineering Sciences, Department of Solid Mechanics, Royal Institute of Technology, Stockholm, Sweden, 2013.


172. ASME Section XI, Division 1, Article A-4000. American Society of Mechanical Engineers (ASME), New York, 2010 Edition.


282. Input information – Nozzle to safe-end, safe-end to pipe and orifice ring welds of main inset nozzles at OL 1 and 2. Teollisuuden Voima Oy, Finland, 2003.


290. OL1 - Järjestelmä 222 - Säätösaavat - Lopullinen Turvallisuusseloste. Report 106089, Teollisuuden Voima Oy (TVO), Finland, October 2013. (in Finnish)

291. OL1/OL2 - Järjestelmän 212 Lopullinen Turvallisuusseloste - Sydämen Tukirakenteet. Report 106077, Teollisuuden Voima Oy (TVO), Finland. (in Finnish)


295. Design Drawing 138853, Rev. D. TVO II Reaktortank - Moderatortank Stativ -
Insvektsnig av Stödben, UDDCOMB Sweden AB, 1976.
296. Design Drawing 135414, Rev. G. TVO I Reaktortank - Tankunderdel, UDDCOMB
Sweden AB, 1974.
298. Design Drawing 235189, Rev. B, TVO I Reaktortank - Moderatortankstativ - Stödben,
UDDCOMB Sweden AB, 1976.
300. Design Drawing 235191, Rev. B, TVO I Reaktortank - Moderatortankstativ - Konring -
301. Design Drawing 235190, Rev. A, TVO I Reaktortank - Moderatortankstativ -
305. OL1 - Final Safety Analysis Report for System 216 - Reactor Instruments Mechanical
Equipment. Report 106081, Teollisuuden Voima Oy (TVO), Finland.
306. Design Drawing AA 103372, Neutrondetektorledrörs - Neutron-detector guide tube,
307. Design Drawing AA 101422, Neutrondetektorstuts - Connection for in-core
308. Design Drawing AA 101423, Neutrondetektorstuts - Connection for in-core
309. Design Drawing 14017881, Control Rod Drive Tube. Teollisuuden Voima Oy (TVO),
1993.
311. Design drawing 135266, Rev. D. TVO I Reaktortank Styrdonsstuts. UDDCOMB
312. Design drawing 135262, Rev. F. TVO I Reaktortank – Bottengavel, UDDCOMB
313. Design drawing 135395, Rev. C, TVO I Reaktortank – Sarg 3 o. 4. UDDCOMB
314. Design drawing 335155, Rev. B, TVO I Reaktortank – Kjol ämnesritning. UDDCOMB
315. Design drawing 335156, TVO I Reaktortank – Kjolmat. UDDCOMB Sweden AB,
327. Design drawing 235168, TVO I - Reaktortank - Avställningsstuts, Bearbetning före insvetsning, Teollisuuden Voima Oy (TVO), Finland.
333. Input information – Nozzle to safe-end, safe-end to pipe and orifice ring welds of main inset nozzles at OL 1 and 2. Teollisuuden Voima Oy, Finland, 2003.


341. Design drawing 1404341, TVO I - System 323 - Safe-end 1-4. Teollisuuden Voima Oy (TVO), Finland.


426. Koski, K. Lujuustarkastelu säätösauvayhteen (system 221) hitsille W11. Tutkimuselostus VAL64-6002, Technical Research Centre of Finland (VTT), Espoo, Finland, 1996. (in Finnish)


458. Cronvall, O. OL1 and OL2 - Reactor Pressure Vessel wall and main nozzles: Transient load frequencies and typical order of occurrence for 1, 3, 5 and 10 year time spans. VTT Report VTT-R-07746-07, Technical Research Centre of Finland (VTT), Espoo, Finland, October 2007. 188 p.


465. Smeekes, P. Conditional core damage probability (CCDP) data for OL1/OL2 RPV and internals. Excel file RPV-RI-ISI.xls, Teollisuuden Voima Oy (TVO), Olkiluoto, Finland, 4.1.2017. (Confidential)


Appendix A: Detailed Literature survey on degradation mechanisms affecting nuclear power plant structures and components

The commonly used expression “degradation mechanism” is also used in this work to denote the different kinds of degradation phenomena which can affect NPP structures and components. They can act both alone and in combination. However, most often one degradation mechanism either governs over the others, or is acting alone. The degradation mechanisms are also called ageing mechanisms. Both of these expressions are used in the following as they are interchangeable. The focus of this study is on degradation mechanisms which can affect the BWR RPV and its internals. These components are metallic. The base material of the BWR RPVs is ferritic steel, whereas that of internals is typically austenitic stainless steel or nickel-base alloy [1]. To obtain a more complete survey on the issue the degradation mechanisms affecting other metallic components are described briefly too. Besides the BWR RPV and its internals, there are lots of other metallic components in BWR NPPs with potential degradation mechanisms, such as piping systems.

Ageing mechanisms are specific processes that gradually change characteristics of a component with time and use [1]. Ageing degradation means those cumulative changes that can impair the ability of a component to function within acceptance criteria. Service conditions outside the prescribed limits can accelerate the rate of degradation.

The technical definition of ageing given by IAEA is the following [2]: Ageing is the continuous time dependent degradation of materials due to normal service conditions, which include normal operation and transient conditions.

A.1 Overview of degradation mechanisms

The degradation modes are presented first. The relevant degradation mechanisms are presented in Table A.1-1. During recent decades a large number of documents have been published on degradation of NPP components. The earlier documents are to varying extent outdated due to a smaller number of experienced operational reactor years. Here, the descriptions of degradation mechanisms mainly follow the most recent and/or relevant reports on the issue by IAEA [1,2], EPRI [3,4,5], U.S. NRC [6,7,8,128] and Swedish SSM [9].
### Table A.1-1. Relevant degradation mechanisms concerning BWR RPV and internals [1-8].

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Degradation sub-mechanism</th>
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<tr>
<td>Irradiation embrittlement (IE)</td>
<td></td>
</tr>
<tr>
<td>Thermal embrittlement (TE)</td>
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</tr>
<tr>
<td>Fatigue (FA)</td>
<td>● low-cycle fatigue (LCF)</td>
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<tr>
<td></td>
<td>● high-cycle fatigue (HCF)</td>
</tr>
<tr>
<td></td>
<td>● environmentally assisted fatigue (EAF)</td>
</tr>
<tr>
<td>Stress corrosion cracking (SCC)</td>
<td>● intergranular SCC (IGSCC)</td>
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<tr>
<td></td>
<td>● transgranular SCC (TGSCC)</td>
</tr>
<tr>
<td></td>
<td>● irradiation assisted SCC (IASCC)</td>
</tr>
<tr>
<td></td>
<td>● primary water SCC (PWSCC)</td>
</tr>
<tr>
<td>General corrosion (GC)</td>
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</tr>
<tr>
<td>Local corrosion</td>
<td>● pitting corrosion (PC)</td>
</tr>
<tr>
<td></td>
<td>● crevice corrosion (CC)</td>
</tr>
<tr>
<td>Erosion-corrosion, flow accelerated corrosion (FAC)</td>
<td></td>
</tr>
<tr>
<td>Creep (CR)</td>
<td></td>
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<tr>
<td>Mechanical wear (MW)</td>
<td></td>
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</tbody>
</table>

Depending on the metal or alloy, the intensity of the external loading and environmental impact, there may be a high potential for degradation. The processes of degradation can result in material and geometry changes, of which the most important are [2]:
- reduced toughness (embrittlement),
- cracking,
- swelling,
- thinning,
- denting, and
- pitting.

EPRI has provided for BWR RPV and internals a very thorough and detailed investigation in analysis report series produced by BWRVIP and by MRP. However, most of these reports are either available for relatively high prices or proprietary, i.e. available only for EPRI members. On the other hand, the freely available EPRI Materials Degradation Matrix, Revision 3 [3] contains detailed summaries of these degradation issues and uses most of the BWRVIP and MRP reports as references.

Some degradation mechanisms concerning the BWR RPV and internals have been left out from Table A.1-1. This is because they are either very rare, concern only certain safety insignificant components, do not concern BWR RPV and internals, and/or have emerged only in the earliest phase of plant operation. In the last case they have then been removed altogether with improved design, maintenance and fabrication practices. This information is confirmed by ref. [3]. For any significant creep the operational temperatures of BWR RPVs and internals are too low. The bolts in some joints do experience some stress relaxation, but as they are easily replaceable considering them is excluded here.

For the BWR RPV and internals, these minor and/or insignificant degradation mechanisms are:
- Corrosion:
  - wastage,
  - fouling,
  - galvanic corrosion,
  - microbiologically induced corrosion (MIC),
• SCC:
  o outside diameter SCC (ODSCC),
  o low temperature creep cracking (LTCC),
  o delayed hydride cracking (DHC),
• Irradiation-creep,
• Stress relaxation,
• Void swelling.

A.2 Descriptions of relevant degradation mechanisms

Detailed descriptions of degradation mechanisms, concerning metallic NPP components, are presented in addition to already mentioned refs. [1,2,3,4,5,6,7,8] also in handbooks [10,11,12,13,14,16,17]. In addition to loading and environmental conditions, degradation of NPP components depends very much on the material. The BWR RPVs are of low alloy steel, whereas their internals are mainly of austenitic stainless steel. As for piping systems, they are mostly made of austenitic stainless steel, and to a lesser extent of ferritic steel [1,3,8].

Of the covered literature on degradation mechanisms of metallic NPP components, the EPRI Materials Degradation Matrix, Revision 3 [3] is the most detailed and widest in scope as well as the most recent one. Thus, it together with its references is the most relevant background source here. However, it is not exhaustive, so it is supplemented by the other mentioned relevant documents. Which is a necessity in any case, in order to produce a thorough review of the issues in question.

A.2.1 Irradiation embrittlement

Neutrons from the nuclear fuel produce energetic primary recoil atoms in metals, which displace large numbers of atoms from their crystal lattice positions by a chain of atomic collisions [17]. This number of neutrons bombarding a given location is traditionally measured by the fluence (number of neutrons per square meter, n/m², for energy \(E > 1.0\) MeV) or displacements per atom (dpa). The correlation data from different sources with diverse neutron energy spectra is best accomplished with dpa, which accounts for both fluence and neutron energy levels [4]. The fluence or dpa provide part of the information needed to assess irradiation embrittlement.

Irradiation embrittlement of metals is caused by the lattice defects induced by neutron bombardment. High energy neutrons cause point defects. Part of these defects form various irradiation induced microstructural features consisting of dislocations, precipitates and cavities. Then, the cavities can be associated with other microstructural details, such as precipitates, dislocations and grain boundaries. These defects and precipitates from irradiation are obstacles to dislocation movement and result in an increase of yield and tensile strength as well as decreased work hardening capacity and ductility, and loss of fracture toughness [3,17].

Illustratively, Figure A.2.1-1 shows for ferritic RPV steel and welds the effect of irradiation on the tensile fracture stress and the corresponding effect on the Charpy test impact toughness. These are shown as a shift of the ductile-to-brittle transition to higher temperatures and the reduction of the upper shelf energy. The significant role of copper content is shown for two welds, which are similar, except for the copper content.
The actual mechanism of irradiation embrittlement is not completely understood. In low alloyed RPV steel, irradiation embrittlement is a function of both environmental and metallurgical variables [17]. Fluence or dpa, and copper and nickel content have been identified as the primary contributors in U.S. NRC Regulatory Guide 1.99, Revision 2 [18]. Other important variables include flux, temperature as well as phosphorus and manganese content. There is evidence that other variables, such as heat treatment, may also influence embrittlement. Therefore, mathematically based statistical data correlations are subject to uncertainty. Wrought austenitic stainless steels do not exhibit the sharp ductile to brittle transition behaviour characteristic of low alloy and carbon steels. Rather, toughness losses due to irradiation tend to accumulate with increasing fluence and saturate at a certain level [1].

![Diagram](image)

Figure A.2.1-1. Schematic diagrams depicting (a) how irradiation induced strength increase results in an upward shift in the Charpy impact toughness transition temperature, and (b) showing the significant role of copper content towards increasing radiation sensitivity; from ref. [70].

In the first class of features, one can further distinguish between Cu rich precipitates (CRP) and Mn-Ni rich precipitates (MNP). The formation of the latter, which might also not contain Cu, is favoured by lower temperature and high Ni (as well as Mn and Si) content. MNP without Cu
are detected only at sufficiently high neutron fluence, not only in (low Cu) RPV steels, but also in Fe-Mn-Ni model alloys [19]. However, there are more recent research results that do not fully agree with this. These results by Lindgren [467] implicate instead a rather continuous (but retarding) precipitation and growth for the fluences analysed. This means that MNP without Cu are detected also at low neutron fluence. According to ref. [19] it is assessed that precipitates rich in Mn and Ni, once nucleated, will rapidly grow to large volume fractions. For these reasons, they are more commonly denoted as late blooming phases (LBP). Again the more recent research results by Lindgren [467] do not agree with this, as instead of the late blooming phases only, features/clusters were observed from low to high fluences.

In RPV steels, the primary mechanism of embrittlement is the obstruction of dislocation motion produced by nano-metric defect structures that develop in the bulk of the material due to irradiation. Two classes of nano-structural features are considered as the main contributors to the embrittlement of RPV steels: (a) clusters of solute atoms such as Cu, Ni, and Mn, generally catalogued as precipitates, and (b) the so-called matrix damage, generally interpreted in terms of clusters of point defects [19].

Other matrix defects include vacancies and one interstitials. Together one vacancy and one interstitial create a so called A Frenkel pair [467]. Most of the Frenkel pairs created during neutron irradiation are recombined within a few picoseconds. The vacancies and interstitials that do not recombine might interact with other features in the vicinity and affect the microstructure by increasing the diffusion, which without irradiation is so low that it hardly occurs during the operational life of a RPV.

The LBP are presently a much researched issue and there is only a limited amount of measured high fluence effect data available. Views on effect of LBP to RPV steels differ to some extent, ranging from small to significant. However, LBP mainly concerns PWR plants with considerably higher fluences than experienced in BWR plants. According to Malerba [19], no sudden blooming is expected, but rather accumulation of decorated loops at a more or less steady rate with increasing dose, up to doses on the order of those corresponding to 60 or more years of RPV operation. The LBP starts to form already at low dose, progressing gradually, and are therefore only detected much later. Moreover, they are only barely inside the phase separation region, so that the volume fraction that forms will remain relatively small, even though a sudden, but limited, increase cannot be ruled out under low temperature conditions. On the other hand, according to Sprouster et al. [20] the MNP may grow at high fluence to relatively large volume fractions, up to approximately 3 %, since there is much more Mn, Ni and Si than Cu in typical low alloy RPV steels. However, the ductile-to-brittle transition fracture temperature approximately scales as \( (300 \, ^\circ \text{C}) \times \sqrt{f} \), where \( f \) is the volume fraction of the precipitates. Thus, the relatively large volume fractions associated with MNP could limit safe extended RPV lifetimes.

To provide an example on accumulated irradiation at the end of design lifetime, Table A.2.1-1 shows assessed fluences for some BWR RPV internals after 40 years in operation. As for rate of irradiation, the typical value for the peak flux at the BWR RPV inner surface is between 5.0E+08 and 5.0E+09 n/(cm²s). The typical fluence (of \( E > 1 \, \text{MeV} \)) for a BWR RPV after 40 years in operation, corresponding here to 32 effective full power years (EFPYs), is between 5.0E+17 and 5.0E+18 n/cm² [71].
Table A.2.1-1. Computed fluences for some BWR RPV internals in U.S. NPPs after 40 years in operation [22].

<table>
<thead>
<tr>
<th>BWR unit</th>
<th>Component</th>
<th>Fluence n/cm²</th>
</tr>
</thead>
<tbody>
<tr>
<td>BWR/6</td>
<td>core shroud</td>
<td>4.0E+20</td>
</tr>
<tr>
<td>BWR/6</td>
<td>top guide</td>
<td>4.0E+20</td>
</tr>
<tr>
<td>BWR/6</td>
<td>core plate</td>
<td>3.0E+20</td>
</tr>
<tr>
<td>BWR/6</td>
<td>jet pumps</td>
<td>1.0E+19</td>
</tr>
<tr>
<td>BWR/6</td>
<td>fuel support</td>
<td>5.0E+21</td>
</tr>
<tr>
<td>Dresden 2</td>
<td>outside core shroud</td>
<td>8.0E+19</td>
</tr>
<tr>
<td>Humboldt Bay</td>
<td>outside core shroud</td>
<td>2.0E+20</td>
</tr>
</tbody>
</table>

Irradiation embrittlement does not directly cause cracking. However, the margin of a material to resist propagation of cracks due to other causes, such as fabrication induced residual stresses, fatigue or SCC, is reduced. The significance of embrittlement for a given component depends on the probability of cracking and the loading of the component [1,3].

A.2.2 Thermal embrittlement

Thermal embrittlement of metals is a time and temperature dependent degradation mechanism. It is caused by the thermally activated movement of lattice atoms over a relatively long time period, a process which can occur without external mechanical load. Resulting changes in microstructure and material properties include embrittlement, as indicated by the decrease in ductility and toughness, as well as an increase in strength properties and hardness [1]. The mechanisms of such embrittlement are varied. They range from the precipitation of secondary phases to the segregation of metalloid impurities to grain boundaries, to the pinning of dislocations by interstitial impurities to, finally, the formation of brittle long range ordered phases [3].

The significant parameters responsible for the thermal embrittlement are [1,2]:
- temperature,
- material state (microstructure),
- time.

Thermal embrittlement of susceptible steels, such as cast duplex stainless steels, can result in brittle fracture associated with either cleavage of the ferrite or separation of the ferrite/austenite phase boundary. The critical stress level for brittle fracture is attained only at higher temperatures. The degree of thermal embrittlement is controlled by the amount of brittle fracture. CASSs with poor impact strength exhibit > 80 % brittle fracture. Other NPP steels and alloys are less prone to thermal embrittlement. In some CASSs, a fraction of the material may fail in brittle fashion but the surrounding austenite provides ductility and toughness. Such steels have adequate impact strength even after long-term ageing. A predominantly brittle failure may occur when either the ferrite phase is continuous, e.g. in cast material with a large ferrite content, or the ferrite/austenite phase boundary provides an easy path for crack propagation, e.g. in high-carbon grades of cast steels that contain phase-boundary carbides. Consequently, the amount, size and distribution of ferrite in the duplex structure and phase-boundary carbides are important parameters that control the extent of thermal embrittlement. The extent of thermal embrittlement increases with increased ferrite content [21]. In duplex steels, thermal embrittlement has little or no effect on the austenite phase. The formation of chromium rich regions in the ferrite is the primary mechanism for thermal embrittlement [72]. Significant reduction in fracture toughness is likely when the ferrite volume fraction exceeds 10 % [23].
Figure A.2.2-1 shows the decrease of room temperature Charpy impact energy during thermal ageing at 400 °C for various heats of CASS.

The kinetics of thermal embrittlement of susceptible steels are controlled primarily by the kinetics of ferrite strengthening, i.e. the size and spacing of chromium fluctuations produced by spinodal decomposition of ferrite. The effect of that is the phase separation into a Cr rich and Cr denuded phase that may have very different mechanical properties and corrosion properties. Small changes in the constituent elements of the material can cause the kinetics of thermal embrittlement to vary significantly. Activation energies of thermal embrittlement can range from 65 to 230 kJ/mole (15 to 55 kcal/mole). The laboratory test results indicate that all materials reach a saturation impact energy, i.e. minimum value that would be achieved by the material after long-term ageing. Saturation impact energy decreases in general with an increase in ferrite content or the concentration of carbon and nitrogen in the steel. Both of these factors promote brittle fracture [21].

Thermal embrittlement does not directly cause cracking. However, the margin of a material to resist propagation of cracks due to other causes, such as fabrication induced residual stresses, fatigue or SCC, is reduced. The significance of embrittlement for a given component depends on the probability of cracking and the loading of the component [1].

**A.2.3 Fatigue**

Fatigue of metals is defined as the structural deterioration that occurs as a result of repeated stress/strain cycles caused by fluctuating mechanical load and/or temperature. After enough many repeated cyclic loads of sufficient magnitude, microstructural damage can accumulate, leading to macroscopic crack initiation at the locations experiencing the highest stress/strain fluctuation. Subsequent continued cyclic loading can lead to the growth of the initiated crack. Fatigue behaviour is related to a variety of parameters, most importantly stress/strain range, mean stress, cycling frequency, surface roughness and environmental conditions [1,3,10,11].
Suresh [10] defines a more detailed classification of the phases of fatigue degradation:

1. Sub-structural and microstructural changes which cause nucleation of permanent damage.
2. The initiation of microscopic cracks.
3. The growth and coalescence of microscopic cracks to form dominant cracks.
4. Stable growth of the dominant macroscopic crack.
5. Structural instability or complete failure due to continued crack growth.

The crack initiation period includes the growth of the initial micro-crack(s). Because the growth rate is still low the initiation period may cover a significant part of the fatigue life. This is illustrated by the generalized picture of crack growth curves by Schijve [11], see Figure A.2.3-1 below. It schematically shows the crack growth development as a function of the percentage of the fatigue life consumed (= $n/N$), with $n$ as the number of experienced load cycles and $N$ as the number of load cycles to fatigue failure. Complete failure corresponds to $n/N = 1 = 100\%$.

There are three curves in Figure A.2.3-1, all of them in agreement with crack initiation in the start of the fatigue life, however, with different values for initial crack length. The lower curve corresponds to micro-crack initiation at intact material surface. The middle curve represents crack initiation from an inclusion. The upper curve is associated with a crack starting from a material defect, such as those encountered in welds.

A metal component subjected to fluctuating stress will fail at stresses much lower than those required to cause fracture in a single application of load [16]. This fatigue limit is an important material property from an engineering point of view. It can be formally defined as a stress amplitude for which the fatigue life becomes infinite in view of the asymptotic character of the stress versus the number of load cycles ($S-N$) curve, see Figure A.2.3-2.

Fatigue at high amplitudes and fatigue lives up to approximately 1.0E+04 load cycles is commonly called low-cycle fatigue. If fatigue covers a large number of cycles, approximately 1.0E+05 load cycles or more, it is called high-cycle fatigue [11]. The boundary between low-cycle and high-cycle fatigue is not defined by a specific number of load cycles. The more relevant difference between the two conditions is that low-cycle fatigue is associated with macro-plastic deformation with every load cycle. High-cycle fatigue is more related to an elastic behaviour on a macro-scale of the material. Actually, high-cycle fatigue is the more common case in practice, whereas low-cycle fatigue is associated with specific structures and load spectra.
LWR internals are subjected to time independent and dependent mechanical and/or dynamic loads. The mechanical loads mainly concern temperature and pressure. A structure will vibrate as a response to dynamic loads. Vibrations can lead to crack initiation and growth. Vortex
shedding and other unsteady flow effects can generate oscillatory hydro-dynamic forces, while cyclic thermal loads are produced by contact with the turbulent mixing of flow streams at different temperatures [7]. According to IAEA [1], there are three sources of fatigue loading significant to the BWR RPV internals. These are system cycling, thermal cycling and FIV, as described in more detail in the following.

System cycling refers to the changes in the reactor system operating conditions, which cause variations in pressure and temperature. Examples of system cycling are start-up, shutdown, reactor scram and safety/relief valve (SRV) actuation. Some system cycling events affect only a part of the RPV. For example, loss of feedwater heaters is significant to the feedwater nozzle and sparger, but has no noticeable impact elsewhere in the RPV.

Thermal cycling occurs due to temperature fluctuations. Thermal transients during operation can cause local or global temperature gradients, which in turn cause cyclic thermal stresses over the component walls. Smooth or sharp thermal transients result in slow or rapid thermal cycling, both of which lead to fatigue usage. The causes for smooth transients are generally start-up and shutdown procedures, or load following operation modes. Connection and disconnection of systems, NPP emergency core cooling system (ECCS) water injection, and leaking of hot or cold water through untight valves may cause rapid thermal cycling. Both affect ageing of material in terms of low-cycle fatigue or high-cycle fatigue.

FIV is a high-cycle loading mode. Fatigue degradation caused by FIV has been observed in several RPV internals, e.g. jet pumps and steam dryers [1]. FIV occurs when the coolant flowing past a component sheds vortices, which create cyclic loads. These loads generally occur in a frequency range up to about 20 Hz, leading to the expectation that FIV cycles accumulate early in operation. It is also possible that some modes of FIV are associated with a particular operating mode, which occurs infrequently [24].

Environment can significantly influence fatigue crack initiation and growth. Environmentally assisted fatigue (EAF), often referred to as corrosion-fatigue, must be considered when dealing with components exposed to the BWR coolant water.

**A.2.4 Stress corrosion cracking (SCC)**

SCC corresponds to crack initiation and ensuing crack growth in susceptible steels or alloys under the influence of tensile stress and a corrosive environment. SCC is a complex phenomenon, driven by the synergistic interaction of mechanical, electro-chemical and metallurgical factors [1,12,13,14,15]. BWR RPV internals are potentially susceptible to the following two forms of SCC [1]:

- IGSCC,
- TGSCC and
- IASCC.

SCC can proceed through a material in either of the following two modes: intergranular (IGSCC), i.e. along the grain boundaries, or transgranular (TGSCC), i.e. through the grains. For illustration, see Figure A.2.4-1. Sometimes the modes are mixed or the mode switches from one mode to the other. IGSCC and TGSCC often occur in the same steel or alloy, depending on the environment, the microstructure or the stress/strain state.
SCC can occur in ductile metallic materials with little or no plastic strain accumulation associated with the process [7]. The three necessary conditions for the occurrence of IGSCC are illustrated in Figure A.2.4-2, and are described as follows [1]:

- susceptible material,
- tensile stress,
- corrosive environment.

If one or more of the above mentioned conditions is not fulfilled, IGSCC is very unlikely to occur.

The hot and oxygenated reactor cooling water creates a corrosive environment in the BWR RPV. The dissolved oxygen in the reactor cooling water increases the electro-chemical potential of austenitic stainless steels and makes them more susceptible to SCC. The presence of impurities, such as chlorides and sulphates, in the coolant may accelerate the crack initiation process [7].

The tensile stresses required to cause SCC are quite high, usually of the scale of the material yield strength. The stresses can be externally applied, but often residual stresses cause SCC [12]. For NPP components, welding process typically produces such locally confined high
residual stresses. In addition, hard machining and abusive grinding can produce surface residual stresses near or above the yield strength of the material [1]. Existing cracks under nominal stresses caused by normal operation can be prone to SCC too. This is because of the locally increased tensile stresses ahead of the sharp crack front as caused by the magnifying effect of the local crack geometry.

There has been two major material factors that have contributed to IGSCC of austenitic stainless steels and alloys in BWR components: thermal sensitization and cold work [28].

Sensitization can make a welded austenitic stainless steel component susceptible to SCC. When such a component is cooled down slowly through the temperature range from 820 to 480 °C, as in a welding process, chromium carbides precipitate at grain boundaries. The precipitation of chromium carbides leads to a decrease in the chromium content in regions adjacent to grain boundaries [7]. When the chromium content in common NPP material type 304 stainless steel drops below 12 % the steel becomes susceptible to corrosion at the grain boundaries [29]. The chromium depletion process is known as the sensitization process, and the weld heat affected zones (HAZs) are typical locations for sensitization [7].

IGSCC has also occurred in stainless steels that were clearly not in the sensitized condition. It has been shown that their susceptibility to IGSCC is due to cold work induced during fabrication. Hardness levels involved have been above 300 Hv (Vickers hardness). In many cases the initial cracking was found to be transgranular and then changed to intergranular mode. The initial transgranular cracking is often associated with a surface layer of cold work induced by grinding or other severe surface machining techniques. Failures have also occurred where the occurrence of IGSCC was attributed to the presence of cold worked material, e.g. cold bent piping. The mechanism by which cold work renders austenitic alloys susceptible to IGSCC is not fully understood and is still being investigated. It is possible that there is an unfavourable interaction between deformation induced martensite, high residual stresses and localized deformation [28].

For SCC the crack tips can often be extremely sharp with widths of the order of 1-5 nm or, less commonly, relatively broad. However, the first criterion that must be met for an initial crack to continue to propagate is that there must be a mechanism to protect the crack faces so that an initially sharp crack does not become a blunt notch, which would probably arrest. This criterion is met in stainless steels and nickel-base alloys, which form protective oxide films in BWR conditions [3]. However, the degree of protection provided by oxides on carbon and low alloy steels in water can be less effective, especially as the temperature is lowered, and localized corrosion or pitting leading to crack blunting may occur [3]. Stainless steel castings and welds containing high levels of delta ferrite unlikely experience SCC. Most often SCC propagates perpendicular to governing tensile stress. Cracks also vary in degree of branching or formation of satellite cracks [1].

Three separate phases of SCC development can be identified [3]:
1. A precursor period during which specific metallurgical or environmental conditions are created at the metal/solution interface that are conducive to subsequent crack initiation. The precursor events associated with a change in metallurgical microstructure involving e.g. thermal ageing or irradiation embrittlement may take even years.
2. The initiation of cracks occurs when the local environment, microstructure and stress conditions have reached a critical combination. Crack initiation sites can include pits, intergranular corrosion, machining marks, crevice corrosion and stress raisers.
3. The growth rate of a dominant crack is very dependent on the material, stress and environmental conditions. The growth rate is rarely constant and may accelerate or decelerate over time.

IGSCC usually appears like brittle material behaviour, since the crack propagates with little or no macroscopic plastic deformation. With the exception of the cracked region, the alloy affected by IGSCC usually appears quite intact. Many steels and alloys are susceptible to IGSCC in at least one environment. However, IGSCC does not occur in all environments, nor does an environment that induces IGSCC in one alloy necessarily induce IGSCC in another alloy [1].

Exposure to high levels of neutron fluence can also cause stainless steels to become susceptible to SCC. This is a special form of SCC known as IASCC, which is also characterized by intergranular crack initiation and propagation. However, there are subtle differences between IASCC and IGSCC. Austenitic stainless steels that undergo IASCC need not be thermally sensitized or cold worked. IASCC is also highly dependent on neutron fluence exposure level [28]. Annealed and irradiated austenitic stainless steel becomes susceptible to IASCC when the threshold fluence level as a function of stress level is met or exceeded. Both stabilized and non-stabilized stainless steels appear to be equally susceptible to IASCC [1].

With the present more accurate analyses, many of the early SCC hypotheses have been shown to be untenable, and the candidate phenomena for environmentally assisted crack propagation for alloys in LWR plants have narrowed down to [3]:

- slip-oxidation,
- film-induced cleavage,
- internal oxidation, and
- hydrogen embrittlement.

**A.2.5 General corrosion**

General corrosion is characterized by uniform surface loss through material oxidation. In aqueous solutions including reactor coolants, it is primarily an electro-chemical process involving an anodic oxidation reaction to form positively charged metal cations, which is balanced electrically by a cathodic reaction involving the reduction of oxygen in solution (if present) and/or reduction of hydrogen ions to molecular hydrogen gas. The point of balance between the anodic and cathodic currents determines the characteristic corrosion potential, ECP, which is measurable [1,3]. For illustration of general corrosion, see Figure A.2.5-1.

General corrosion of all metallic materials in reactor coolants typically proceeds at a few microns per year at most [3]. However, BWR internals are made from austenitic steel or nickel-base alloy with very low corrosion rates in the BWR environment [1]. Although general corrosion rates are low for both carbon and low-alloy steels under controlled chemistry conditions the actual amount of corrosion product formed (in terms of weight) can be very large given the area of exposed steel, and this can give rise to crud build-ups affecting local corrosion and/or heat transfer as well as corrosion product activation and consequent radioactive contamination problems out of core [3]. Moreover, the stress induced on an adjacent component by the volumetric increase of the oxide can also cause other degradation mechanisms, such as SCC [4].
A.2.6 Local corrosion

The modes of local corrosion, being crevice corrosion and pitting corrosion, are described in the following. These corrosion modes cannot occur in reactor coolant environments without significant perturbation of the intended water chemistry. For this reason, localized corrosion modes in water cooled reactors mainly concern tertiary water systems as well as systems containing stagnant water.

Crevice corrosion is caused by corrosion cells which are nucleated by differences in concentration in the corrosive environment. The corrosion manifested in the crevice can be uniform, shallow pitting or selective [2].

Crevice corrosion is associated with occluded volumes inside which a quasi-stagnant solution is present with a mechanism making the solution more aggressive, e.g. increased impurity concentration with increased acidity or alkalinity. Crevices may be inherent to the component design, such as at gasket joints, lap joints, bolt heads and threads, or they may be due to corrosion deposits and sludge piles. The critical factors controlling this form of attack are the geometry of the crevice and the electro-chemical or thermal-hydraulic mechanisms that change the cation and anion concentrations within the crevice [3]. An illustration of crevice corrosion is shown in Figure A.2.6-1.

In BWR water, which contains dissolved oxygen and hydrogen peroxide, electro-chemically driven concentration of anions in crevices is possible in principle. Examples of such environmental chemistry changes in crevices in BWR components are associated e.g. with safe-ends, access hole covers and shroud head bolts [33,34].
The mechanism of pitting corrosion can be divided into three consecutive steps [35,36]:
- initiation,
- metastable propagation, and
- stable propagation.

The initiation step is a local breakdown of the passivating oxide layer by aggressive ions in the environment. The corrosion process can then continue in the unprotected metal revealed by the initiation step. The corrosion rate is increased because even more aggressive environment is produced by the corrosion reaction itself. However, at the earlier stages of the pit propagation, when pits are still very small, they can re-passivate spontaneously. This stage is often referred to as metastable pit growth. The stage of stable propagation is reached when spontaneous re-passivation is no longer possible, whence pitting corrosion will take place. An illustration of pitting corrosion is shown in Figure A.2.6-2.

![Illustration of pitting corrosion](image)

*Figure A.2.6-2. Illustration of pitting corrosion. For better clarity, the metal oxide cap that forms on the top of the pit and further inhibits the diffusion of oxygen into the pit is excluded here.*

Pitting corrosion has very similar attributes to electro-chemical crevice corrosion, as it also depends in part on the creation of a localized environment within the pit. However, an important difference is that the features that lead to pit initiation are inherent in the microscopic material and non-metallic inclusion properties, as compared to the macroscopic dimensions associated with crevice corrosion. Pitting has received much attention and research, see for example refs. [13,37,38,39,40]. This is due to observed through-wall leakages in tube components and due to uncertainty in predicting the time before it is observed, as caused by the stochastic nature of the initiation process [3].

### A.2.7 Erosion-corrosion, flow accelerated corrosion

The movement of solutions above a threshold velocity can degrade metals by the interaction of fluid induced mechanical wear or abrasion with corrosion. The general expression erosion-corrosion (E/C) includes all forms of accelerated attack in which protective surface films and/or the metal surface itself are removed by the combination of solution velocity and corrosion, such as impingement attack, cavitation damage and fretting corrosion [1].

Flow accelerated corrosion (FAC) corresponds to erosion (or thinning) of metallic component wall without threshold solution velocity. FAC is a complex phenomenon that depends on many parameters of water chemistry, material composition and hydro-dynamics. FAC involves the electro-chemical aspects of general corrosion plus the effects of mass transfer and momentum transfer [1]. FAC can occur in water or steam under single or two-phase conditions, especially at areas of high turbulence that are often associated with geometrical discontinuities or abrupt changes in flow direction [3]. An illustration of FAC is shown in Figure A.2.7-1.
FAC and E/C are characterized by the constant removal of protective oxide films from the metal surface, ranging from thin invisible passive films to thick visible films of corrosion products. Typical general corrosion kinetics involves the formation of a protective oxide that slowly thickens with time [41]. The thickening of the protective film makes subsequent corrosion reactions at the solution/oxide or oxide/metal interface more difficult since the reactants have to pass through the film with ever increasing thickness. The corrosion rate kinetics in this condition is typically parabolic. In the case of FAC or E/C, only a limited thin protective film is established due to constant flow induced mass transport removal/dissolution of the oxide. This results in linear corrosion kinetics where the change in thickness is proportional to time. Local attack occurs in the region where the film has been removed. This corrosion can be further accelerated if the solution contains solid particles (e.g. insoluble salts) that have an abrasive action [1].

![Figure A.2.7-1. Illustration of FAC showing potential sites for the two different types of wet steam FAC damage in a NPP piping Tee, from ref. [8].](image)

**A.2.8 Creep**

Creep is the deformation that occurs over a period of time in a material subjected to constant stress, even below the elastic limit. For metallic materials, the creep reaches a significant level when the temperature exceeds 40 % of the absolute melting point [2,16]. Due to thermal activation, materials can slowly and continuously deform even under constant stress, and eventually fail.

The propagation of creep is determined by several competing reactions, including [16]:
- strain hardening,
- softening processes; recovery, re-crystallisation, strain softening and precipitation over ageing,
- damage processes; cavitation and cracking.

Of the above mentioned reactions, strain hardening tends to decrease the creep rate, whereas the other reactions tend to increase it [16]. Basically, two processes occur in the microstructure that affect the material properties [2]:
- plastic deformation processes,
- changes in the microstructure.
Both of the above mentioned processes are time dependent and they show certain interactions. The changes in the microstructure can be caused by high temperatures alone, since they are the consequence of thermally activated mechanisms [2].

Deformation due to creep is usually divided into three regimes: primary, secondary and tertiary, see Figure A.2.8-1. These regimes are controlled by different mechanisms. Components are usually designed to be operated only in the primary and secondary regimes [2,16].

The time dependent creep damage processes occur according to the following steps [2,16]:
- Creep damage in the primary and secondary regimes, in which the occurred damage is not irreversible.
- Nucleation of creep pores near the end of the second regime.
- Coagulation of the creep pores to form micro-cracks.
- Crack growth due to creep. In this regime spontaneous failure can occur when large regions of the material have already suffered from this process.
- Creep rupture. With increasing exposure time and accumulation of microstructural damage the ability for creep deformation decreases and, thus, ruptures occur in the region having the lowest ductility.

Due to operational temperature range in NPPs being in general below the creep range of the metallic components creep seldom occurs at BWR plants, if at all.

An aggressive environment can have an influence on the creep behaviour and on the degree of damage resulting by it. In metallic materials, an internal oxidation process can occur and accelerate the creep process [2]. The initial creep strength depends on the initial strength of the material at the considered temperature. If the thermally induced process (thermal ageing) leads to a decrease in strength, then more unfavourable conditions have to be expected concerning the long range creep behaviour [2,16].

![Figure A.2.8-1. Schematic illustration of creep damage curves showing the three different creep regimes: primary, secondary and tertiary, from ref. [16]. Here \( \varepsilon_r \) is rupture strain and \( t_r \) is rupture time, respectively.](image)
A.2.9 Mechanical wear

Wear of metals is broadly characterized as mechanically induced or aided degradation mechanism due to contact of two materials. Degradation from small amplitude oscillatory motion between continuously rubbing surfaces is generally termed fretting. Vibration of relatively large amplitude, resulting in intermittent sliding contact between two parts, is termed sliding wear, or wear. Generally, wear results from concurrent effects of vibration and corrosion [1]. Wear is the loss of material, generally measured as the rate of removal of surface material, caused by the relative motion between adjacent metal surfaces or by the action of hard abrasive particles in contact with a metal surface [3]. In BWR NPPs also sliding friction can occur between moving parts.

The process of particle removal by wear is caused by different mechanisms, which depend on the characteristics of the interacting materials, the type of relative motion and the chemical environment. The wear mechanisms are [2]:

- adhesion (micro-welding of asperities with subsequent decohesion due to continued motion),
- surface fatigue by repeated transmission of normal or shear forces,
- abrasion, erosion (micro-grooving by hard particles or hard asperities of a counter body).

As a consequence of the removal of wear particles, fresh surfaces are being created, mostly by plastic deformation in the micro-range. In chemically active environments, chemical reactions with these highly excited surface areas can occur. This leads either to the formation of surface layers which slow down the wear process or to chemical reactions through which the process of material removal is accelerated together with a simultaneous corrosion process [2].

Under certain circumstances, hard oxide layers and oxidic particles can be produced. They can inhibit the adhesion wear mechanism but considerably enhance the abrasive component, especially when the particles cannot be removed from the contact area. Since oxidization produces an increase in material volume, the clearance of fitted parts can be reduced and make further motion impossible. This can lead to the over-stressing of components and subsequent mechanical failure [2].

The major cause for fretting and wear is FIV. Initiation, stability and growth characteristics of damage by this mechanism may be a function of a large number of variables, including local geometry, stiffness of the component, gap size between the components, flow velocities and directions, as well as oxide layer characteristics [1].

A.2.10 Interaction of degradation mechanisms

According to experience, when some degradation mechanisms act simultaneously, they can create a joint effect, which in some cases is even more severe than the arithmetically added result of their separate effects. In these cases, the resulting degradation phenomenon is often quite complex. Due to this, and realising that the interaction of various degradation mechanisms is still a more recent research topic, the physical mechanisms of most interaction phenomena are not clear. Descriptions of the better known interacting degradation mechanisms are presented in the following. The interaction of erosion and corrosion is excluded from the following because it is commonly treated as a mode of degradation, and it has been described in Section A.2.7.
Another issue is the consecutive effect of degradation mechanisms. This mode of ageing can still include to smaller extent the interaction of degradation mechanisms. Examples of this ageing mode in reactor coolant water environments include:

- initiation of a crack by SCC with further growth of it by fatigue,
- initiation of a crack by fatigue with further growth of it by SCC.

**Irradiation-creep**

The general mechanism of irradiation-creep is well understood [30]. It is an athermal process that depends on the neutron fluence and stresses, and can also be affected by void swelling, should it occur. The mechanism manifests itself in two ways. In the first case, it operates to mitigate potential increases in stresses caused by void swelling. In the second case, it can occur in the absence of void swelling and can in addition cause stress relaxation of preloaded components, possibly leading to excessive wear or fatigue, or affect components with sustained pressure. A large amount of fast and thermal test reactor stainless steel material test data exists for irradiation-creep. The MRP database consists of data for a variety of austenitic stainless steel alloys, including solution annealed type 304 and cold worked type 316. The current database indicates that a greater irradiation-creep rate occurs for the former materials than for the latter. The recently obtained data are consistent with other literature data [3].

**Corrosion-fatigue (environmentally assisted fatigue)**

In case of fatigue in corrosive environments, the number of load cycles needed to crack initiation is considerably smaller than that needed in an inert environment. In addition to crack initiation, the corrosive environment also influences the ensuing cyclic crack growth rate. The interaction of mechanical alternating stresses and corrosion attack usually leads to transgranular cracking with a low associated deformation. Since the corrosion mechanism is mainly time dependent and the fatigue mechanism is controlled by the number of load cycles, a complex interaction between these mechanisms occurs. No threshold conditions exist for corrosion-fatigue with respect to the corrosion system and stress amplitude [2].

In corrosion-fatigue the crack growth rate can be influenced by the following features and conditions [2]:

- characteristics of the corrosive environment, such as pH value, temperature, electrochemical potential or oxygen content in water,
- loading frequency and wave form,
- mean load and load amplitude, and
- sulphur content of steel.

Several corrosion-fatigue mechanisms have been proposed to explain the enhanced crack growth rates with varying degrees of success. The generalised corrosion-fatigue cracking mechanism involves the single or mutual occurrence of hydrogen induced cracking and/or anodic dissolution at the crack tip. According to current understanding, there appears to be at least four possible mechanisms of anodic dissolution [31]:

- slip dissolution,
- brittle film rupture,
- corrosion tunnelling, and
- selective dissolution (dealloying).
Nowadays, corrosion-fatigue is most often associated with EAF, which is described earlier in Section A.2.3. EAF concerns interaction of fatigue with the reactor coolant water and its chemistry.

**Creep-fatigue**

Under power plant components operating at relatively high temperatures, changes in prevailing conditions during operation cause transient temperature gradients. If these transients are repeated, the differential thermal expansion during each transient results in thermally induced cyclic stresses. The extent of the resulting fatigue damage depends on the nature and frequency of the transient, on thermal gradient in the component and on material properties. In case of such components, the damage caused by both fatigue and creep needs to be taken into account [16].

There is ample evidence to show that rupture ductility has a major influence on creep-fatigue interaction. In case of ferritic steels and austenitic stainless steels, the lower the ductility, the lower is the creep fatigue endurance. In addition, long hold times, small strain ranges and low ductility favour creep dominated failures, whereas short hold times, intermediate stress ranges and high ductility favour creep-fatigue interaction failures [16]. As an example of test results, the effect of tensile hold time on fatigue endurance of type 316 stainless steel is shown in Figure A.2.9-1.

Due to operational temperature range in NPPs being in general below the creep range of the metallic components, creep-fatigue seldom occurs at BWR plants, if at all.

![Figure A.2.9-1. Test results showing the effect of tensile hold time on fatigue endurance of type 316 stainless steel, from ref. [32].](image-url)
A.3 Observed degradation

To date the cracking degradation of BWR RPV and its internals has been limited to SCC and fatigue in their various forms [1]. Although degradation due to other mechanisms during plant life cannot be discounted, such occurrences would be expected to occur earlier in the plant life. The field experience and the understanding of relevant degradation mechanisms support this conclusion.

Incidents documented to date indicate that the cracking frequency of reactor internals is increasing, due in part to expanded scope of inspections. Cracking indications are typically found in weld HAZ material adjacent to circumferential welds, although longitudinally orientated cracks have also been observed. Cracking is typically caused by IGSCC and IASCC, and has been observed in all three stainless steel types of BWR core shrouds in worldwide use, i.e. Type 304, 304L, 316L and 347 stainless steels. To date, in three plants with Type 304L stainless steel there has been cracks in the mid-to-upper weldments, and in two plants with Type 347 stainless steel there has been cracks in both the upper and lower weldments [1].

The observed cracking degradation history of BWR RPVs and their internals is described component specifically in more detail in the following, with representative examples. As mentioned earlier, operating BWR RPVs and internals have been fabricated by several suppliers. Figure 2-1 in Section 2 shows an Asea-Atom BWR RPV and some of its main internals. To show more BWR RPV internals, Figure A.3-1 below presents U.S. generations BWR/3 and BWR/4 BWR RPV and main internals.

Core plate

The first instance of core plate cracking in a BWR has been reported in November 1994 when IGSCC was observed in the core plate rings and top guide of the Würgassen BWR. Metallurgical investigation showed that the Würgassen core plate material was heavily sensitized due to use of a material heat with high carbon and low stabilization, with sensitization due to stress relief heat treatment after fabrication [44]. The inspections were performed by visual methods. IGSCC in the core plate was observed in the rim near the welds of the rim to the plate after 19 years of operation.

In April 1996, cracking of a core plate sub-component was detected at Hatch 1 NPP unit after 15 years of operation. The affected location was the creviced locating pin to core plate attachment weld. It was postulated that the pin attachment weld failed due to IGSCC in the creviced weld HAZ [1].

Core spray internal piping

Cracking of internal core spray piping has been observed in several BWRs [45]. Cracking has been found in the thermal sleeve collar, in the creviced weld of RPV downcomer slip joint sleeve, and in the downcomer piping elbow weld. Here, downcomer refers to the feed water flow downwards between the RPV wall and the core shroud.
Core spray sparger

There have been several reports of cracking in BWR core spray spargers [1,7]. Cracking has been discovered by visual inspections conducted during refueling outages. Typically, the cracking has occurred at the tee-box to sparger pipe welds. In addition, there have been some cases where circumferential cracks have been observed around the sparger piping away from the tee-box. Some of these SCC incidents were attributed to cold work caused by cold bending operations [1].

![Diagram of Reactor Vessel and Main Internals]

*Figure A.3-1. U.S. generations BWR/3 and BWR/4 BWR RPV and main internals, from ref. [71].*

Control rod guide tube and CRD housing

In two older U.S. BWRs leakage was detected in the gap between the RPV wall and the control rod drive (CRD) housing. This was diagnosed to be caused by development of through-wall
SCC in the stub tube in the vicinity of the J-weld that joins the CRD housing to the top of the stub tube. The cracks were intergranular [7].

**In-core housing**

IGSCC has been reported in in-core housings fabricated of Type 304 stainless steel at BWR/4 and BWR/5 plants in Japan [1]. In the first failure, through-wall cracking was detected below the attachment weld. It was determined to have been caused by severe sensitization during welding of the housing to the RPV at the time of initial installation.

**Jet pump**

Jet pumps in most early BWR-3 units were affected by FIV problems, which led to the development of fatigue cracks in the pump support system [7].

SCC was detected in jet pump hold-down beams in six US BWRs in the early 1980’s. The beams are made of nickel alloy Inconel X-750. The cracks were located across the ligament of the beam section at the mean diameter of the bolt thread area. The detected cracks were intergranular [7].

Several IGSCC incidents occurred in the replaceable Alloy X-750 hold-down beams from 1979 to 1982. The detected cracking initiated from the bolt hole region in the center portion of the beam [48].

IGSCC was detected in jet pump instrumentation penetration welds in 1984 in a U.S. NPP [71]. The base material was stainless steel.

IGSCC was detected in jet pump safe-ends in 1985 in a U.S. NPP [71]. The base material was stainless steel.

In September 1993, Grand Gulf Nuclear Power Station Unit 1 (GG-1) experienced an in-service failure of a jet pump beam due to IGSCC in the “ear” location at the end of the beam [1].

In November 1996, after about 25 years of service, cracks were discovered in two of the ten jet pump riser assembly elbows in a BWR outside U.S. [46].

In January 1997, three crack indications were found in two of the ten jet pump riser elbows at LaSalle Unit 2 [47]. More precisely, the cracks were located in the weld HAZ of the riser elbow to thermal sleeve attachment and appeared to be caused by IGSCC.

In January 2002 a jet pump beam failure occurred at Quad Cities 1 due to IGSCC in a beam region that had not previously experienced cracks and that was not normally evaluated during in-service inspection [1]. The cracked beam was an original component that had been in service for approximately 30 years. The failure location was about midway down the transition region between the thick center part of the beam and the thinner ends.
Core shroud

The first documented incident of cracking in a core shroud was reported in August 1990 at the Kernkraftwerk Mühleberg BWR [49]. The diagnosis of the root cause of the cracking was IASCC promoted by weld residual stresses and possible corrosion induced oxide wedging stresses [43].

In 1993, metallurgical samples removed from the upper and mid-core shroud weld HAZ of a BWR located in the U.S. was confirmed to be caused by IGSCC. The damage was diagnosed to be caused by neutron irradiation and surface cold work [1].

In Japan, SCC has been reported in several NPPs in core shrouds made of type 316L stainless steel, and cracks have been found both in the ring region and the mid-shell region. These cracks were initiated and propagated by cold work, such as hard machining and grinding. The locally high weld residual stresses enforced this. There is a potential for cracking of this kind in all BWR plants where the requisite combination of material, environment and fluence conditions exists [42,43].

In Oskarshamn 1 NPP unit in Sweden cracks initiated by thermal fatigue were detected in core shroud of 347 stainless steel. This was caused by a broken feed water sparger. The ensuing crack growth was driven by IGSCC [50].

Core shroud cracking has been reported also in BWRs in Germany, Switzerland, Spain and Taiwan [1].

Core shroud support

The core shroud support in a Swedish NPP showed cracks in the flanged connection to the core shroud [50]. These cracks were most likely caused by rapid thermal fluctuations.

Extensive IGSCC cracking has been discovered in several BWR access hole covers [1]. In 1999, about 300 cracks were found on the shroud support weld lines of one Japanese BWR/2 plant during the core shroud replacement [51]. Most cracks were found in the horizontal weld to the RPV bottom and perpendicular to the weld lines. These cracks initiated in Alloy 182 welds due to tensile residual weld stress.

Feedwater Sparger

In 1972, large circumferential fatigue induced cracks were detected in a feedwater sparger of a U.S. BWR-3 unit. Subsequent inspections revealed similar fatigue induced cracks in other U.S. BWR-3 and BWR-4 units. The cracks were located near the sparger water inlet in the vicinity of the feedwater nozzle. Failures were attributed to FIV and rapid thermal cycling [7].

Top guide

Approximately 1-1/2 inches long through-thickness grid beam crack was found in the Oyster Creek plant top guide in 1991. An electron microscopic examination of a sample removed from
the top guide was conducted under an EPRI program and showed intergranular cracks with no indications of pre-irradiation sensitization, suggesting that cracking was caused by IASCC. An inspection at the next refuelling outage of the plant revealed two more cracks similar to the first one. The cracks were located at the bottom of un-notched areas of the 304 stainless steel top guide grid beams [52].

The second observation of top guide cracking was reported in 1994 at Würzburg BWR. This was detected near the weld HAZ of a top guide ring assembly weld. The detection was confirmed by metallurgical evaluation to be IGSCC in a material that was thermally sensitized during stress relief after fabrication [42].

**On Nordic cracking degradation history of BWR RPVs and their internals**

The cracking history data in the following concerns Swedish and Finnish BWR units, and is mainly from SKI report 02:50 [53]. This report covers cracking occurrences from 1972 to 2000. All these BWR units are designed by Asea-Atom (which is nowadays Westinghouse) [60]. The cracking history data concerning OL1 and OL2 was taken from ref. [54]. As it is a confidential report, the description of its data here is more general. In addition to using existing documented data, the author of this work visited the Olkiluoto, Forsmark, Oskarshamn and Ringhals NPPs during 2015-2016, to discuss and learn about the possible more recent flaw findings in their BWR units. These discussions are also summarised here.

The considered Nordic BWR units are:

- Barsebäck 1,
- Barsebäck 2,
- Forsmark 1,
- Forsmark 2,
- Forsmark 3,
- Oskarshamn 2,
- Oskarshamn 3,
- Ringhals 1,
- Olkiluoto 1, and
- Olkiluoto 2.

Of these BWR units, Barsebäck 1 and 2 were decommissioned during the first decade of this millennium. However, before that, they had been in operation for about 30 years. As for experienced degradation mechanisms, the Swedish BWR RPVs and their internals have mostly been damaged by IGSCC and IASCC. Thus, the scope here concerns mainly those degradation mechanisms. Table 3.3-1 below shows for Swedish BWR RPV internals the IGSCC and IASCC occurrences detected by the end of 2000. Then, Table 3.3-2 summarizes the corresponding crack findings for the OL1 and OL2 units by the end of 2000. The information concerning more recently detected cracks in the Nordic BWRs is based on the discussions between the author of this work and the experts from the above mentioned four NPPs.

Note, that all crack findings presented in Tables A.3-1 and A.3-2 as well as those described by the experts from the above mentioned four NPPs have been fully repaired or the component replaced.
Table A.3-1. For Swedish BWR RPV internals, the IGSCC and IASCC occurrences detected by the end of 2000 [53].

<table>
<thead>
<tr>
<th>Component</th>
<th>No. of detected cracks</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core shroud lid bracket</td>
<td>45</td>
<td>Ni-based X-750</td>
</tr>
<tr>
<td>RPV head spring beams and brackets</td>
<td>19</td>
<td>Ni-based Alloy 182</td>
</tr>
<tr>
<td>Bolt, nut</td>
<td>14</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core shroud lid support</td>
<td>13</td>
<td>Ni-based X-750</td>
</tr>
<tr>
<td>Steam separator</td>
<td>5</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core grid support</td>
<td>3</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Core shroud</td>
<td>1</td>
<td>stabilized stainless steel</td>
</tr>
<tr>
<td>Feedwater sparger</td>
<td>1</td>
<td>stainless steel</td>
</tr>
</tbody>
</table>

Table A.3-2. For OL1/OL2 RPV and internals, general summary of the crack findings by the end of 2000 [54].

<table>
<thead>
<tr>
<th>Component</th>
<th>Cause of cracks</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core grid</td>
<td>IASCC</td>
<td>stainless steel</td>
</tr>
<tr>
<td>Spring beams</td>
<td>IGSCC</td>
<td>Bofors CRO684</td>
</tr>
<tr>
<td>Spring beam support brackets</td>
<td>IGSCC</td>
<td>nickel-base alloy</td>
</tr>
<tr>
<td>RPV flange</td>
<td>hot cracks</td>
<td>ferritic steel</td>
</tr>
<tr>
<td>Steam separator outlet</td>
<td>IGSCC</td>
<td>stainless steel</td>
</tr>
</tbody>
</table>

The first discussions on more recent cracking was with the OL1/OL2 experts. For that purpose the author of this work spent three days in the Olkiluoto NPP site in December 2015. In addition to these discussions, confidential documents on detected cracking were made available for the duration of the visit. The program of the visits to the three Swedish NPPs was similar.

According to TVO expert [55] and associated confidential documentation, cracks have been detected from the OL1/OL2 RPV and internals during 2000-2016 from:
- Core shroud lifting lug,
- New feedwater spargers,
- Feedwater nozzle to safe-end weld,
- Core spray nozzle to safe-end weld,
- Flange cooling nozzle/pipe weld,
- Steam dryer welds for steam guide plates, and
- Steam dryer outlet packages.

Next discussions on more recent cracking were carried out with the Forsmark NPP experts. For that purpose, the author of this work spent two days in the Forsmark NPP site in May 2016. According to the Forsmark expert [56] and associated confidential documentation, cracks have been detected from F1/F2/F3 RPV and internals during 2000-2016 from:
- Spring beam support brackets,
- Flange cooling spray piping,
- Long nozzle pipes in cooling spray piping,
- Steam dryer support beams,
- Inner roof plates of the steam dryer,
- Core shroud support leg,
- Control rods,
- Control rod blades, and
- Water level measurement nozzle/PRV weld.
For discussions on more recent cracking with the Oskarshamn NPP experts, the author of this work spent one day in the Oskarshamn NPP site in November 2016. According to the Oskarshamn expert [57] and associated confidential documentation, cracks have been detected from the RPVs and internals of the OKG1/OKG2/OKG3 NPP units during 2000-2016 from:

- Long nozzle pipes in cooling spray piping,
- Spring beam support brackets,
- Steam separator,
- Core shroud lid,
- Feedwater nozzle,
- J-groove welds of small nozzles,
- Control rods,
- Core shroud shell welds, and
- Core shroud support legs.

For discussions on more recent cracking with the Ringhals NPP experts, the author of this work spent one day in the Ringhals NPP site in December 2016. According to the Ringhals expert [58], associated confidential documentation and SKI Report 2004:16 [59], cracks have been detected from the RPV and internals of the RAB1 NPP unit during 2000-2016 from:

- Core shroud lid,
- Water level measurement nozzle,
- Core spray nozzle,
- Other nozzles manufactured of Alloy 600 and welded with Alloy 182.

Summary

Table A.3-3 presents a summary on the degradation experience concerning BWR RPV components important to safety. Note, that Asea-Atom BWR NPPs do not have jet pumps.

**Table A.3-3. Summary on degradation experience concerning BWR RPV internals important to safety [1].**

<table>
<thead>
<tr>
<th>Component</th>
<th>Degradation mechanism</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Core Plate</td>
<td>IGSCC</td>
</tr>
<tr>
<td>2. Core Spray Internal Piping</td>
<td>IGSCC</td>
</tr>
<tr>
<td>3. Core Spray Sparger</td>
<td>IGSCC</td>
</tr>
<tr>
<td>4. CRD Guide Tube</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>5. CRD Housing</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>6. In-Core Housing</td>
<td>IGSCC</td>
</tr>
<tr>
<td>7. Jet Pump</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Diffuser IGSCC</td>
</tr>
<tr>
<td></td>
<td>Hold-down beam IGSCC</td>
</tr>
<tr>
<td></td>
<td>Inlet mixer Fatigue due to improper installation</td>
</tr>
<tr>
<td></td>
<td>Riser IGSCC</td>
</tr>
<tr>
<td>8. LPCI Coupling</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>9. Orificed Fuel Support</td>
<td>No incidents of cracking reported</td>
</tr>
<tr>
<td>10. Core shroud</td>
<td>IGSCC/IASCC</td>
</tr>
<tr>
<td>11. Shroud Support</td>
<td>IGSCC</td>
</tr>
<tr>
<td>12. Top Guide</td>
<td>IGSCC/IASCC</td>
</tr>
</tbody>
</table>
Appendix B: Detailed survey on and modelling of degradation mechanisms affecting BWR RPV and its internals

The This section describes the significant degradation mechanisms that affect BWR RPV and its internals, and evaluates the potential significance of their effects on the continued performance of safety functions of the considered components throughout the plant service life.

For each significant degradation mechanism the following issues are described:
- computational modelling,
- susceptibility,
- mitigation,
- inspection and monitoring.

The survey on computational modelling, susceptibility and mitigation of degradation mechanisms mainly follows the most recent and/or relevant reports on the issue by IAEA [1,2], EPRI [3,4,5], U.S. NRC [6,7,8] and Swedish SSM [9]. In addition, also relevant journal articles, conference papers, academic theses, dissertations, handbooks and technical reports publicly available are covered.

National codes and standards, such as ASME Code from the U.S., provide computational models for irradiation embrittlement, SCC and fatigue. For other relevant degradation mechanisms, computational models are seldom presented in the national codes and standards, due to which they are taken from fitness-for-service handbooks and reliable technical reports, such as those by EPRI.

As for inspection and monitoring, the associated requirements are mainly given in national codes and standards. The most significant ones of these are:
- Section XI of the ASME Code [81] from U.S.,
- KTA 3204 [140] from Germany,
- RSE-M [64] from France,
- SKIFS [96] from Sweden,
- JSME S NA1-2008 [97] from Japan, and
- YVL Guide E.5 [98] from Finland.

The main techniques used for the inspection and monitoring of BWR RPV and its internals are [117]:
- ultrasonic examination methods,
- acoustic emission monitoring,
- visual inspection, and
- Eddy current inspection.

Degradation mechanisms are specific processes that gradually change characteristics of a component with time and use. Ageing degradation concerns those cumulative changes that can impair the ability of a component to function within acceptance criteria. Service conditions outside prescribed limits can accelerate the rate of degradation.
Evaluation of age related degradation mechanisms is here based on BWR service experience, laboratory data, and relevant experience from other industries, where applicable. The following degradation mechanisms, as according to ref. [1], will be reviewed and assessed for relevance to BWR RPV internals:

- irradiation embrittlement,
- thermal embrittlement,
- fatigue;
- system cycling,
- thermal cycling,
- stress corrosion cracking (SCC);
- IGSCC,
- IASCC,
- general corrosion,
- erosion-corrosion, flow accelerated corrosion (FAC), and
- mechanical wear.

The characteristics of irradiation embrittlement are described in more detail than those concerning other covered degradation mechanisms. This is because it relates most to the main argument of the dissertation, i.e. extension of operational lifetime of a BWR RPV to at least 80 years. Of the degradation mechanisms listed in Table A.1.1 in Appendix A.1, creep is not described further here. This is because under operational BWR temperatures its effect is practically negligible. However, it is possible that creep would have some effect later, during the LTO period. As there still is no evidence concerning this for BWR RPVs and internals, this issue is excluded here.

B.1 Irradiation embrittlement

Mechanistically, the lattice defects induced by neutron bombardment results in irradiation embrittlement. High-energy neutrons displace atoms from their normal lattice positions and create point defects. The significance of irradiation embrittlement for a given component depends on the probability of cracking and the loading of the component [17]. The main degrading effect of irradiation embrittlement is the decrease in material fracture toughness.

Another affecting embrittlement mechanism causing decrease in material fracture toughness is thermal embrittlement. When measuring experimentally the effect of irradiation embrittlement the effect of thermal embrittlement is included too, and it is difficult or impossible to separate these effects from each other. However, the effect of thermal embrittlement is generally much smaller than that of irradiation embrittlement.

B.1.1 Computational modelling of irradiation embrittlement

The majority of procedures developed within various surveillance programmes to assess irradiation embrittlement correlate the shift of the material specific reference nil-ductility temperature, $RT_{NDT}$, from ductile to brittle with different parameters. These include neutron fluence, irradiation temperature, chemical composition, neutron flux, neutron spectrum and micro-structural state. These procedures incorporate both physically motivated features and empirical calibration. This semi-empirical nature means that the equations are not applicable to all materials. Here the computational modelling of irradiation embrittlement concerns only ferritic steels.
According to the U.S. ASME code approach documented in Regulatory Guide 1.99, Rev 2 [18], \( RT_{NDT} \) is defined as the shift in the 41 J impact energy transition temperature, \( TK_{41J} \). It is assumed that the true static fracture toughness shift and crack arrest shift is equal to or less than \( \Delta TK_{41J} / 2 \). An additional material specific margin in the determination of \( RT_{NDT} \) is also required. For welds the margin is 31 °C, and for base metals it is 19 °C [18]. In both cases, the margin is not more than \( \Delta RT_{NDT} / 2 \). This margin is added to the experimentally measured or computed \( \Delta RT_{NDT} \) value.

The 41 J transition temperature is measured with Charpy V-notch (CVN) test and the obtained upper shelf energy (USE) is an indirect measure of the ductile initiation fracture toughness at higher temperature upper shelf levels [70].

When credible surveillance data from the reactor in question is not available, the adjusted \( RT_{NDT} \) [°C] for each material in the RPV beltline is to be calculated as [18]:

\[
RT_{NDT} = RT_{NDT, initial} + \Delta RT_{NDT} + \text{margin}
\]  
(B.1.1-1)

where \( RT_{NDT, initial} \) [°C] is the reference temperature for the unirradiated material, as defined in Paragraph NB-2331 of ASME Section III [147]. If the measured values of initial \( RT_{NDT} \) for the material in question are not available, generic mean values for that class of material can be used.

The parameter \( \Delta RT_{NDT} \) [°C] is the mean value of the adjustment in reference temperature caused by irradiation. The definition of it can be written as:

\[
\Delta RT_{NDT} = (CF)f^{(0.028-0.10 \log(f/f_{adj}))}
\]  
(B.1.1-2)

where \( CF \) [°C] is the chemistry factor, \( f [\times 1.0E-19 \text{ n/cm}^2] \) is the accumulated neutron fluence (for \( E > 1.0 \text{ MeV} \)) and \( f_{adj} = 1.0 \text{ n/cm}^2 \). RG 1.99 [18] contains tabulated \( CF \) data for both welds and base metals. Even when \( \Delta RT_{NDT} \) is calculated with the chemistry factor it is required to use the additional safety margin prescribed in RG 1.99 [18]. When two or more credible surveillance data sets are available \( CF \) is to be determined from the data by least square fitting.

The margin in equation (B.1.1-1) is the quantity [°C] that is to be added to obtain conservative, upper bound values of adjusted reference temperature for the calculations required by Appendix G of 10 CFR Part 50 [118]. The margin [°C] can be written as:

\[
\text{margin} = 2 \sqrt{\left(\frac{\sigma_I}{\sigma_{I,adj}}\right)^2 + \left(\frac{\sigma_{\Delta}}{\sigma_{\Delta,adj}}\right)^2}
\]  
(B.1.1-3)

where \( \sigma_I \) is the standard deviation of the initial \( RT_{NDT} \), \( \sigma_{\Delta} \) is the standard deviation of \( \Delta RT_{NDT} \), and \( \sigma_{I,adj} = \sigma_{\Delta,adj} = 1.0 \text{ °C} \). The values for \( \sigma_I \) and \( \sigma_{\Delta} \) are given in RG 1.99 [18] for both base metal and welds.

RG 1.99 [18] considers also the decrease of accumulated fluence through the wall thickness, i.e. attenuation, can be written as follows:
\[ f = f_{\text{surf}} \exp \left[ \frac{-0.24(25.4 \times x)}{x_{\text{adj}}} \right] \]  

(B.1.1-4)

where \( f_{\text{surf}} \) is the calculated value of the neutron fluence at the inner wetted surface, \( x \) [mm] is the depth coordinate through the wall with origin at inner surface and \( x_{\text{adj}} = 1.0 \) mm. The procedures of RG 1.99 [18] are based on only 177 data points and on the incomplete understanding of embrittlement mechanisms that were available in the 1980's [78]. By scope the procedure of ref. [18]:

- applies to U.S. RPV base materials and to their welds,
- is valid as such for a nominal irradiation temperature 288 °C, below 274 °C embrittlement is greater, and above 310 °F less embrittlement occurs, with correction factor used for justification by reference to actual data.

In the French RSE-M code [61], there are two equations for the \( RT_{\text{NDT}} \) shift of ferritic RPV base material. These are FIM and FIS equations, as derived by Brillaud et al. [62], and they are based on extensive test reactor experiments. The FIM equation for \( \Delta RT_{\text{NDT}} \) [°C] can be written as:

\[
\Delta RT_{\text{NDT}} = \left[ 17.3 + 1537 \left( \frac{P}{P_{\text{adj}}} - 0.008 \right) + 238 \left( \frac{Cu}{Cu_{\text{adj}}} - 0.08 \right) + 191 \left( \frac{Ni}{Ni_{\text{adj}}} \right)^2 \left( \frac{Cu}{Cu_{\text{adj}}} \right) \right] \times \left[ \frac{f}{f_{\text{adj}}} \right]^{0.35} \times (1 \, ^{\circ}C)
\]  

(B.1.1-5a)

whereas the original FIS equation can be written as:

\[
\Delta RT_{\text{NDT}} = \left[ 8 + 24 + 1537 \left( \frac{P}{P_{\text{adj}}} - 0.008 \right) + 238 \left( \frac{Cu}{Cu_{\text{adj}}} - 0.08 \right) + 191 \left( \frac{Ni}{Ni_{\text{adj}}} \right)^2 \left( \frac{Cu}{Cu_{\text{adj}}} \right) \right] \times \left[ \frac{f}{f_{\text{adj}}} \right]^{0.35} \times (1 \, ^{\circ}C)
\]  

(B.1.1-5b)

where \( P \), \( Cu \) and \( Ni \) are the quantities of phosphorus, copper and nickel in units of weight percentage [weight-%] and \( P_{\text{adj}} = Cu_{\text{adj}} = Ni_{\text{adj}} = 1.0 \) weight-%. These two equations are essentially the same, but whereas the FIM equation is a direct best fit to data, the FIS equation is more conservative, from a safety point of view.

The French approach differs from the ASME methodology. The \( RT_{\text{NDT}} \) shift is defined as the shift in the 56 J impact energy transition temperature, \( TK_{56J} \) [°C], or the shift in the 0.9 mm lateral expansion transition temperature, \( TK_{0.9\text{mm}} \) [°C], whichever is greater. The lateral expansion corresponding to a certain energy level is affected by the material yield strength. Increasing the yield strength makes plastic deformation of the specimen more work demanding. Therefore, \( \Delta RT_{\text{NDT}} \) is generally controlled by \( \Delta TK_{0.9\text{mm}} \). The French approach does not apply additional safety margins. The French codes [63,64] allow the fluence dependence of \( \Delta RT_{\text{NDT}} \) to be determined experimentally, but do not give any recommendations for the type of expression to be used.
FIM and FIS equations were developed in the late 1980’s. To incorporate all French surveillance data obtained since then and to take better into account the high fluence region, Todeschini et al. [65] developed the following updated equation. It can be written as:

$$\Delta R_{NDT} = A \left[ 1 + 35.7 \left( \frac{P}{P_{adj}} - 0.008 \right) + 6.6 \left( \frac{Cu}{Cu_{adj}} - 0.08 \right) + 5.8 \left( \frac{Ni}{Ni_{adj}} \right)^2 \left( \frac{Cu}{Cu_{adj}} \right) \right]^{0.59}$$  \hspace{1cm} (B.1.1-6)

where $A$ is 15.4 °C for forgings and standard reference material (SRM), and 15.8 °C for welds, whereas $(X)^+$ means that the value of the term is zero when $X$ is negative.

The German Nuclear Safety Standards KTA 3201.2 [66] and KTA 3203 [67] require that the USE of the RPV base material must remain above 68 J during operation. In the KTA 3203 standard [67], the $R_{NDT}$ shift of the ferritic RPV base material is expressed as a temperature shift limit, $RT_{lim}$ [°C], instead of a predictive equation. It states that for the temperature shift of the most irradiated region of the RPV inner wall:

$$RT_{NDT} \leq RT_{lim} = \begin{cases} \frac{40 \, ^{\circ}C}{30 \, ^{\circ}C + 10 \times f}, & f > 1 \times 10^{19} \text{n/cm}^2 \ (E > 1 \text{MeV}) \\ \frac{f}{f_{adj}}, & f > 1 \times 10^{19} \text{n/cm}^2 \ (E > 1 \text{MeV}) \end{cases}$$  \hspace{1cm} (B.1.1-7)

This model is also valid for RPV weld materials on the conditions that $Cu < 0.15$ weight-% and $0.05 < Ni < 1.7$ weight-%, respectively. This model is very conservative and will be included only as an inspection whether the determined temperature transition shifts exceed it or not.

As for the chemistry factor $CF$, the European approaches also apply that concept to determine the irradiation shift directly from the steel chemistry. The French codes contain chemistry factor equations which are dependent on $P$, $Cu$ and $Ni$.

The Japan Electric Association (JEAC) describes in standard JEAC 4201-2000 [68] a method to calculate the $R_{NDT}$ shift caused by neutron irradiation throughout the service life of the RPV materials. It is calculated separately for base and weld materials. These equations recognize the effects of $P$, $Cu$ and synergism between $Cu$ and $Ni$. The equation for base material can be written as:

$$\Delta R_{NDT} = -16 + 1210 \left( \frac{P}{P_{adj}} \right) + 215 \left( \frac{Cu}{Cu_{adj}} \right) + 77 \left( \frac{Ni}{Ni_{adj}} \right) \left( \frac{Cu}{Cu_{adj}} \right) \times \left[ \frac{f_{adj}}{f_{adj}^{0.29-0.04 \log(f/f_{ef})}} \right] \times (1 \, ^{\circ}C)$$  \hspace{1cm} (B.1.1-8a)

and the equation for weld material can be written as:
\[
\Delta RT_{NDT} = \left[ -26 - 24 \left( \frac{Si}{Si_{adj}} \right) - 61 \left( \frac{Ni}{Ni_{adj}} \right) + 301 \left( \frac{Ni}{Ni_{adj}} \right) \left( \frac{Cu}{Cu_{adj}} \right) \right] \times \\
\times \left[ \left( \frac{f}{f_{adj}} \right)^{0.25 \text{--} 0.10 \log} \right] \times (\text{1 °C})
\]

(B.1.1-8b)

where \(Si\) is the quantity of silicon in units of weight percentage [weight-%] and \(Si_{adj} = 1.0\) weight-%. No thresholds are explicitly given for equations (B.1.1-8). The embrittlement correlation equations of JEAC4201-2000 [68] were replaced in JEAC4201-2007 [69] by a new embrittlement correlation method proposed by Soneda et al. [70]. The development of this correlation was needed partly because a lot of new surveillance data had been accumulated, but the major reason was the large underestimation of the surveillance data as predicted by the JEAC4201-2000 [68] method in high-copper base metals irradiated at very low flux conditions in BWRs. It was concluded that this large amount of embrittlement is due to the effect of low flux irradiation, which was not considered earlier. Based on results from microstructural characterization of base metals and weld metals several other important conclusions were also made. All these conclusions were incorporated to a relatively large set of time dependent equations describing the microstructural evolution as well as the \(RT_{NDT}\) shift. These complicated equations can only be solved with a computer code. After the development of the JEAC4201-2007 method [69] some new surveillance data were generated, particularly at very high fluences in PWR plants. Half of the data was well predicted by the JEAC4201-2007 method [69] but the remaining half was under predicted. Due to this discrepancy the model coefficients were calibrated against the updated surveillance data. The revised embrittlement correlation method was adopted as 2013 addendum to JEAC4201-2013 [73].

More recent advanced models for \(RT_{NDT}\) shift of ferritic RPV base material are described here without equations due to their large number and complexity.

Due to better understanding of the irradiation embrittlement processes and broader databases of surveillance results, new models for the \(RT_{NDT}\) shift have been and are being developed. The E 900-02 model, also known as the Regulatory Guide 1.99 Rev. 3, was introduced in 2002 and is recommended by the American Society for Testing and Materials (ASTM) [74]. This model considers two degrading mechanisms, which are stable matrix damage and copper rich precipitations. This \(RT_{NDT}\) shift model is more accurate than the RG 1.99, Rev 2 [18] model and other earlier models.

The EONY model has been developed by Eason, Odette, Nanstadt and Yamamoto [75,76], which effort was motivated by further surveillance data obtained during 2003-2004, both from BWRs and PWRs. To be able to represent correctly the data from BWRs, for which the fast flux at the RPV wall is substantially lower than that in PWRs, the notion of an effective fluence was introduced. This model is also limited to consider stable matrix damage and copper rich precipitations. This \(RT_{NDT}\) shift model is more accurate than the RG 1.99, Rev 2 [18] model and other earlier models.

The wide range embrittlement trend curve by Kirk [77] is based on larger amount of experimental data than the models described above. This approach differs from the others by applying good representations of empirical trends instead of a physically guided mathematical form. The model considers the threshold values for minimum and maximum copper contents
as well as neutron fluence and threshold fluence for embrittlement to occur. The forms of two significant model terms were determined by regression analysis of the copper threshold values estimated at different fluences. This $RT_{NDT}$ shift model is more accurate than the RG 1.99, Rev 2 [18] model and other earlier models.

None of the $RT_{NDT}$ shift models presented above consider any secondary or parallel processes, such as LBP. In addition, since these models are developed using specific databases and materials, test results from significantly different physical conditions may not agree well with predictions. For instance, materials with a composition very different from those in the database should not be expected to behave accordingly.

Eason, Odette, et al. published in 2013 a physically based correlation for irradiation induced transition temperature shifts for RPV steels [78]. In terms of fluence this correlation is valid up to $5.0E+19$ n/cm², and is thus applicable also to design lifetime of 40 years for BWR plants and beyond. The 2013 publication is a shorter presentation of the model described originally in a more detailed project report published in 2007 [79]. The correlation consists of two terms, which correspond to stable matrix features (SMF) and CRP. The correlation has been fitted to data from U.S. NPP RPVs. The applicability of the correlation for BWR plants is further enhanced by its capability to capture also the low flux conditions of BWRs. This correlation does not specifically take into account the possible LBP. The Eason, Odette, et al. model [78] is now included also in the ASME code, see 2015 Edition of ASME Section XI [81]. This model is also best applicable to the RPVs of the Asea-Atom BWR plants. By scope the procedure of ref. [78]:

- apply to U.S. RPV base materials and to their welds,
- is based on 855 data points,
- is valid as such for a nominal irradiation temperature from 272 to 299 °C,
- is valid for fluences from $9.26\times10^{15}$ to $7.13\times10^{19}$ n/cm², and
- is valid for fluxes from $1.81\times10^{8}$ to $9.71\times10^{11}$ (n/cm²)/s.

Odette and Yamamoto developed also in 2013 two physically based RPV steel irradiation embrittlement prediction models [80] applicable to BWR plants. They take into account the dose rate effect, radiation enhanced diffusion and precipitation as well as low flux. These models are:

- Two feature solute trap enhanced recombination model (2FM), and
- Three feature model (3FM) with additional unstable matrix defects (UMD) hardening and sinks.

These RPV steel irradiation embrittlement models have been fitted against U.S. NPP RPV embrittlement database (PREDB) data, of which 99 % is for fluences less than $5.0E+19$ n/cm². Thus, the 2FM and 3FM models are less accurate for high fluences. 2FM model is applicable to fluence levels expected in the currently planned reactor lifetime and flux levels of up to $1.0E+12$ n/(cm²·s). The model assumes that dose rate effects are solely due to the flux dependence of radiation enhanced diffusion (RED). The 2FM model considers two hardening features, which again are SMF and CRP. The presented 3FM model is an update of the original one that was developed in 1990’s to treat the effects of high flux test reactor irradiations. In addition to SMF and CRP, 3FM model considers also the effect of unstable matrix defects (UMD). Due to their effect, increasing flux can increase, decrease, or leave unaffected hardening and embrittlement, depending on the alloy composition, irradiation temperature, flux and fluence. The description of the 3FM model on irradiation hardening and embrittlement is extensive enough but still approximate. In reality, there is no sharp delineation between UMD,
SMF and CRP, especially at high flux. Such features also implicitly interact with one another by mechanisms, such as competition for finite numbers of solute atoms and excluded volume effects. In effect, the 3FM model only applies to very high flux test reactor data. The 2FM model is the foundation of the regulatory approach in the U.S. and it more than covers BWRs even for extended life. The 2FM and 3FM models do not specifically take into account the LBP.

The change of fracture toughness due to irradiation is commonly defined through the shift in $RT_{NDT}$. Appendix A of ASME Section XI [81] presents for ferritic RPV base material definitions of critical fracture toughness for the lower bound crack initiation value, $K_{ic}$ [MPa·m$^{-1}$], and for the lower bound crack arrest value, $K_{ia}$ [MPa·m$^{-1}$]. The effects of irradiation on the crack initiation and arrest fracture toughness can be estimated by applying the shift in the $RT_{NDT}$ to shift the ASME lower bound curves by moving the curves by the same shift amount, but leaving the shapes unaltered. These relationships can be written as [81]:

$$
K_{ic} = \left[ 36.5 + 22.783 \exp \left[ 0.036 \left( \frac{T - RT_{NDT}}{T_{adj}} \right) \right] \right] \times (1 \text{ MPa} \cdot \text{m}) \tag{B.1.1-9a}
$$

$$
K_{ia} = \left[ 29.4 + 13.675 \exp \left[ 0.0261 \left( \frac{T - RT_{NDT}}{T_{adj}} \right) \right] \right] \times (1 \text{ MPa} \cdot \text{m}) \tag{B.1.1-9b}
$$

where $T_{adj} = 1.0 ^{\circ}C$. The upper limit value for both $K_{ic}$ and $K_{ia}$ is 240 MPa·m [81]. For LWR plants the decrease of the upper shelf value of both $K_{ic}$ and $K_{ia}$ as a function of fluence can be assessed by using the simple procedure in ref. [83]. This procedure is a correlation as based on measured data.

According to the KTA 3201.2 [66], the fracture toughness during operation is determined with the given fracture toughness curve by using the adjusted reference temperature. This temperature may be determined either according to the reference temperature $RT_{NDT}$ concept or the Master Curve concept. The latter concept has been developed in VTT by Wallin, see e.g. refs. [84,85,86]. The Master Curve concept is applicable to the RPVs of the Asea-Atom BWR plants.

According to the RCC-M code [63] the fracture toughness during operation is determined with the given fracture toughness curve by using the adjusted reference temperature. Again, the adjusted reference temperature may be determined either according to the reference temperature $RT_{NDT}$ concept or the Master Curve concept.

The Master Curve concept together with some modifications is described in the following. The scatter of fracture toughness in the transition region of low alloyed ferritic RPV steels can be modelled with Weibull statistics. This approach, proposed by Wallin [84] and implemented in Test Standard ASTM E1921-05 [86], characterizes the median $J$-integral based fracture toughness, $K_{JC}$, of ferritic steels at the onset of cleavage cracking. It uses a concept of universal temperature dependence of fracture toughness in the transition region, which is called the Master Curve. It is defined as the median fracture toughness of a test specimen, adjusted to a crack front length of 25.4 mm (1 inch).

The temperature dependence of the fracture toughness $K_{JC}$ [MPa·m$^{-1}$] according to the Master Curve can be written as [86]:

208
where the unit of the reference fracture toughness transition temperature $T_0$ is °C. $T_0$ corresponds to the temperature at which $K_{JC} = 100$ MPa$\sqrt{m}$. Due to equation (B.1.1-10) being a laboratory data based correlation the parts on the right side of the equality sign do not follow a rigorous unit treatment. This simple equation, which describes the shape of the median Master Curve, is assumed to be constant for all ferritic steel types, fluence levels and annealing heat treatments.

Instead of indirect CVN tests, the measurement of actual fracture toughness of irradiated small surveillance specimens has become possible using elastic-plastic fracture mechanics methods, primarily employing the $J$-integral measure of toughness. The ductile Master Curve reference temperature $T_0$ can be determined using a small number of test specimens and the $J$-integral resistance ($J-R$) curve can be measured for assessment of ductile fracture initiation and tearing resistance [70].

Two more analysis procedures have been developed for a material that contains randomly distributed macroscopic inhomogeneities. These are the fitness-for-service assessment procedure SINTAP [87] and the multi-modal (MM) method [88,89]. SINTAP contains a lower tail modification of the Master Curve analysis which enables conservative lower bound type fracture toughness estimates. In the MM approach, the total dataset is presumed to be composed of several subsets (populations), even up to infinitely many. Each subset follows the Master Curve distribution but has a different $T_0$. The combined distribution is fully defined by two parameters: the mean reference temperature of all populations, $T_m$, and the standard deviation around the mean $\sigma_{Tm}$ [90].

There exists a quite large number of correlations for determining the effect of neutron irradiation to fracture toughness of ferritic RPV base materials. For collections of relevant correlations, see e.g. IAEA report No. NP-T-3.11 [74] and article PVP2014-28540 [91].

The materials of the BWR RPV internals are mainly of austenitic stainless steel and nickel-based alloys. For the former material type report NUREG/CR-7027 [126] presents for ductile fracture toughness $J_{lc}$ a disposition curve that bounds the existing experimental data. The trend curve takes into consideration: (a) a threshold neutron exposure for irradiation embrittlement of austenitic stainless steels and a minimum fracture toughness for these materials irradiated to less than the threshold value, (b) a saturation neutron exposure and a saturation fracture toughness for materials irradiated to greater than this value, and (c) a description of the change in fracture toughness between the threshold and saturation neutron exposures. The fracture toughness $J_{lc}$ [kJ/m$^2$] can be written as [126]:

$$J_{lc} = \left(7.5 + 110\exp\left[-0.35\left(\frac{f}{f_{adj}}\right)^{0.4}\right]\right) \times \left(1 \text{kJ/m}^2\right)$$

(B.1.1-11)

where the unit of the fluence $f$ is now dpa and $f_{adj} = 1.0$ dpa. Due to equation (B.1.1-11) being a laboratory data based correlation the parts on the right side of the equality sign do not follow a rigorous unit treatment. The lower bound trend curve given by equation (B.1.1-11) is consistent with the EPRI lower bound model proposed for PWR plants. The MRP model for fracture toughness is expressed in terms of a lower bound $K_{jc}$ curve, where $K_{jc}$ is the equivalent
critical stress intensity factor. MRP stands for Materials Reliability Program. Which works within/through EPRI. This model bounds all the fracture toughness data from the fast reactors, BWRs, and PWRs as a function of the neutron dose. $K_{\text{fc}}$ [MPa√m] can be written as [129]:

$$K_{\text{fc}} = \left[180 - 142\left[1 - \exp\left(f/f_{\text{adj}}\right)\right]\right] \times\left(\text{MPa}\sqrt{\text{m}}\right)$$

(B.1.1-12)

where the unit of fluence $f$ is again dpa and $f_{\text{adj}} = 1.0$ dpa. Due to equation (B.1.1-12) being a laboratory data based correlation the parts on the right side of the equality sign do not follow a rigorous unit treatment. As for change of material strength properties of austenitic stainless steels due to irradiation, reports NUREG/CR-7027 [126] and MRP-135-Rev.1 [127] present for types 304 and 316 steels fluence dependent correlations for the yield strength and the ultimate tensile strength, respectively.

The earliest fracture toughness models are also the most conservative ones. These include the models in Appendix A of ASME Section XI [81] and in the RCC-M code [63]. There are several such earlier models. The commonly used ones are listed in IAEA report No. NP-T-3.11 [74] and article PVP2014-28540 [91]. Arguably, presently the most realistic and accurate development is the Master Curve concept by Wallin [84, 86]. This is because it is based on measuring the reference temperature $T_0$ from the test specimens instead of indirect CVN tests. With the Master Curve even a small number of test specimens suffices. Moreover, the Master Curve concept is the first fracture toughness model that takes into account the scatter of fracture toughness in the transition region.

**B.1.2 Susceptibility to irradiation embrittlement**

The scope in the following covers both low alloy and carbon steels as well as austenitic stainless steels. Low alloy and carbon steels do exhibit a sharp ductile to brittle transition behaviour, whereas wrought austenitic stainless steels do not. The toughness losses of the latter steels due to irradiation tend to accumulate with increasing fluence and saturate at levels > 1.0E+25 n/m². Information in ref. [19] describes the results of a fracture toughness study performed on irradiated reactor internal material of type 304 stainless steel taken from operating BWRs with fluences ranging from 1.0E+25 to 6.0E+25 n/m² ($E > 1$ MeV). That study confirmed a fracture toughness saturation level of 55 MPa√m for all fluences considered, and can be directly applied to the evaluation of highly irradiated RPV internals [1].

While neutron irradiation results in some reduction in fracture toughness at the center of the top guide and the mid-plane of the core shroud, elsewhere fracture toughness remains high [1]. For cracked components a fracture mechanics evaluation of material that has been exposed to high neutron fluence should be performed to assure crack stability. In general, there appears to be no life limiting degradation due to irradiation embrittlement alone.

There remain limitations in accurately predicting the embrittlement trends of BWR materials. Since there are only few data points associated with irradiation at the lower BWR neutron fluence rates (most data are obtained from PWR or test reactor irradiations), uncertainties remain regarding the effect of neutron flux on the embrittlement rate [3].

Material type specific susceptibility to irradiation embrittlement:

- Ferritic steels: Embrittlement of low alloy steels used for LWR RPVs is primarily caused by the irradiation induced formation of copper-rich precipitates that harden the material matrix and reduce fracture toughness. Significant variations in irradiation embrittlement have also been observed between different types of ferritic steels and even between different
heats of the same steel. Steels with a very low copper content show low embrittlement in spite of high irradiation doses. The effect of irradiation exposure at low temperatures (below 274 °C) increases the rate of embrittlement damage. Weld metal is generally more sensitive to irradiation embrittlement than base metal. Chemical impurity level, chemical composition variability and different microstructure are responsible for the greater sensitivity of the weld metal [3]. The threshold neutron fluence for ferritic RPV base materials is 1.0E+17 n/cm² (E > 1.0 MeV) [117,128].

- **Austenitic stainless steels [3]:** All data for the yield strength, the ultimate tensile strength and the elongation between room temperature and LWR operating temperature indicate a saturation of irradiation strengthening between approximately 5 dpa (3.3E+21 n/cm², E > 1.0 MeV) and approximately 20 dpa (1.33E+22 n/cm², E > 1.0 MeV). Beyond that the mechanical properties reach a plateau and remain at that level without further change. Existing fracture toughness data show a saturation of irradiation embrittlement between 5 and 10 dpa (3.33E+21 and 6.67E+21 n/cm², E > 1.0 MeV). The minimum initiation fracture toughness, J_{IC}, is approximately 10 kJ/m², from which the lower bound fracture toughness, K_{IC}, is calculated to be 38 MPa\sqrt{m} at neutron doses greater than 6.67E+21 n/cm² (E > 1.0 MeV) or 10 dpa. The stainless steel fracture data are sufficiently complete to identify the lower bound plateau at fluences > 10 dpa (6.67E+21 n/cm², E > 1.0 MeV) up to approximately 90 dpa (6.00E+22 n/cm², E > 1.0 MeV).

Sparse or non-existent data at high fluences accumulated over long times create uncertainties in irradiation embrittlement predictions [94]. This issue relates to the extension of operational life to 60 and 80 years. Simply stated, extending the operation from 40 to 80 years doubles the neutron exposure for the RPV. To obtain data at high fluences for life extension purposes requires applying test reactor experiment data produced under high neutron fluxes. More research is needed to enable the application of data obtained at high flux to RPV conditions of low flux and high fluence. In addition, more recent experimental evidence shows that LBP could produce a significant degradation effect to RPV base material. This phase concerns the behaviour of nickel, manganese and silicon after accumulation of high fluence, and can result with unanticipated drop in RTNDT [170].

Table B.1.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to irradiation embrittlement.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>• affects only within a few meters from reactor core,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>• for ferritic steels, the decrease of fracture toughness as a function of fluence does not saturate,</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>• for austenitic stainless steels, the decrease of fracture toughness as a function of fluence saturates</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>between 5 and 10 dpa,</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>• see Section B.1.1 for assessment of decrease of fracture toughness.</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td></td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
</tr>
</tbody>
</table>
B.1.3 Mitigation of irradiation embrittlement

To decrease the propagation of irradiation embrittlement in BWR RPV and its internals, several mitigation techniques have been developed. These techniques include:

- fluence rate reduction,
- modification of emergency core cooling and other systems,
- thermal annealing, and
- power reduction.

These mitigation techniques are described in more detail in the following. The mitigation of irradiation embrittlement concerns mainly the RPV.

Fluence rate reduction

Two principles are available for reducing fluence accumulation in the exposed components [99]. The core, i.e. the irradiation source, can be modified or reduced to give a lower fluence rate. Another way is to place irradiation shields or reflectors between the core and the exposed components. A cylindrical tank called core barrel holds the core basket containing fuel assemblies in PWR and VVER plants (the latter one is Russian PWR type). The cylindrical wall of the core barrel serves also as a thermal shield to reduce the neutron flux at the RPV wall [101]. Additional neutron shielding is provided in the active core region by neutron panels or thermal shields that are attached to the outside of the core barrel at strategically located positions [102]. Yet another option is to use an irradiation shield between the core and the core barrel. Such a shield is e.g. in the Olkiluoto 3 PWR unit [103]. This far, no irradiation shields or reflectors have been installed to BWR RPVs.

The main objective of low fluence schemes is to modify the circumferential neutron fluence rate distribution in such a way that the fluence rate at the critical locations of the RPV is reduced. The critical locations, i.e. those restricting the RPV service life, are generally welds. This is due to their chemical composition and subsequent faster embrittlement, but also the base material may become life limiting [99].

The fluence rate at the RPV can be reduced most efficiently by reducing power in the critical peripheral fuel assemblies, i.e. in those nearest the RPV wall. Roughly 85 % of the fluence to the RPV is estimated to come from the core peripheral assemblies [100].

The following procedures are applicable for reducing the fluence rate [104,105,106,107,108]:

- Low-leakage fuel management. Some or all of the peripheral fresh fuel assemblies are replaced by low reactivity fuel assemblies, i.e. those having spent from one to three cycles in the reactor.
- Some of the peripheral fuel assemblies are replaced by dummy assemblies, which contain stainless steel or zirconium rods instead of UO₂ pellets. Either partially or fully replaced assemblies can be used. Typically, 5-10 % of the fuel assemblies need replacing to maintain circumferential symmetry.
- Installation of neutron absorbing materials on the core periphery. For example, peripheral control rods or burnable absorber rods placed at critical locations can be used to reduce the fluence rate.
Modification of emergency core cooling and other systems

The most severe loading condition for RPVs is a pressurised thermal shock (PTS). An extra safety margin against it can be achieved by modifying the emergency cooling system so that the maximum thermal loading in the RPV during such events is reduced. However, PTS concerns mainly PWR RPVs, for BWR RPVs it is very unlikely.

To protect RPVs against transient load cases with over pressure during their lower temperature phase, pressure-temperature limits taking into account the fracture toughness of the RPV material and irradiation embrittlement are determined for the RPV. Pressure-temperature limiters together with programmable controllers are used to ensure that the pressure limits are not exceeded during the lower temperature phases of heat-up and cool-down [109].

Thermal annealing

Thermal annealing is the only way to recover the fracture toughness properties of materials [110]. Thermal annealing is a method by which the component is heated up to some temperature considerably higher than the operational temperature, by use of an external heat source (electrical heaters, hot air). This temperature is held for a given period and then the component is slowly cooled. This far thermal annealing has only been applied to RPVs.

As an example of the effect of thermal annealing, the planned annealing of the Yankee Rowe PWR RPV was estimated to give a 45-55 % recovery of the fracture toughness, with the annealing temperature of 345 °C being 83 °C above the service temperature [111]. In a study [112] concerning LWR RPV base metal SA 553 B with 0.20 % of copper and 0.010 % of phosphorus, the percentage of transition temperature recovery after irradiation at 288 °C (2.4E+19 n/cm²) was 52 % at 343 °C annealing, 72 % at 399 °C annealing and 99 % at 454 °C annealing, respectively. The recovery of upper shelf energy was complete already after 399 °C annealing. Annealing times were 168 h.

In the wet annealing method the maximum temperature is limited to about 350 °C. Hence, it can be used only in reactors with a low service temperature. Due to a rather limited recovery and a high re-embrittlement rate, wet annealing is not a viable solution for power reactors.

In dry annealing, a RPV is heated by electric resistance heaters [99]. Proposals for using e.g. induction heating, superheated steam or hot burned gas have also been suggested.

This far the thermal annealings have been mainly carried out for VVER 440 reactors [99].

The computational assessment for recovery of CVN USE and $RT_{NDT}$ shift for U.S. RPV ferritic base materials are described in Regulatory Guide 1.162 [92]. These equations were developed by Eason et al. [93]. According to ref. [92], alternative computational methods may be used if appropriate justification is provided. For convenience, the formulations by Eason et al. [93] are presented here. The correlation developed for CVN USE after thermal annealing, $USE_{ia}$ [J], can be written as:

$$USE_{ia} = USE_i + \left[1 - 0.586 \left( -\frac{t_i}{15.9 \cdot \text{adj}_i} \right) \right] \times$$

(B.1.3-1)
\[
\times \left\{ 0.7752 \times \Delta\text{USE}_i + \left( 0.120 \frac{T_a}{T_{\text{adj}}} - 104 \right) \left( \frac{Cu}{Cu_{\text{adj}}} \right) + 0.0389 \frac{T_a}{T_{\text{adj}}} - 17.6 \right\} \times (1.36 J)
\]

where \(\text{USE}_i\) [J] is \(\text{USE}\) after irradiation, \(t_a\) [h] is annealing time, \(T_{\text{adj}} = 1.0\) h, \(\Delta\text{USE}_i\) [J] is \(\text{USE}\)-\(\text{USE}_i\), \(T_a\) [°C] is annealing temperature and \(T_{\text{adj}} = 1.0\) °C. The correlation developed for \(RT_{\text{NDT}}\) after thermal annealing, \(RT_{\text{NDT},i}\) [°C] can be written as:

\[
RT_{\text{NDT},i} = RT_{\text{NDT},j} - \Delta RT_{\text{NDT}} \left\{ \frac{5}{9} \left[ 0.5 + 0.5 \tanh \left( \frac{a_1 (T_a + 32) (\frac{9}{5} - a_2)}{a_3} \right) \right] - 32 \right\} \tag{B.1.3-2}
\]

where \(RT_{\text{NDT},j}\) [°C] is \(RT_{\text{NDT}}\) after irradiation, \(\Delta RT_{\text{NDT}}\) [°C] is \(RT_{\text{NDT}}\)-\(RT_{\text{NDT},i}\), whereas:

\[
a_1 = 1 + 0.0151 \ln \left( \frac{T_a}{T_{\text{adj}}} \right) - 0.424 \left( \frac{Cu}{Cu_{\text{adj}}} \right) \tag{B.1.3-3a}
\]

\[
Cu = \min \left\{ Cu_{\text{measured}}, 0.30 \text{ weight - %} \right\} \tag{B.1.3-3b}
\]

\[
a_2 = \begin{cases} 
0.584(T_i + 637), & T_i \geq 427 \text{ °C} \\
0.584T_i - 15.5 \ln \left( \frac{\phi}{\phi_{adj}} \right) + 833, & T_i \leq 399 \text{ °C} 
\end{cases} \tag{B.1.3-3c}
\]

\[
a_3 = 95.7 \tag{B.1.3-3d}
\]

where \(\phi\) [n/(m²s)] is neutron flux and \(\phi_{adj} = 1.0\) n/(m²s). Interpolation for values of \(a_2\) is allowed between 399 and 427 °C. The correlation for recovery of \(RT_{\text{NDT}}\) shift for U.S. RPV ferritic base materials in IAEA report No. NP-T-3.11 [74] is similar to that in Regulatory Guide 1.162 [92].

**Power reduction**

The fluence rate at the RPV can also be decreased simply by reducing core power. A power reduction of 10-20 % has typically been projected to result in sufficient reduction in the fluence rate [116]. There are no reported cases in which only derating power would have been used to decrease the fluence rate.

**B.1.4 Inspection and monitoring of irradiation embrittlement**

Every BWR RPV operating in U.S. and Europe has an on-going RPV material irradiation surveillance programme [117]. This is mainly carried out by testing surveillance capsules, which have been exposed to irradiation inside the RPV for different durations. To date, a large number of surveillance capsules have been removed from their host RPVs and tested. In a BWR the only concern relative to irradiation embrittlement is the hydrostatic test temperature.
Additional evidence of the changes in the fracture toughness of the RPV beltline materials resulting from neutron irradiation shall be obtained from results of supplemental tests, such as measurements of dynamic fracture toughness of the beltline materials [117].

Each plant in the U.S. BWR fleet has an existing RPV surveillance programme that consists of a set of surveillance capsules that were installed when the plant was licensed. This is also the case for all BWR RPVs. These capsules typically include specimens for plate, weld and HAZ materials. The test results from the specimens are used for monitoring irradiation embrittlement of the RPV beltline materials.

Instead of using the plant specific surveillance data, the data from across the whole fleet could be used. Material data from another plant surveillance program or other source could be used to better represent the limiting material for the target plant. Integrating the existing surveillance programs together is called the integrated surveillance programme (ISP).

In the U.S., Appendix G of 10 CFR Part 50 [118] delineates the requirements for prevention of fracture of the ferritic materials in the primary coolant pressure boundaries of the U.S. NPPs, with emphasis on the RPV. Appendix G and Appendix H of ref. [118] necessitate the calculation of changes throughout the service life in the fracture toughness of the RPV as caused by neutron irradiation. As for the location of the surveillance specimen capsules, Appendix H also requires that they must be placed near the inside of the RPV wall in the beltline region so that the specimen irradiation history duplicates to the extent practicable within the physical constraints of the system the neutron spectrum, temperature history and maximum neutron fluence experienced by the RPV inner wall.

Concerning requirements for an ISP, according to Appendix H of ref. [118] the representative materials chosen for surveillance for a reactor are irradiated in one or more reactors that have similar design and operating features. The ISP is intended for substituting the existing plant surveillance capsule programs with representative weld and base material data from the host reactors.

According to the stipulations of the German Nuclear Safety Standard KTA 3203 [67] irradiation embrittlement can be neglected when neutron fluence is lower than 1.0E+21 n/m² ($E > 1$ MeV). KTA 3203 [67] is valid up to fluence of 5.0E+23 n/m². However, the German Reactor Safety Commission (RSK) Guidelines [119] include a recommendation for a maximum allowable fast neutron fluence of 1.0E+23 n/m². The number of surveillance sets and the withdrawal schedule relative to the RPV fluence (50 % and 100 % of the design fluence) is also fixed in KTA 3203 [67], as depending on the RPV design fluence.

In Finland, the requirements are presented in Regulatory Guides on nuclear safety (YVL) by the Radiation and Nuclear Safety Authority (STUK). They provide the requirements for brittle fracture analysis, fracture toughness tests and surveillance program. The requirements are in accordance with the ASME code. In Olkiluoto BWR units OL1 and OL2, RPV surveillance specimens (Charpy and tensile test specimens for base metal, weld metal and HAZ) are located at two distances from the core. The nearest specimens are close to the outside surface of the core shroud wall. Other specimens are located approximately in the midpoint between the core shroud and the inner surface of the RPV. In addition to these RPV specimens, in OL1 there are also core shroud specimens (of stainless steel) inside the core shroud [117].
However, degradation by irradiation embrittlement cannot be detected by in-service inspection (ISI) techniques, which are for detection of cracks. The testing of the surveillance specimens is the only way to detect this degradation mechanism. Even though irradiation embrittlement reduces the margin of a material to resist crack propagation due to other degradation mechanisms, such as fatigue or SCC, the applicable ISI techniques are left to be described in connection to them.

B.2 Thermal embrittlement

Thermal embrittlement is a time and temperature dependent degradation mechanism. It is caused by the thermally activated movement of lattice atoms over a long time period, a process which can occur without external mechanical load. Changes in microstructure and material properties are the consequence of these diffusion processes as indicated by a decrease in fracture toughness but as an increase in the yield strength and the tensile strength \[1\].

Thermal embrittlement does not directly cause cracking. However, the margin of a material to resist propagation of cracks due to other causes, such as fabrication, fatigue or SCC, is reduced.

As mentioned earlier in Appendix B.1, with experimental measurements it is difficult or impossible to separate the effects of irradiation embrittlement and thermal embrittlement from each other.

B.2.1 Computational modelling of thermal embrittlement

There are not too many procedures for computational assessment of thermal embrittlement (or thermal ageing) of metallic NPP materials. These procedures are material type specific correlations, as based on fitting to laboratory results. Here the computational modelling of thermal embrittlement concerns only ferritic steels.

As for national codes, assessment of material degradation of RPV base material due to thermal embrittlement is considered in two French codes, namely 2012 edition of RCC-M \[63\] and 2010 edition of RSE-M \[95\]. According to 2012 edition of RCC-M \[63\] the \(\Delta RT_{NDT,aged}^\circ\text{C}\), is computed as:

\[
\Delta RT_{NDT,aged} = \Delta RT_{NDT,initial} + \Delta RT_{NDT,aging}
\]

(B.2.1-1)

The shifts \(\Delta RT_{NDT,aging}^\circ\text{C}\) for base metal and HAZ are shown in Table B.2.1-1 below. This procedure may not be very accurate, but it is used to compute the \(RT_{NDT}\) of thermally aged RPV steels and associated welds because there are no better procedures available.

<table>
<thead>
<tr>
<th>Base metal</th>
<th>(\Delta RT_{NDT,aged}^\circ\text{C})</th>
<th>HAZ</th>
<th>(\Delta RT_{NDT,aged}^\circ\text{C})</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>300 °C</td>
<td>325 °C</td>
<td>350 °C</td>
</tr>
<tr>
<td>P ppm</td>
<td>40 years</td>
<td>60 years</td>
<td>40 years</td>
</tr>
<tr>
<td>40</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>60</td>
<td>3</td>
<td>3</td>
<td>6</td>
</tr>
<tr>
<td>80</td>
<td>4</td>
<td>4</td>
<td>8</td>
</tr>
</tbody>
</table>

\(\Delta RT_{NDT,aged}^{\circ\text{C}}\) for RPV materials due to thermal ageing \[63\]. Here \(P\) means phosphorus.
The 2010 edition of RSE-M [95] contains predictive equations to estimate $RT_{NDT}$ after 40 years of operation. The equations are given for base metal, HAZ under cladding and weld metal.

Chopra [21,122,123] has developed two different approaches to determine the extent of the thermal ageing of CASSs. These approaches quantify the extent of ageing at the LWR operating temperatures by measuring the room temperature Charpy impact energy, $C_v$ [J/cm$^2$], after ageing at temperatures within the range of 300 to 400 °C. The latter ageing temperature range is often employed to accelerate the rate of thermal ageing as compared to that occurring at normal NPP operating temperatures. The first approach provides a means for estimating the lower bound fracture toughness after long-term thermal ageing, whereas the second approach allows estimating fracture toughness at a given service time and temperature. However, both of these approaches are indirect, and therefore their accuracy is questionable. A more accurate and direct determination procedure would be needed. Such as the Master Curve procedure [86] for determining the fracture toughness.

Chopra [122] has also developed a procedure for estimating mechanical properties, i.e. $C_v$ and elastic-plastic fracture toughness, of thermally aged CASS piping components. The procedure is divided into three parts:

a) estimation of lower-bound material properties of steels with unknown chemical composition,

b) estimation of saturation material properties of steels with known chemical composition but unknown service history, and

c) estimation of material properties at time and temperature for steels having known chemical composition and service history.

The estimation of lower-bound material properties required by the first part of the procedure, is based on the worst case chemical composition (> 15 % ferrite), which is very conservative for most steels. A more realistic lower bound can be developed if the ferrite content is known.

Once the value of $C_v$ at saturation is known the service time fracture toughness curve can be determined for known service history by using the correlations by Chopra, published originally in report NUREG/CR-4513, Rev. 1 [123] in 1994 and then as updated in report NUREG/CR-7185 [130] in 2015. The updated lower bound correlations are presented in the following. For centrifugally-cast materials, the best-fit correlations are used. For CF-8M materials the value of the room temperature $C_v$ [J/cm$^2$] for the transition from one expression to the other varies between 35 and 45 J/cm$^2$ because of the differences in the standard deviation for the fit to the individual data set.

At room temperature (RT) of 20 °C, the deformation $J$ per ASTM Specification E 813-85 [124] or E 1152-87 [125], $J_d$ [kJ/m$^2$], for static cast CF-3 and CF-8 steels can be written as:

$$J_d = 49 \left( \frac{C_v}{C_{v_{adj}}} \right)^{0.52} \left( \frac{\Delta a}{\Delta a_{adj}} \right)^6 \times \left( \frac{1 \text{ kJ}}{\text{m}^2} \right)$$

where $C_{v_{adj}} = 1.0$ J/cm$^2$ and $\Delta a_{adj} = 1.0$ mm. For CF-8M steels with RT $C_v$ values greater than 35 J/cm$^2$ $J_d$ is:
\[ J_d = 16 \left( \frac{C_v}{C_{v_{adj}}} \right)^{0.67} \left( \frac{\Delta a}{\Delta a_{adj}} \right)^n \left( \frac{1}{m^2} \right) \]  
(B.2.1-3)

and for CF-8M steels with RT Cv values equal to or less than 35 J/cm\(^2\) it is:

\[ J_d = 1.44 \left( \frac{C_v}{C_{v_{adj}}} \right)^{1.35} \left( \frac{\Delta a}{\Delta a_{adj}} \right)^n \left( \frac{1}{m^2} \right) \]  
(B.2.1-4)

where \( \Delta a \) [mm] is crack extension, and for static-cast or centrifugally-cast CASS materials the exponent \( n \) [-] for CF-3 steel at RT is given as:

\[ n = \max \left\{ 0.15 + 0.16 \log_{10} \left( \frac{C_v}{C_{v_{adj}}} \right) \right\} 0.34 \]  
(B.2.1-5)

and for CF-8 steels as:

\[ n = \max \left\{ 0.20 + 0.12 \log_{10} \left( \frac{C_v}{C_{v_{adj}}} \right) \right\} 0.34 \]  
(B.2.1-6)

and for CF-8M steels as:

\[ n = \max \left\{ 0.23 + 0.08 \log_{10} \left( \frac{C_v}{C_{v_{adj}}} \right) \right\} 0.34 \]  
(B.2.1-7)

The \( J_d \) values for other static-cast CASS materials and centrifugally-cast in different temperatures are defined in the same way in ref. [130].

Depending on the available information, the initial fracture toughness of the unaged material or minimum fracture toughness of unaged CASSs is used as an upper bound for the estimations [123].

As for the change of the material strength properties due to thermal embrittlement, NUREG/CR-4513, Rev. 1 [123] presents for CASSs CF-3, CF-8 and DF-8M thermal exposure dependent correlations for the yield strength and the ultimate tensile strength at RT and 290 °C, respectively.

In Japan, Kawaguchi et al. [131] have developed a correlation for assessment of thermal ageing effect to fracture toughness of CASSs as based on Charpy impact energy. The material specific parameter values in the correlation are obtained by means of a multiple regression analysis of test data serving as a function of the chemical composition of the material. In France, Faidy [132] has developed a correlation for assessment of thermal ageing effect to fracture toughness.
of CASSs. The correlation is based on equivalent ageing time at 400 °C as well as Charpy impact energy values at 20 and 320 °C.

However, there are very few BWR RPV internals of CASS. For the BWR RPV base materials the thermal embrittlement correlation in RCC-M [63] and RSE-M [95] is arguably the most applicable.

**B.2.2 Susceptibility to thermal embrittlement**

Thermal embrittlement is not a significant degradation mechanism for RPV internals made of wrought steel or Ni-Cr-Fe alloys. Neither is it significant for the orificed fuel supports which are made of CASS because stress levels are low. These stress levels are not of sufficient magnitude to cause cracking of the orificed fuel support, irrespective of the delta ferrite content. Therefore, thermal embrittlement is not a significant degradation mechanism for any RPV internals [1].

Embrittlement associated with specific alloy microstructural/compositional features and exposure over time at elevated temperature is of concern with CASSs, martensitic and ferritic stainless steels, ferritic and low alloy steels as well as with some high chromium content nickel-base alloys. The mechanisms of such embrittlement are varied. They range from [3]:

- the precipitation of secondary phases (which increases the yield strength and decreases the fracture toughness),
- to the segregation of metalloid impurities to grain boundaries (which can induce intergranular fracture),
- to the pinning of dislocations by interstitial impurities (which inhibit plasticity) to the formation of brittle long-range ordered phases.

Material type specific susceptibility to thermal embrittlement:

- **CASSs**: These steels are susceptible. The screening criteria for susceptibility adopted by the U.S. NRC are shown in Table B.2-2-1 in the following. The demarcation criterion between potentially significant and non-significant thermal embrittlement for the screening criteria in Table B.2.2-1 is based on ductile fracture tearing resistance of 255 kJ/m² [3].
- **Nickel-base alloys**: Some of these materials are susceptible. A specific form of thermal embrittlement that can arise in nickel-base alloys containing around 30 % chromium, such as Alloys 690, 152 and 52, can occur as a result of long-range ordering by the formation of the inter-metallic compound Ni₂Cr [133]. This can be avoided if the iron content of the material is > 7 % [134].
- **Weld metals and some chromium rich martensitic steels**: These materials are to lesser extent susceptible [1,3,7].
- **Ferritic and low-alloy steels**: These materials are to some extent susceptible [3].

Table B.2.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to thermal embrittlement.
Table B.2.2-1. Summary on susceptibility of materials of BWR RPV and its internals to thermal embrittlement [1,3,110,135]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>thermal ageing is not a significant degradation mechanism for RPV or any of its internals.</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

Table B.2.2-2. Screening criteria for thermal embrittlement of CASSs by U.S. NRC [135].

<table>
<thead>
<tr>
<th>Mo Content (Wt. %)</th>
<th>Casting method</th>
<th>Ferrite Content</th>
<th>Significance of Thermal Ageing</th>
</tr>
</thead>
<tbody>
<tr>
<td>High (2.0 - 3.0)</td>
<td>Static</td>
<td>&gt; 14 %</td>
<td>Potentially susceptible</td>
</tr>
<tr>
<td></td>
<td></td>
<td>≤ 14 %</td>
<td>Not susceptible</td>
</tr>
<tr>
<td></td>
<td>Centrifugal</td>
<td>&gt; 20 %</td>
<td>Potentially susceptible</td>
</tr>
<tr>
<td></td>
<td></td>
<td>≤ 20 %</td>
<td>Not susceptible</td>
</tr>
<tr>
<td>Low (0.50 max.)</td>
<td>Static</td>
<td>&gt; 20 %</td>
<td>Potentially susceptible</td>
</tr>
<tr>
<td></td>
<td></td>
<td>≤ 20 %</td>
<td>Not susceptible</td>
</tr>
<tr>
<td></td>
<td>Centrifugal</td>
<td>All</td>
<td>Not susceptible</td>
</tr>
</tbody>
</table>

B.2.3 Mitigation of thermal embrittlement

Thermal annealing is the only way to recover the toughness properties of a material [110]. This technique is described in more detail in Appendix B.1.3. This technique is used for improvement of material properties degraded by irradiation embrittlement, which are mostly the same material properties that are degraded by thermal embrittlement.

In PWR and VVER plants, the core barrel acts as a thermal shield, protecting the RPV from thermal embrittlement. This is more a beneficial side effect, as the main purpose of the core barrel is to shield the RPV from irradiation and peaks of thermal load transients. As mentioned in Appendix B.1.3, this far no core barrels have been installed to BWR RPVs.

B.2.4 Inspection and monitoring of thermal embrittlement

Unlike in case of irradiation embrittlement, there are less surveillance programs in BWR plants to monitor the propagation of thermal embrittlement. However, this monitoring is done in several PWR and VVER plants. An example of the latter type is the VVERs in Loviisa, Finland.

Degradation by thermal embrittlement cannot be detected by ISI techniques, which are for detection of cracks. Even though thermal embrittlement reduces the margin of a material to resist crack propagation due to other degradation mechanisms, such as fatigue or SCC, the applicable ISI techniques are left to be described in connection to them.
B.3 Fatigue

Fatigue is defined as the structural deterioration that occurs as a result of repeated stress/strain cycles caused by fluctuating mechanical loads and temperatures or vibration loads. After repeated cyclic loading microstructural damage can accumulate, leading to macroscopic crack initiation at the most highly affected locations. Subsequent continued cyclic loading can lead to growth of an initiated crack [1].

B.3.1 Computational modelling of fatigue

There are several approaches for computational modelling of fatigue. Since the introduction of damage accumulation concept by Palmgren [136] and linear damage rule by Miner [137], both published several decades ago, a multitude of cumulative fatigue damage models have been developed. These models can be divided into six categories [138]:

- linear damage rules,
- non-linear damage curves and two-stage linearisation approaches,
- life curve modification methods,
- approaches based on crack growth concepts,
- continuum damage mechanics models, and
- energy based theories.

In the top level the fatigue degradation models can be divided into:

- procedures that model fatigue until a macroscopic flaw has initiated,
- procedures that model the growth of an initiated flaw.

B.3.2 Fatigue crack initiation models

Of the procedures that model fatigue until initiation of a macroscopic flaw, the linear damage rules are most commonly used. This approach is described in more detail in the following.

The Palmgren-Miner rule (or Miner’s rule) is a linear damage accumulation rule used to predict the number of load cycles to failure under variable amplitude loading. The Palmgren-Miner rule asserts that the damage fraction, $\Delta_i$, at any stress level, $S_i$, is linearly proportional to the ratio of $n_i$, the number of load cycles of operation under this stress amplitude to $N_i$, the total number of load cycles that would produce a failure at that stress level. The damage fraction is computed as follows [137]:

$$\Delta_i = \frac{n_i}{N_i}$$  \hspace{1cm} (B.3.2-1)

where $n_i \leq N_i$. If the stress amplitude is changed, a new partial damage is calculated for this new amplitude level, where the corresponding $N_i$ value is found from an applicable S-N curve. These are commonly called fatigue end-of-life curves. The total accumulated damage, $D$, is then given by [137]:

$$D = \sum_i \Delta_i = \sum_i \frac{n_i}{N_i}$$  \hspace{1cm} (B.3.2-2)

and failure is assumed to occur when $D \geq 1$. Here failure is a quite strong expression as the criterion corresponds to initiation of a macroscopic flaw. However, in many cases most of the
time in operation for a component under fatigue is spent in the phase before the flaw initiation. The parameter $D$ is commonly called cumulative usage factor (CUF). The main drawbacks of the linear damage rule are its independence of load level and load sequence as well as lack of taking into account the interaction of the loads.

The fatigue end-of-life curves are used together with the Miner’s rule. These curves indicate how many stress cycles it takes to initiate fatigue cracks in components and grow them to a macroscopic size. These curves are material type specific and indicate the maximum allowable number of stress cycles for applied cyclic stress amplitudes. Design curves for RPV materials are given in ASME Section III, Appendix I [139] and in national standards, such as KTA 3204 [140]. The ASME III fatigue end-of-life curves [139] are entirely based on data obtained from tests in air, mainly at room temperature. These curves were developed by applying a factor of 2 on stress or 20 on number of stress cycles, whichever is lower, to the mean failure curve for small polished specimens. For less than 10000 cycles the factor 20 on cycles gives the lower curve. These factors are intended to account for size effects, surface finish, statistical scatter of the data, and differences between laboratory and industry environments, but do not include the effects of LWR coolant and temperature. The application of this approach can lead to overly conservative fatigue life assessment results. For austenitic stainless steels and certain nickel-based alloys used e.g. in RPV internals the latest update of ASME fatigue end-of-life curve is presented in Addendum 2009b of Section III [147], see Figure B.3.2-1.

The transferability of the above mentioned fatigue test results to NPP conditions is limited. This is because:

- uniaxial test loading differs a lot from the actual loading conditions in the NPPs,
- tests done in air do not take into account the effect of the NPP environment,
- polished test specimen surfaces deviate a lot from the surfaces of actual components.

![ASME fatigue end-of-life curve for austenitic stainless steels and certain nickel-base alloys, according to Addendum 2009b of ref. [147].](image_url)
In the U.S., the main procedure to take into account the effect of NPP environment to the linear damage accumulation is presented in report NUREG/CR-6909 [141]. It provides an environmental fatigue correction factor (F_{en}) methodology that is considered acceptable for incorporating the effects of reactor coolant environments on fatigue usage factor evaluations of metal components for new reactor construction. The methodology reflects the earlier development on the subject carried out in Japan. Report [141] describes the environmental fatigue correction factor methodology of fatigue assessment for the four major material types, those being carbon steels, low-alloy steels, wrought and cast austenitic stainless steels, and Ni-Cr-Fe alloys, respectively. An update of report [141] was published in 2014, which is NUREG/CR-6909, Rev. 1 [144]. However, its status was a draft report for comments. ASME has also published Code Case N-792 [145] containing a F_{en} procedure, which is almost the same as that in report [141]. The final edition of the NUREG/CR-6909, Rev. 1 was published in March 2018, see ref. [146].

The effects of the reactor coolant environment on the fatigue life of structural materials are expressed in terms of a nominal environmental fatigue correction factor, F_{en,nom} [-], which is defined as the ratio of fatigue life number of load cycles in air at room temperature, N_{air,RT}, to that in water at the service temperature, N_{water}, as follows [141]:

\[
F_{en,nom} = \frac{N_{air,RT}}{N_{water}}
\]  

(B.3.2-3)

The necessary input data for this fatigue evaluation procedure includes partial fatigue usage factors U_1, U_2, U_3, ..., U_n, as determined in ASME Section III [147] for Class I fatigue assessments. To incorporate environmental effects into the ASME Section III fatigue evaluation, the partial fatigue usage factors for a specific stress cycle or load set pair are multiplied by the environmental fatigue correction factor to obtain the environmental partial fatigue usage factor U_{en,i} [-] as follows:

\[
U_{en,i} = U_i F_{en,i}
\]  

(B.3.2-4)

where the fatigue usage factor values are determined according to the current code fatigue design curves, as given e.g. in Appendix I [139] of ASME Section III. The cumulative fatigue usage factor, U_{en} [-], considering the effects of reactor coolant environments is calculated as:

\[
U_{en} = U_1 F_{en,1} + U_2 F_{en,2} + U_3 F_{en,3} + ... + U_i F_{en,i} + ... + U_n F_{en,n}
\]  

(B.3.2-5)

where F_{en,i} [-] is the nominal environmental fatigue correction factor for the "i"th stress cycle, see Paragraph NB-3200 of ref. [147], or for a load set pair, see Paragraph NB-3600 of ref. [147]. Because environmental effects on fatigue life occur primarily during the tensile part of a loading cycle (i.e. rising ramp with increasing strain or stress), this calculation is performed only for the tensile stress producing portion of the stress cycle constituting a load pair. The necessary input data to calculate F_{en} for each stress cycle or load set pair include strain rate, temperature, dissolved oxygen in water and, for carbon and low-alloy steels, sulphur content.

However, the description of the environmental fatigue analysis methodology appears insufficient for computing the strains and consequent F_{en} values for actual NPP piping components concerning typical load transients with time dependently varying loads. Namely, the partial usage factors U_i are computed according to the Paragraphs NB-3200 and NB-3600 of ref. [147] for load cycles as defined therein, and the correspondence of these load cycle
definitions and the rising ramps with increasing strain or stress considered by the modified rate approach is unclear for the actual NPP environments. Moreover, the choice of sign for the cycle specific stress intensities computed according to the Paragraph NB-3200 of ref. [147] is not unambiguous, and neither is the choice of the associated strain component. Other gaps concerning the application of the Fen approach in practise exist as well. The main shortcoming of the NUREG/CR-6909 [141] Fen approach appears to be its uniaxial nature, which remarkably limits its feasibility to actual plant conditions, where the stresses/strains experienced by components are three dimensional.

Most of the drawbacks described above are partly or fully overcome in the NB-3650M Fatigue Calculation Procedure, as developed by TVO and FEMdata [148]. This procedure takes into account e.g. three dimensional stress state, real strains, and time dependency in the application of the rainflow load cycle computation procedure.

A similar Fen approach as that presented in report [141] has also been adopted in Japan. It is given in the JSME codes for nuclear power generation facilities [142,143]. In Germany, the Fen approach according to report [141] can be applied as such when the threshold of attention value has been reached or exceeded. This value was determined in 2013 edition of Safety standard KTA 3201.2 [66] as CUF of 0.4 for both austenitic and ferritic materials of both BWR and PWR piping components. In 2013 edition of KTA 3201.2 [66], also new design fatigue curves were provided for austenitic stainless steel grades 1.4541 and 1.4550, which steels are widely used in German NPP piping components.

VTT has also provided development in assessing the effect of LWR environment to fatigue. Solin et al. [149] have developed a Fen model that attempts to take better into account the transferability of laboratory results to real components. This model substitutes Fen with a factor for real effects of LWR environment, F_{real} [-], as follows:

\[
F_{real} = F_{en,T} \times F_{en,water} \times F_{transferability}
\]  

(B.3.2-6)

where \( F_{en,T} [-] = N_{RT,air}/N_{T,air} \), \( F_{en,water} [-] = N_{T,air}/N_{T,water} \), and \( F_{transferability} [-] \) summarizes the factors needed to transfer the laboratory data into plant conditions, such as the time between relevant loading transients, the transient itself and the frequency of the loadings. Sub-indexes \( RT, T \) and water correspond to room temperature, operational plant temperature and LWR water environment.

Note, that this far the environmental fatigue correction factor methodology has been applied only to NPP piping. It is foreseen that it is also applicable to BWR RPV internals.

**B.3.3 Fatigue crack propagation models**

Most of the computational models for fatigue induced crack growth apply stress intensity factor, \( K \) [MPa\(\sqrt{m}\)], which is a loading parameter from fracture mechanics. Most often crack opening mode is considered, i.e. mode I, in which case \( K \) is denoted as \( K_I \). For a more detailed description on \( K \) and fracture mechanics, see Section 8.1.2. One of the earliest and most often used fatigue crack propagation models is the Paris-Erdogan equation [150]. It is an empirical equation that relates the cyclic crack growth rate to \( K_I \) range \( \Delta K_I \). It can be written as follows:
\[
\frac{da}{dN} = C_{FA} \left[ \frac{\Delta K_I}{\Delta K_{I,adj}} \right]^{m_{FA}}
\]  
(B.3.3-1)

where \(a\) [mm] is crack depth, \(N\) [-] is the number of load cycles, \(\Delta K_I\) [MPa\(\sqrt{m}\)] is \(K_I\) range, i.e. \(K_{I,\text{max}}-K_{I,\text{min}}\), \(\Delta K_{I,adj} = 1.0\ \text{MPa}\sqrt{m}\), whereas \(C_{FA}\) [mm/(load cycle)] and \(m_{FA}\) [-] are material, temperature and environment specific constants. The values for both of these constants are determined as based on experimental data.

The Paris-Erdogan equation is limited by the following assumptions: the crack growth depends only on \(\Delta K_I\), the stress amplitude is constant and small enough for linear-elastic fracture mechanics to be applicable, and that the crack growth rate is independent of the previous load history.

Failure occurs when the maximum value of \(K_I\) exceeds the fracture toughness or some predetermined critical crack depth is reached. Thus, the Paris-Erdogan equation describes crack growth at intermediate values of fatigue crack growth curve, see Figure B.3-1 below. Therein, region II represents the intermediate crack propagation region where the size of the plastic zone ahead of the crack tip is large compared to the mean grain size, but much smaller than the crack length. Region I contains the stress intensity factor range threshold, below which fatigue cracks do not propagate, and region III is characterised by rapid and unstable crack growth just prior to the failure [151]. However, in case of large scale yielding Paris-Erdogan equation can remain valid. This is due to extensive isotropic hardening. This was demonstrated experimentally by Ljustell [152].

The fatigue damage propagation model proposed by Forman [152] covers both the intermediate and unstable \(\Delta K_I\) regions. This model can be written as:

\[\log \left( \frac{da}{dN} \right) = m \log (\Delta K_I) - \log(\Delta K_{I,th}) \]

\(\Delta K_{I,th}\) and \(K_{IC}\) represent the threshold and fracture toughness, respectively.

**Figure B.3-1. Typical fatigue crack growth behaviour in metals.**

The fatigue damage propagation model proposed by Forman [152] covers both the intermediate and unstable \(\Delta K_I\) regions. This model can be written as:
\[
\frac{da}{dN} = \frac{C_F (\Delta K_I / \Delta K_{I,adj})^{m_p}}{(1-R)(K_C / \Delta K_{I,adj} - \Delta K_I / \Delta K_{I,adj})} = \frac{C_F (\Delta K_I / \Delta K_{I,adj})^{m_p}}{(1-R)(K_C / \Delta K_{I,adj} - K_{\text{max}} / \Delta K_{I,adj})}
\]  

(B.3.3-2)

where \(C_F\) [mm/cycle] and \(m_p\) [-] are material and environment specific constants, \(K_C\) [MPa \(\sqrt{m}\)] is the fracture toughness and \(R = K_{\text{min}}/K_{\text{max}}\) is stress ratio. The values for \(C_F\) and \(m_p\) need to be determined experimentally. Equation (B.3.3-2) indicates that as \(K_{\text{max}}\) approaches \(K_C\), crack growth rate \(da/dN\) in turn tends to infinity. Here \(K_C\) depends mainly on the elastic \(K_I\) part of the limit load of the test specimen. Thus, \(K_C\) does not well correspond to actual fracture toughness. With equation (B.3.3-2) it is possible to predict both stable intermediate crack growth and accelerated crack growth rate for various stress ratios.

Other significant approaches based on the crack growth approach include:

- McEvily [154] developed an equation that can be fit to the entire fatigue crack growth curve.
- According to the model by Elber [155] a crack only propagates while its flanks are separated, and is then driven by the effective stress intensity factor range, \(\Delta K_{\text{eff}}\).
- Wheeler [156] developed a model based on the yield zone concept for tensile overloads.
- Johnson [157] has developed a systematic modelling technique for variable amplitude loading to account for interaction effects. The procedure includes a multi-parameter yield zone model, which accounts for crack growth retardation, acceleration and underload effects by decreasing or increasing the stress ratio.
- A more advanced development is the model by Forman and Mettu [158], which follows a cycle-by-cycle integration method as using the sigmoidal crack growth rate relationship. This model is now also included in the European Fitness For Service Network (FITNET) fitness-for-service procedure [159].

The multi-axial fatigue degradation models proposed in the literature may be categorised into three groups: stress based, strain based and energy based methods [161,162,163]. In their comprehensive review on the subject, Marquis and Socie [164] consider methods based on cyclic \(J\)-integral as a separate group from the energy based methods. For plastic conditions, \(J\)-integral is the applicable fracture mechanics parameter to describe the growth potential of a crack. For long life fatigue problems most of the multi-axial fatigue criteria are stress based. In order to handle non-proportional loading effects on fatigue resistance, many new methodologies have been developed. They are based on various concepts, such as the critical plane approach, integral approach and mesoscopic scale approach [160].

Of fatigue crack growth rate equations for metallic NPP component materials, data for the material and environment specific constants are most available for Paris-Erdogan equation (B.3.3-1). Due to this it is the most applicable equation, regardless of its limited scope. The more recent and advanced fatigue crack growth rate equations are more accurate, but it is often difficult to find applicable data for their material and environment specific constants.

### B.3.4 Susceptibility to fatigue

The primary degradation mechanism affecting BWR RPVs is fatigue. Three of the highest ranking degradation sites have been eliminated in the newer plants by design changes, and they have been repaired in most of the older plants. These include austenitic stainless steel safe-ends that have been sensitized by post-weld heat treatment, crevice areas where thermal sleeves are attached to Inconel or stainless steel safe-ends, and austenitic cladding on the inside corners and surrounding regions of the nozzles. Another important potential degradation site is the outer end of the nozzle forgings and the attached safe-ends. The most severely affected nozzle of this type is the feedwater nozzle [165].
Material type specific susceptibility to fatigue:
- All metallic material types are susceptible to fatigue. However, this varies between the material types.

Table B.3.4-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to fatigue. Figure B.3.4-1 presents fatigue threshold data for commonly used metal alloys as a function of stress ratio, $R$. One should be cautious when applying fatigue threshold data, as that very much depends on is crack closure considered or not.

Table B.3.4-1. Summary on susceptibility of materials of BWR RPV and its internals to fatigue [1,3,165,172]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>• for cumulative fatigue, damage is assumed to occur when: CUF ≥ 1,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>• for fatigue crack propagation, $\Delta K_{th}$ values for metal</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>alloys as a function of stress ratio, $R = K_{i,min}/K_{i,max}$,</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>vary between 2 to 12 MPa$\cdot$m, see Figure B3.4-1,</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>• for fatigue crack propagation, according to [172]:</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>$\Delta K_{th} = 5.5$ for $R &lt; 0$,</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>$\Delta K_{th} = 5.5(1-0.8R)$ for $0 \leq R &lt; 1.0$</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
</tr>
</tbody>
</table>

Figure B.3.4-1a. The first set of the fatigue threshold data for some commonly used metal alloys as a function of stress ratio, from ref. [166].
Figure B.3.4-1b. The second set of the fatigue threshold data for some commonly used metal alloys as a function of stress ratio, from ref. [167].

B.3.5 Mitigation of fatigue

Mitigation of fatigue damage for existing components is carried out by reducing the magnitude of the applied loads or thermal conditions or by reducing the number of load cycles [3,168]. For thermal transients, reduction in the rate of temperature change for extreme temperature cycles can be effective [168]. In rare instances, selection of a more fatigue resistant material can be beneficial [3]. However, the normal operating cycles are not generally the source of significant fatigue damage in NPPs.

The observed fatigue cracking in service has mostly been due to high-cycle fatigue as a result of conditions not anticipated at the time of original plant design [168], or due to the incorrect quantification of the stress/strain/frequency conditions for a specific component under various operational modes [3]. Some cases of low-cycle fatigue cracking (with a significant environmental contribution) have also been reported [169]. Changes in operating conditions can also introduce new, unanticipated fatigue loads, as has occurred subsequent to power uprates [3].

Component replacement is also one way to mitigate fatigue degradation. It is necessary when repair is not feasible or possible. For BWR RPV internals, replacing is a viable approach, as most of these components are not welded to their positions. Instead, they lie tightly on top of each other, and the support/connection between them is established by mass gravity and pre-tension produced with RPV-head and its spring beams.

B.3.6 Inspection and monitoring of fatigue

The RPV and its internals are inspected in accordance with ASME Section XI [81], or according to corresponding national standards as applied in other countries, such as KTA 3204 [140] in Germany and YVL Guides in Finland. While monitoring is not a requirement in all countries, at least most NPPs use monitoring techniques.

Non-destructive testing (NDT) of structural NPP components is required by the regulatory agencies, as according to identified codes and standards. NDT is carried out with special equipment, applying e.g. the ultrasonic technique. Visual examinations are done too. Their objective is to discover relevant conditions including distortion, cracking, loose or missing parts, wear or/and corrosion. Underwater television camera is an applicable visual examination tool coupled with photographic capabilities, enlargement and a permanent record. Further enhancement is available with an underwater conveyance system.

NDT with ultrasonic technique (UT) is used for the evaluation of components where detection of flaws, such as cracks, is necessary [1]. UTs, such as the cylindrically guided wave technique, can be used to detect flaws in bolts and threaded rods using transducers which emit ultrasonic sound waves that travel through solids and liquids at different velocities. UT is also applicable for detecting flaws in RPV internals. UT equipment must be qualified for specific geometrical configurations of NPP components. Typical UT examination sites are component welds, for the RPV most importantly main welds and nozzle to safe-end welds.

In addition to UT and visual examination, other inspection techniques applicable to detect fatigue induced cracks include [171]:

- Eddy Current technique, which applies alternating currents through a coil and a conductor, creating a magnetic field. Any surface discontinuity alters the field and can be detected with a secondary coil in the Eddy Current probe. Flaw size is indicated by the extent of the response change as the probe senses the object being examined.
- Liquid penetrant, which uses the capillary effect by which the liquid is absorbed by the surface defect, is applicable to detect cracks.

If any defect or degradation mechanism is observed by inspection or monitoring it should be assessed according to applicable national codes and standards.

The RPVs in the U.S. are inspected in accordance with Section XI of the ASME Code [81]. This is also globally the most commonly applied standard for operation and ISI of NPP components. There are three types of examinations used during in-service inspection: visual, surface and volumetric. The inspections are also included in the pre-service inspection (PSI) that is required for the RPVs. Each NPP follows a pre-service and in-service inspection program based on selected intervals throughout the design life of the plant. The RPV inspection category is described in Table IWB 2500-1 of ASME Section XI [81], which details the inspection requirements. Extensive manufacturing and pre-service inspections are required for the RPVs. In addition, ASME Section XI [81] requires four inspection intervals during the first 40 years of the plant life. The first inspection will occur from 3 to 10 calendar years after the initial plant start-up. The required RPV inspections include:

- volumetric inspection of all pressure-retaining welds (i.e. shell, head, head to flange, shell to flange, and repair welds),
- volumetric inspection of all full-penetration nozzle welds (i.e. nozzle to vessel welds),
• visual (VT-2) inspection of the external surface of 25% of the pressure-retaining partial penetration welds in the vessel (i.e. control rod drive mechanism housing and instrumentation tubes),
• volumetric and surface inspection of all pressure-retaining dissimilar metal welds in vessel nozzles (i.e. nozzle to safe-end butt welds),
• volumetric and surface inspection of all pressure-retaining bolting greater than two inches in diameter (i.e. closure studs).

The inspection technique used most often is UT. The inspection method and techniques have to be chosen to detect all safety relevant flaws in the planes perpendicular to the main stresses, the planes parallel to the fusion lines of the welds and the planes perpendicular to the welds.

For a wall thickness of equal to or more than 100 mm, additional inspection with techniques for detecting planar flaws perpendicular to the surface will be performed [81].

Examination categories for the BWR RPV internals are defined in Table IWB-2500-1 of Section XI [81]. Therein, it is stated that:
• accessible welds in integrally welded core support structures must be visually inspected,
• accessible surfaces of removable core support structures and accessible welds must be visually inspected.

The acceptance standard for the core support structure is provided in Paragraph IWB 3520.2 of Section XI [81]. Therein, it is stated that the welds in control rod drive (CRD) housings can have a volumetric or surface examination. The acceptance standard for the CRD housing is provided in Paragraph IWB-3523 of Section XI [81].

ASME Code and other program inspections are valid and effective for verifying the structural integrity of the NPP components. These inspections are performed unless an exemption is authorized by the appropriate regulator on a plant specific basis.

Other internal components not covered by ASME Code Section XI requirements are addressed by various national regulatory requirements and GE Service Information Letters (SILs).

In Finland, in-service inspection (ISI) requirements are specified in YVL Guide E.5 [98]. Additional inspections are performed if technical reasons necessitate or service experiences indicate some reason to increased frequency of inspection.

While monitoring techniques/systems cannot detect material degradation of RPV internals they are useful tools for providing information on behaviour of internals during NPP operation. The following monitoring techniques are recommended for use during NPP operation:
• loose parts monitoring,
• neutron noise monitoring,
• direct vibration monitoring, and
• on-line primary water chemistry monitoring.

If the monitoring systems indicate that there is a loose part in the RPV or that the fuel or reactor internals are vibrating, the information/data should be diagnosed. In the case of a loose part, the size or weight and the location in the primary coolant system can be determined and a decision as to plant shutdown could be made based on safety and/or economic consideration. If in case of neutron noise or direct vibration monitoring there is an indication that either the fuel or a RPV internal is vibrating, the information/data should be diagnosed in accordance with an
applicable code/standard. Based upon the diagnosis of the information/data from the vibration monitoring, a decision can be made to NPP shutdown or continue operating until the next outage.

If the on-line chemistry monitoring system detects that the primary coolant does not meet the specifications, the source of the ingress of the impurities should be identified and corrective actions taken. If the amount of halogens exceed the specifications a cleaning or flushing operation will be required during the next outage.

B.4 SCC

SCC is the term given to crack initiation and sub-critical crack growth of susceptible steels and alloys under the influence of tensile stress and a corrosive environment. SCC is a complex phenomenon driven by the synergistic interaction of mechanical, electro-chemical and metallurgical factors. BWR RPV internals are potentially susceptible to two predominant forms of SCC, which are [1]:
- intergranular stress corrosion cracking (IGSCC), and
- irradiation assisted stress corrosion cracking (IASCC).

B.4.1 Computational modelling of IGSCC

There are basically three approaches to assess the propagation of SCC [3]:
1. Experimental and probabilistic analyses of past plant failures, which can lead to the definition of risk based inspection priorities and to the prediction of future frequencies of failure.
2. Analyses of laboratory data in order to develop empirical life prediction algorithms between the degradation rate and the interacting effects of various system parameters.
3. The development of mechanisms based SCC assessment algorithms in conjunction with good quality laboratory degradation rate data.

These three approaches have recently been reviewed, see ref. [173].

Some notable fracture mechanics based examples of the SCC assessment models are described in the following. They are adjusted to IGSCC analyses by using applicable material, temperature and environment specific constants. In the fracture mechanics based treatments for SCC, the variable of interest, being the already mentioned $K_I$, is measured against time. An illustration of SCC propagation rate as a function of $K_I$ for metallic materials is shown in Figure B.4.1-1.

The commonly used equation for computation of SCC rate [174] at intermediate stage 2 can be written as:

$$\frac{da}{dt} = C_{SCC} \left( \frac{K_I}{K_{I,adj}} \right)^{n_{SCC}}$$

(B.4.1-1)

where $a$ [mm] is again crack depth and $t$ [year] is time and $K_{I,adj} = 1.0$ MPa$\sqrt{m}$, whereas $C_{SCC}$ [mm/year] and $n_{SCC}$ [-] are material, temperature and environment specific constants. In most cases, the $C_{SCC}$ and $n_{SCC}$ values applicable for NPP components have been defined with upper
bound approach in relation to the underlying experimental data. This means that the model curve is positioned so that most or all data points are below it. This is a very conservative approach. The popularity of this SCC equation is based on its simplicity and availability of values for $C_{SCC}$ and $n_{SCC}$.

Here $K_{IC}$ depends mainly on the elastic $K_I$ part of the limit load of the test specimen. Thus, $K_{IC}$ does not well correspond to actual fracture toughness. A more advanced and in some applications useful modification of the stage 2 SCC rate equation that also takes into account $K$ threshold is the EPRI MRP-55 model [175,176]. It can be written as:

$$
\frac{da}{dt} = \exp \left[ - \frac{Q_g}{R} \left( \frac{T_{adj}}{T} - \frac{T_{adj}}{T_{ref}} \right) \right] \alpha \left( \frac{K_I - K_{I,th}}{K_{I,adj}} \right)^\beta
$$

(B.4.1-2)

where: $Q_g$ is thermal activation energy for crack growth = 130 kJ/mol, $R$ is universal gas constant = 8.314E-03 kJ/molK, $T$ is absolute operating temperature at crack location [K], $T_{ref}$ is absolute reference temperature used to normalise data = 598.15 K (325 °C), $\alpha$ is crack growth rate coefficient = 2.67E-12 m/s at 325 °C for $da/dt$ in units of m/s and $K_I$ in units of MPa$\cdot$m, $K_{I,th}$ is crack tip stress intensity factor [MPa$\cdot$m], $K_{I,adj}$ is crack tip stress intensity factor threshold = 9 MPa$\cdot$m, $\beta$ is dimensionless exponent = 1.16.

Equation (B.4.1-2) is mainly applicable to alloy Inconel 600, which material is used e.g. in the safe-ends of the BWR RPV nozzles [175,176]. Due to equation (B.4.1-2) being a laboratory
data based correlation the parts on the right side of the equality sign do not follow a rigorous unit treatment.

A similar approach to equation (B.4.1-2) has been developed for alloys 182 and 82, which are used e.g. for welding of BWR components of austenitic stainless steel. This equation is the EPRI MRP-115 model [177,178] and it can be written as:

$$\frac{da}{dt} = \exp\left[ - \frac{O_g}{R} \left( \frac{T_{adj}}{T} - \frac{T_{adj}}{T_{ref}} \right) \right] \alpha f_{alloy} f_{orientation} \left( \frac{K_f}{K_{f,adj}} \right)^{\beta}$$

(B.4.1-3)

where: 
- $f_{alloy} = 1.0$ for alloy 182 and 0.385 for alloy 82,
- $f_{orientation} = 1.0$, except for crack propagation that is clearly perpendicular to the dendrite solidification direction, where $f_{orientation} = 0.5$,

and otherwise the parameter explanations/values are the same as those for equation (B.4.1-2). Due to equation (B.4.1-3) being a laboratory data based correlation the parts on the right side of the equality sign do not follow a rigorous unit treatment.

A more recent predictive methodology for SCC propagation using a mechano-chemical model based on a slip formation/dissolution was presented by Saito and Kuniya [179]. This model consists of combined kinetics of the plastic deformation process as a mechanical factor and the slip dissolution-repassivation process as an environmental factor at the crack tip.

Of the SCC propagation equations for metallic NPP component materials, equation (B.4.1-1) is the most applicable one, because of its simplicity and the availability of the data for the material and environment specific constants. This equation has more recently been supported by the latest editions of the ASME Section XI [81], which now contain SCC propagation equations of this type for both BWR and PWR environments. For BWR environments the covered materials are:

- alloy 600 and associated weld material alloys 182 and 132, and
- austenitic stainless steel.

**B.4.2 Computational modelling of IASCC**

The computational modelling of IASCC resembles that of IGSCC as the applied equation is quite similar compared to the SCC rate equation (B.4.1-1). In this case, material, temperature and environment specific constants applicable to IASCC are used.

It has been more challenging to determine experimentally the values for the constants in IASCC rate equation than for those in SCC rate equation, because the test specimens have to be irradiated. Over the years, the available experimental IASCC growth rate data has gradually increased.

IASCC equations estimating crack growth rate (CGR) in BWR environments for austenitic stainless steels have been developed from about 2000 onwards by using then available experimental data, see e.g. refs. [180,181,182], but substantially more measured CGR data are available now. The most recent update of the IASCC equation for austenitic stainless steels is by Eason and Pathania [184]. As compared to the underlying experimental data this equation corresponds to 75th percentile of the measured CGRs. Even though the upper bound approach has been relaxed to some extent, to avoid excessive conservatism, their IASCC equation is still
conservative. In addition, also several of the earlier IASCC equations [180,181,182,183] have been developed using the 75th percentile approach.

Eason and Pathania [184] have developed IASCC equations for both normal water chemistry (NWC) and hydrogen water chemistry (HWC) conditions of BWRs under operational temperature of 288 °C. The equation for NWC conditions can be written as:

\[
\frac{da}{dt} = 2.84 \times 10^{-17} \left( \frac{\sigma_{0.2}}{\sigma_{\text{adj}}} \right)^{2.675} \left( \frac{K_I}{K_{I,\text{adj}}} \right)^{2.486} \times \left( \frac{\text{mm}}{\text{year}} \right)
\]

(B.4.2-1a)

and that for HWC conditions can be written as:

\[
\frac{da}{dt} = 1.35 \times 10^{-17} \left( \frac{\sigma_{0.2}}{\sigma_{\text{adj}}} \right)^{2.547} \left( \frac{K_I}{K_{I,\text{adj}}} \right)^{2.504} \times \left( \frac{\text{mm}}{\text{year}} \right)
\]

(B.4.2-1b)

where \( \sigma_{0.2} \) [MPa] is the yield strength of irradiated austenitic stainless steel, \( \sigma_{\text{adj}} = 1.0 \) MPa, and otherwise the variables and their dimensions are the same as for those in equation (B.4.1-1). The irradiated yield strength can be estimated from irradiation dose, material type, temperature, and cold work before irradiation (if any). The major uncertainty in application of all published IASCC equations is the apparent variability in CGR of different material heats under the same environment, dose, and loading conditions [184]. The causes of these large differences in CGR from heat to heat are not well understood. Part of the variability attributed to material susceptibility may arise from differences in experiments at various laboratories, because most heats have been tested at only one laboratory.

Of the IASCC propagation models for metallic NPP component materials, equation by Eason and Pathania [184] is arguably the most accurate one, because it is based on larger amount of underlying experimental data than the other models.

**B.4.3 Susceptibility to IGSCC**

Susceptibility to IGSCC varies with alloy composition and metallurgical condition. Given conditions of normal stress and BWR environment, several materials have shown susceptibility to IGSCC as a result of the material itself or due to its fabrication history. For RPV internals, these materials include austenitic stainless steel of type 304/316 as well as nickel-base alloys 600 and 182 [1]. Of these, the two former ones are base materials and the latter one is weld material.

Annealed and irradiated austenitic stainless steel becomes susceptible to IASCC when certain criteria are met or exceeded. Most often these criteria are threshold values for fluence as dependent on the magnitude of the prevailing stress level. Both stabilized and non-stabilized stainless steels appear to be equally susceptible to IASCC.

The Generic Letter 88-01 [185] and report NUREG 0313, Revision 2 [186] present the position of the U.S. NRC on austenitic stainless steel piping potentially susceptible to IGSCC in BWRs. NRC Generic Letter 88-01 [185] applies to all BWR piping made of austenitic stainless steel with nominal outer diameter of four inches (101.6 mm) or larger and containing reactor coolant at a temperature above 200 °F (93 °C). Thus, ref. [185] applies also to BWR RPV internals of
austenitic stainless steel. According to refs. [185,186] the following steels are susceptible to IGSCC, as depending on their carbon and ferrite content:

- austenitic stainless steel with carbon content exceeding 0.035 %,
- CASS with carbon content exceeding 0.035 % and ferrite content less than 7.5 %.

Susceptibility to IGSCC is affected by the following characteristics [1]:

- ECP: When the Electrochemical Corrosion Potential (ECP) of stainless steel and nickel-base alloys is lower than -230 mV, susceptibility to IGSCC is reduced. The primary method to lower the ECP below this threshold value is by HWC.
- Sensitization: When non-stabilized austenitic stainless steels containing greater than 0.02 wt% carbon are furnace or weld heated, chromium (Cr) carbide precipitation at the grain boundary creates an envelope of Cr depleted austenite that in certain environments is not resistant to corrosion. The Cr depleted zone is no longer a stainless steel, but rather a localized low alloy steel anode, galvanically coupled to a large area stainless steel cathode. IGSCC can occur when high enough tensile stress is applied to austenitic stainless steel that has become thus sensitized, if the environment can support the corrosion reaction. A similar Cr depletion sensitization phenomenon occurs also in nickel-base alloys.
- Cold work: Such cold work operations as bending, cutting, forming, rolling, hard machining and abusive grinding can make austenitic stainless steel susceptible to SCC in the BWR environment. The nature of the cold work, like the increase of surface hardness, affects the degree of the SCC susceptibility, and the combination of cold work followed by sensitization is synergistically damaging. Even non-sensitized materials like low carbon steel (less than 0.020 % of carbon) can become susceptible to IGSCC in the cold worked condition. SCC initiates in the cold worked layer and propagates beyond that.
- Tensile stresses: The propagation of SCC requires considerably high tensile stresses. There are three primary sources of tensile stresses for RPV internals: (1) fabrication induced stresses, (2) primary stresses, and (3) secondary stresses. Fabrication induced stresses arise from manufacturing and installation. These include fit-up and assembly in the shop or field as well as machining and welding. In addition to welding, also hard machining and abusive grinding can produce surface residual stresses near or above the material yield strength. Service induced primary stresses and secondary stresses for RPV and its internals during normal operation are generally lower than that. The weld residual stresses (WRRs) can be considerably reduced by a post-weld heat treatment (PWHT).

Table B.4.3-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to IGSCC.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
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<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
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<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td></td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td></td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
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</tbody>
</table>

Table B.4.3-1. A summary on susceptibility of materials of BWR RPV and its internals to IGSCC [1,175,176,185,186,187,188,189,190,191]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.
B.4.4 Susceptibility to IASCC

Susceptibility to IASCC is affected by the following characteristics:

- All susceptibility issues described in Appendix B.4.3 for IGSCC apply also to IASCC [1].
- Based on the available field and laboratory data, neutron fluence \((E > 1 \text{ MeV})\) threshold of approximately \(5.0 \times 10^{24} \text{ n/m}^2\) exists for annealed types 304, 304L, 347 and 348 stainless steel components under high tensile stress, and approximately \(2.0 \times 10^{25} \text{ n/m}^2\) for components under moderate or low tensile stress [192,193]. Welded and irradiated RPV internals, such as the core shroud, appear to have lower threshold fluence due to the presence and interaction of weld sensitization, high residual stresses and irradiation.
- A summary of IASCC in LWRs and associated metallurgical changes in austenitic stainless steels as a function of neutron irradiation in \(\text{n/cm}^2\) \((E > 1 \text{ MeV})\) and displacements per atom (dpa) is shown in Figure B.4.4-1 below.

![Figure B.4.4-1. A summary of IASCC in LWRs and associated metallurgical changes in austenitic stainless steels as a function of neutron irradiation in n/cm² \((E > 1 \text{ MeV})\) and displacements per atom (dpa), from ref. [28].](image)

Table B.4.4-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to IASCC.
Table B.4.4-1. Summary on susceptibility of materials of BWR RPV and its internals to IASCC [1,185,186,192,193]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

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</thead>
<tbody>
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<td>• austenitic stainless steels,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>• stabilised and non-stabilised stainless steels,</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>• nickel based alloys Inconel 600, weld metal 182,</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>• for type 304, 304L, 347 and 348 steels neutron fluence threshold is 5.0E+24 n/m² under high stress and 2.0E+25 n/m² under low stress [192,193],</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>• same material specific $K_{ih}$ data as for IGSCC, see Table B.4.3-1.</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td></td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td></td>
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<tr>
<td>low alloy RPV steels</td>
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</table>

B.4.5 Mitigation of SCC

Radiolysis of the coolant in the BWR core produces an aggressive oxidizing environment. The oxidant concentrations are a key factor in the initiation and propagation of IGSCC. A number of mitigation strategies have been developed against SCC in BWRs [194]. The most often applied technique is to reduce the oxidization ability of the coolant. Laboratory and reactor tests in the late 1970’s and early 1980’s showed that adding hydrogen to control the oxidant concentration and subsequent ECP is a feasible method to control IGSCC [1,117].

In the early 1990’s, General Electric (GE) introduced a program known as Optimum Water Chemistry to establish chemistry standards that would address the IGSCC concern and integrate the requirements to reduce radiation exposure to personnel and radio-active waste while protecting the BWR fuel. This program was later expanded into a Reactor Internals Management (RIM) program. As a result of the increased occurrence of core shroud cracking, in late 1994 the BWR plant owners formed the BWRVIP committee which is managed by EPRI, as mentioned in Appendix B.1. The BWRVIP is developing several approaches to improve inspections, monitoring, and repair efforts, and modification of reactor water chemistry to reduce the probability of cracking in BWR plants.

Report NUREG/CR-7153, Vol. 3 [170] presents a NPP component specific summary of mitigation actions and recommended guidelines against SCC degradation, based on experience concerning mainly/only U.S. BWR units up to 2014.

Techniques to mitigate SCC are described in more detail in the following.

Mitigation with water chemistry control

There are several methods to mitigate SCC by coolant chemistry control, including [1,117]:

- lowering ECP in the bulk water and locally in the crack opening by producing less acidic electrolytes, or
- improving the protecting effect of the oxide layer on the metal surface.

These controls may be implemented by applying the following actions [1,117]:

- decreasing the reactor water conductivity to extremely low values,
- increasing the pH-value, and
• adding conditioning agents, such as a corrosion inhibitor at a low concentration level, to the bulk water.

The two most common methods currently being used to mitigate IGSCC and IASCC in BWR internals through water chemistry control are HWC and Noble Metal Chemical Application (NMCA) [1,117]. Additional information on the experience concerning these mitigation methods and their effects on IGSCC and IASCC, radiation dose and fuel integrity is provided in BWR Water Chemistry Guidelines - 2000 Revision [195]. This Guideline is an industry consensus document and is updated periodically.

HWC is effective in reducing oxidant (oxygen and hydrogen peroxide) concentrations to low levels, below 2 ppb [1,117]. Subsequently, this condition results in an environment with ECP less than -230 mV (SHE). Laboratory and reactor tests have shown that initiation and propagation of IGSCC is mitigated when the ECP is below this value. The concentration of feedwater hydrogen required to mitigate IGSCC in BWR internals varies from 1 to 2 ppm.

NMCA involves injecting platinum and rhodium compounds into the reactor water during an outage. The noble metals deposit on the surfaces in contact with the water. Platinum and rhodium catalyze the recombination of hydrogen and oxygen produced by radiolysis in the core. This leads to a decrease in the local oxygen concentration at the surfaces and a reduction in ECP to values below -230 mV at feedwater hydrogen concentrations of 0.2-0.4 ppm, in contrast to 1-2 ppm required with HWC [1,117]. NMCA increases the effectiveness of hydrogen in mitigating and allows a reduction of radiation exposure to plant personnel. During operation there is a depletion of noble metal from the surfaces of the reactor internals and piping. Consequently, a re-application of NMCA is necessary every 3 to 5 years. Noble metal coatings can be applied underwater remotely using the plasma spray coating process. This application is particularly suitable for such components as the core shroud [1].

**Mitigation with surface treatment**

Peening techniques which introduce a compressive surface residual stress have been shown to be effective SCC mitigators. The following peening techniques have been developed [1,117]:

- laser peening (LP),
- water jet peening (WJP), and
- shot peening (SP).

The peening process introduces compressive stresses to the component surface layer by constraint of the surrounding material.

The LP process utilizes water-penetrable green light of a frequency-doubled Nd:YAG laser delivered with an optical fiber and generates high pressure plasma of several GPa on the surface [196,197,198]. The WJP process relies on the pressure derived from the cavitation collapse at the surface under the high pressure water jet [199,200,201]. The SP process utilizes spherical Type 304 stainless steel shots hardened during production process to have Vickers hardness of about 500, which are projected with highly pressurized water (approximately 1 MPa) to the surface [202,203].

The effectiveness of these processes to mitigate SCC of type 304 stainless steel have been demonstrated by laboratory testing. Compressive residual stress of several hundred MPa to
depth varying within 300-1000 μm is achieved. SCC susceptibility can be significantly reduced or eliminated by peening processes [1,117].

Solution annealing of type 304 stainless steel is a well established method for eliminating sensitization, thereby reducing susceptibility to SCC. This method has been extended to surface treatment to desensitize Type 304 steels by surface melting and solution annealing utilizing appropriate heat input controls [1,117].

**Mitigation with stress state improvement**

Induction heat stress improvement (IHSI) can improve the residual stress state at the inner surface of components by applying a temperature difference across the wall thickness by induction heating the outer surface and water cooling the inner surface. This far IHSI has been applied to type 304 stainless steel piping welds in many BWRs in Japan and in the USA. For application to type 304 steel the maximum temperature is limited to 550 °C to avoid thermal sensitization. In this case, after IHSI the stresses at the inner surface remain slightly tensile [28]. More recently, a modified IHSI was developed for type 316(NG) stainless steel piping. For modified IHSI the maximum temperature at the outer surface can be increased to 650 °C due to the high sensitization resistance of the base material, so that it can also improve the stress state to be fully compressive at the inner surface [28].

Mechanical stress improvement (MSIP) method is also widely used in the U.S. NPPs to mitigate SCC. MSIP modifies the existing inner diameter residual tensile stresses in the weld metal and HAZs of butt welds in piping components [204]. A load is applied to the outside surface with a large two-piece mechanical hydraulic clamping device connected by two pairs of tangentially positioned studs. MSIP results in compressive residual stresses in the interior region of the pipe weld to minimize the likelihood of initiation or propagation of primary water SCC (PWS CC) [204].

EPRI report TR-110701 [212] presents a summary of application of both IHSI and MSIP up to 1998.

**Repair welding**

There are several repair welding techniques that can be applied to NPP components.

The half bead technique is the traditional procedure in the U.S. for making weld repairs. The technique should produce an acceptable repair in a component without using PWHT. Alternatively, temper bead technique is a machined welding repair method that can be used without PWHT. This technique utilises machine gas-tungsten arc welding (GTAW) process which allows performing weld repairs remotely. It provides an alternative to the conventional half bead repair technique, which has been considered unsuitable for repair applications under high radiation exposure [205]. It has been accepted by ASME in 1992 and the technique has been used extensively. A controlled heat input, multi-pass welding procedure creates excellent grain refinement and tempering of the base metal HAZ without the necessity for grinding the first layer [206].
The main weld repair method used in the U.S. is overlay welding, which is applied on the outer surface of the component. The fully structural weld overlay (WOL) is thick enough to fully replace the original weld, while the optimised WOL is thinner. The WOL technique has been approved by the U.S. NRC as an applicable repair method for RPV nozzle safe-ends [207,208].

The U.S. industry has also developed an onlay welding technique, which is applied to the inner surface. With the onlay process no prior excavation of material is performed. During onlay welding only a small layer of 2 to 3 weld beads would be deposited on the inside surface of the component [209].

Inlay welding technique has been used mainly outside the U.S., e.g. in Sweden. Inlay welding includes machining of a groove and welding, usually using an improved filler material compared to Alloy 182, which has been found to be susceptible to SCC [210].

Other mitigation techniques

In some NPPs a repair has been made by spark eroding the defects and leaving the grooves as they are, or by performing local weld repairs. In this technique the defects are removed, and approval to continue operation without further actions is applied as based on results from structural integrity calculations [211].

Component replacement is also a viable mitigation technique for BWR RPV internals. This approach is described in more detail in Appendix B.3.5.

B.4.6 Inspection and monitoring of SCC

As mentioned earlier in Appendix B.3.6 the RPV and its internals are inspected in accordance with ASME Section XI [81], or according to corresponding national standards, as applied in each country. All NPPs use also monitoring techniques.

As the inspection requirements for BWR RPV and its internals are already described in detail in Appendix B.3.6, they are not repeated here. Those requirements cover detection of flaws caused by all anticipated/experienced degradation mechanisms. The most often used inspection technique of BWR RPV and its internals is UT. It can be applied in several ways. Manufacturers nowadays provide and develop several types of UT detection units incorporating different wave modes, angles and configurations, all designed to enhance sensitivity to crack detection [213]. The UT applications are capable for detecting cracks in both base material and welds.

The main UT applications are summarised in the following:

- **Shear wave techniques**: These techniques apply best for corner reflection detection, the best possible angle is 45° to find defects in inner surface [214].
- **Longitudinal wave techniques**: The beam distortion is less compared to the shear waves and this technique can be used in Inconel metal weld inspection [215].
- **Mode-conversion techniques**: The probe sends direct longitudinal waves at a 70° angle and at the same time direct shear waves at about 30° angle [216].
- **Synthetic aperture focusing technique (SAFT)**: This technique is applicable to a wide range of ferritic and austenitic materials and is used for testing of pipes, turbines, plates, vessels and pump housings [213].
• Acoustic holography: The highly resolved austenitic and Inconel weld ultrasonic images appear after the holographic treatment of A-scans. The technique is used for flaw sizing and weld quality estimation [217,218].

• Phased array techniques: The acoustic beam is steered by using electronic delays between different elements of the probe. These delays are used both in transmission and reception modes. Phased array is used for detection and sizing of both circumferential and axial defects [219,220].

In addition to UT and visual examination, other inspection techniques applicable to detect SCC induced cracks include:

• Eddy Current technique, which is described in more detail in Appendix B.3.6.

• Liquid penetrant, which is described in more detail in Appendix B.3.6.

• Acoustic emission inspection, which applies small amplitude stress waves that are emitted by kinetic energy as the material is locally strained beyond its elastic limit. Piezo-electric transducers on component surface detect stress waves as small displacements. In addition to SCC this technique is also applicable for detecting hydrogen cracking [171].

B.5 General corrosion

General corrosion is characterized by uniform surface loss due to material oxidation. In aqueous solutions including reactor coolants, it is primarily an electro-chemical process involving an anodic oxidation reaction to form positively charged metal cations, which is balanced electrically by a cathodic reaction involving the reduction of oxygen in solution (if present) and/or reduction of hydrogen ions to molecular hydrogen gas [3].

B.5.1 Computational modelling of general corrosion

Several procedures using electro-chemical and thermo-dynamic models exist for computation of general corrosion rates in both carbon steels and austenitic stainless steels, see e.g. refs. [222,223]. However, such models are not suitable for mechanical analyses of e.g. wall thinning due to corrosion as the computed results mainly concern corrosion potential parameters, such as current density at the corrosion site. However, some models for general corrosion exist that are applicable to structural integrity analyses. Relevant ones of these are presented in the following.

The straightforward equation for assessment of general corrosion propagation depth, \( x \) [\( \mu m \)], can be written as [224]:

\[
x(t) = C \left( \frac{t}{t_{adj}} \right)^n
\]

(B.5.1-1)

where \( C \) [\( \mu m \)] is effective corrosion rate in the absence of a coating, \( t \) [year] is time since the start of the corrosion process, \( t_{adj} \) is 1.0 years and \( n \) [-] is power rule exponent for non-linear behaviour. \( C \) and \( n \) are experimentally determined material and environment specific constants. The more advanced empirical model for corrosion depth, \( x \) [\( \mu m \)], in steel under general corrosion can be written as [225]:

241
\[ x(t) = C \left( \frac{t - t_i}{t_{adj}} \right)\alpha \]  

(B.5-1-2)

where \( t \) [year] is time, \( t_i \) [year] is induction or initiation time required to activate the process, while \( C \) [\mu m] is rate parameter and \( \alpha \) [-] is time-order parameter. The values for parameters \( C \) and \( \alpha \) are determined from experimental data, supplemented by knowledge of the physics of the underlying mechanism of attack. \( C \) and \( \alpha \) data for various steel types and environments are given in ref. [225].

Two general methods are recognized for estimating atmospheric corrosion resistance of low alloy steels [226] from test data. The first utilizes linear regression analysis of short-term data to predict long-term performance by extrapolation. The second determines a corrosion resistance index based on chemical composition of the steel. The regression analysis presumes a log-linear relation between material loss and time, leading to equation (B.5.1-2). The approach to extrapolate beyond the scope of the measured data leads to unknown decrease in accuracy. Thus, this far no accurate assessment has been provided for long-term extrapolation or similitude of accelerated ageing testing [225].

The applicability of empirically defined models for general corrosion depends on the available measurement data. Once all available data for the analysis case has been collected, based on it one can choose an applicable model for general corrosion. It is difficult to identify the most recommendable model. The accuracy of the computational analysis results depends also on the accuracy of the available measurement data.

**B.5.2 Susceptibility to general corrosion**

Laboratory tests and operational experience have established that for RPV internals of austenitic stainless steels and nickel-base alloys general corrosion is not a significant degradation mode. These conclusions are based on the very low general corrosion rates which have been observed in BWR plants for all materials of RPV internals [1]. However, in the rare case of locally missing region of inner cladding the RPV base material can became susceptible to very slowly progressing general corrosion.

Material type specific susceptibility to general corrosion:

- Stainless steels and nickel-base alloys: The corrosion rate of these materials in water at all temperatures, relevant to water cooled reactors, are lower than those of carbon steels, due to the presence of adherent, protective surface spinel type oxides composed of a mixture of \( \text{NiFe}_2\text{O}_4, \text{Fe}_3\text{O}_4 \) and \( \text{FeCr}_2\text{O}_4 \). These are double layer oxide films with the inner layer being a chromium rich normal spinel (chromites), and the outer layer an inverse spinel magnetite/nickel ferrite (ferrites) [3].

- Carbon and low alloy steels: The corrosion rate of these steels in high temperature, neutral or slightly alkaline, de-aerated water decreases after initial immersion due to the growth of thermo-dynamically stable magnetite [228]. The oxidation rate follows cubic or parabolic corrosion damage versus time dependency. The magnetite that forms initially in high temperature de-oxygenated water is not particularly protective for the underlying metal. However, the oxide film grows rapidly. Larger magnetite crystallites, exceeding 0.5 \( \mu m \) in size, deposit on the outer surface of this base layer via a dissolution/precipitation reaction. The deposited oxide then grows and coalesces to create a relatively thin compact layer whose metal protection capability is enhanced by the deposition of fine \( \text{Fe}_3\text{O}_4 \) precipitates in the pores of the outer oxide [3].
Table B.5.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to general corrosion.

Table B.5.2-1. Summary on susceptibility of materials of BWR RPV and its internals to general corrosion [1,3]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

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<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

- rates of general corrosion for BWR internals are very low,
- in aqueous conditions pH, ECP, solubility of magnetite coating and water chemistry affect general corrosion.

B.5.3 Mitigation of general corrosion

General corrosion rates can be managed by:
- consideration of the thermo-dynamically stable (or metastable) species (dissolved metal cations, oxides, salts, etc.) at the metal/water interface, and
- control of the relevant oxidation and reduction kinetics.

Corrosion protection of structural alloys in reactor coolants primarily depends upon the formation of stable protective oxide films and maintaining water chemistry conditions that avoid damaging them [3].

General corrosion is well understood and not generally an issue for austenitic stainless steels if water chemistry is maintained. There have been few instances of issues in service. According to a recent expert panel assembled by the U.S. NRC [227] the probability for general corrosion in BWR RPB and its internals is very low. Thus, the main mitigation technique against general corrosion is improved water chemistry, such as HWC, but the actual purpose for these chemical improvements is mitigation against SCC.

B.5.4 Inspection and monitoring of general corrosion

The inspection requirements and monitoring for BWR RPV and its internals are already described in detail in Appendix B.3.6. Visual inspection methods are used to detect discontinuities and imperfections on the surface of components, including general corrosion [1].

B.6 E/C and FAC

E/C includes all forms of accelerated attack in which protective surface films and/or the metal surface itself are removed by combination of solution velocity and corrosion. FAC corresponds to erosion (or thinning) of component wall without threshold solution velocity, involving the
electrochemical aspects of general corrosion plus the effects of mass transfer and momentum transfer.

### B.6.1 Computational modelling of E/C and FAC

There are several procedures and analysis codes available to computationally predict the propagation of FAC. Some of the analysis codes are commercially available, some are proprietary. Significant FAC analysis procedures and codes are described in the following. This is followed with describing a computation procedure to predict E/C.

The KWU-KR model [229] allows calculating the FAC rate of carbon steel components as a function of Keller’s geometry factor, flow velocity, fluid temperature, material chemical composition, fluid chemistry (pH at 25 °C and dissolved oxygen), exposure time, and, in the case of two-phase flow, steam quality. The thinning of the component wall is calculated as a function of time with this model. The wall corrosion, \( W_C(t) \) [cm], is the thickness of the layer that has corroded away, and is given as [229]:

\[
W_C(t) = \frac{\Delta \phi_R t}{\rho_{st}}
\]  

(B.6.1-1)

where \( \Delta \phi_R \) [g/(cm²-h)] is FAC rate, \( t \) [h] is exposure time and \( \rho_{st} \) [g/cm³] is density of steel. The FAC rate is computed according to ref. [229] using data on pH, oxygen content, liquid velocity, geometrical factor, total content of chromium and molybdenum in steel, and operating temperature. The KWU-KR model is mainly used for FAC analyses of pipes but it is also applicable to other geometries, such as blades and plates. More recently, a probabilistic development of this FAC model has been provided, see ref. [8].

Extending the Keller and the Kastner correlations and the Berge model for FAC, Chexal and Horowitz designed and implemented an algorithm which is used in the CHECWORKS analysis code by EPRI [231]. In the CHECWORKS code, the equation for FAC rate [mm/year] can be written as [231]:

\[
\text{FAC rate} = F_1 \times F_2 \times F_3 \times F_4 \times F_5 \times F_6 \times F_7 \times (1 \text{ mm/year})
\]

(B.6.1-2)

where \( F_1 \) [-] is temperature factor, \( F_2 \) [-] is mass transfer factor, \( F_3 \) [-] is geometry factor, \( F_4 \) [-] is pH factor, \( F_5 \) [-] is oxygen factor, \( F_6 \) [-] is alloy factor, and \( F_7 \) [-] is void fraction factor.

CHECWORKS uses a large database of experimental and plant data while incorporating local conditions through water chemistry modelling (pH and dissolved oxygen), void fraction and flow modelling (velocity, pressure and enthalpy), as well as geometry factors from plant data with insight from copper modelling tests [231].

The process of FAC includes complicated chemical kinetics and fluid mechanics. It is difficult to incorporate all the process laws in the derivation of the model. Instead, Sanchez-Caldera et al. looked for the minimum number of variables necessary to explain the experimental data, indicated that [233]:

(a) the wear is a linear function of time,
(b) the wear rate is maximum at a temperature near 150 °C, and
(c) the wear increases with fluid velocity.
Based on the above mentioned information, Sanchez-Caldera et al. developed a simple analytical model to predict the FAC rate [233].

A theoretical FAC model of the interaction between electro-chemical and mechanical factors during erosion-corrosion process [234,235] has been developed on the basis of non-equilibrium thermodynamics, dislocation kinetics, and electro-chemistry. It has been concluded by VTT [236] that while this FAC model captures several interactions between mechanical and electro-chemical factors in erosion-corrosion process, further work especially on the hydrodynamic correlations and the material correlations is needed in order to transform this model into a predictive tool for wall thinning rates of NPP components.

According to the early views of Electricité de France (EDF) [237], FAC has been originally regarded as a completely deterministic phenomenon, as concerning carbon steel components under flows of hot degassed water. The influence of several parameters, such as the redox equilibrium fixed by the ferrous iron ion concentration and convective diffusion transport of soluble products into the flow, is explained with an analytical model [237]. A means of estimating quantitatively the corrosion kinetics in terms of wall thinning is given. Both single- and two-phase flows are considered. More recently, EDF has provided a probabilistic development of their FAC model, see ref. [238].

A steady state E/C model for the feedwater piping has been proposed by Gopika et al. [240]. In the model, oxide dissolution and mass transfer based on oxide dissolution are considered as main factors that influence E/C rate. The E/C kinetics are governed by two sequential steps, which are kinetic rate of oxide dissolution, \( R_k \) [mm/year], and mass transferred limited rate, \( R_{MT} \) [mm/year]. The total E/C rate, \( R_{E-C} \) [mm/year], can be written as:

\[
R_{E-C} = \left( R_k^{-1} + R_{MT}^{-1} \right)^{-1}
\]

(B.6.1-3)

where:

\[
R_k = R_0 \exp\left[ -E_k / (R \times T) \right] R_{adj1}
\]

(B.6.1-4a)

\[
R_{MT} = K (C_S - C_b) R_{adj2}
\]

(B.6.1-4b)

while \( R_0 = 9.55 \times 10^{32} \) atoms/(cm²s), \( E_k = 31580 \) cal/mol is activation energy, \( R = 2 \) cal/mol/K is universal gas constant, \( T [K] \) is temperature, \( R_{adj1} = 1.0 \) [mm/year]/[atoms/(cm²s)], \( K [-] \) is mass transfer coefficient, \( C_S \) [mol/mm²] is surface concentration, \( C_b \) [mol/mm²] is given bulk concentration, and \( R_{adj2} = 1.0 \) [mm/year]/[mol/mm²]. Data and assessment equations for these coefficients and concentration parameters are given in ref. [240].

Arguably, the FAC model in CHECWORKS analysis code [231] should be the most accurate one, as it is the most recent one. However, according to comparison results presented in report NUREG/CR-5632 [8] the EPRI model [231] is much more conservative than the KWU-KR model [229]. Keeping in mind that the KWU-KR model [229] is conservative too. All necessary data concerning the FAC models by EDF [237] are not publicly available. Thus, it is recommendable to use the KWU-KR model [229].
B.6.2 Susceptibility to E/C and FAC

Stainless steel and nickel-base alloys are generally resistant to E/C [1]. FAC occurs especially at areas of high turbulence that are often associated with geometrical discontinuities or abrupt changes in flow direction. FAC is most commonly observed in carbon steel used in feedwater, extraction steam and drain lines, and in copper-based alloys used in condensers and heat exchangers [3].

FAC of carbon steels also depends strongly on the steam quality of two-phase flows [231]. The kinetics of FAC increases very quickly when the steam quality attains 20 to 40 % and is at maximum between 40 and 80 %. It is also observed that the range of temperatures normally associated with FAC in single-phase water extends to higher temperatures in two-phase flow [232]. The deleterious effect of two-phase flow is all the more exaggerated as the pH of the water decreases or with increasing partition of the volatile treatment base to the vapour phase [3].

Table B.6.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to E/C and FAC.

Table B.6.2-1. Summary on susceptibility of materials of BWR RPV and its internals to erosion-corrosion and FAC [1,3,229]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>· FAC is most commonly observed in carbon steel used in feedwater, extraction steam and drain lines, and in condensers and heat exchangers,</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td></td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td></td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td></td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td></td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>· FAC of carbon steels also depends strongly on the steam quality of two phase flows, it is at maximum between 40 and 80 %,</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>· see Appendix B.6.1 for assessment of propagation of FAC.</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td></td>
</tr>
</tbody>
</table>

B.6.3 Mitigation of E/C and FAC

As BWR RPV internals are mainly of austenitic stainless steel, which generally is not susceptible to E/C or FAC, there is no specific need for mitigation against these degradation mechanisms. The BWR RPV base material is carbon steel, which in principle is susceptible to FAC, but as it is protected by an internal cladding of austenitic stainless steel there is no need for mitigation against E/C or FAC. However, to cover the very small risk of FAC for some BWR internals the mitigation issues are described in the following.

FAC is significantly affected by temperature with a maximum in dissolution rates of carbon steels occurring at about 130 °C in single-phase flow and at about 180 °C in two-phase flow [3]. The susceptibility to FAC strongly decreases in higher temperatures than these. As the operational temperature of BWR RPV and its internals is approximately 285 °C there is no need to alter it to decrease the susceptibility to FAC.

The influence of water chemistry on corrosion potential is another important factor affecting FAC of carbon steels. Oxygen at concentrations ranging from a few ppb to a few tens of ppb, depending on the flow rate, can have a dramatic effect on increasing the corrosion potential by
about 600 to 700 mV and on decreasing very significantly the solubility of the iron oxides. Hematite (Fe₂O₃), the oxide of iron that forms at high corrosion potential, is about three orders of magnitude less soluble in water than magnetite that forms at low corrosion potential. This has led to the practice of adding 20-30 ppb of oxygen to feedwater in BWRs [3].

The elemental composition of carbon (and low alloy steels) has a significant impact on FAC rates. The most important alloy variable is the chromium content, as based on the protective nature of the oxides that are formed on chromium containing alloys. Copper and molybdenum alloying additions have also been suggested to have beneficial effects, although such effects are typically smaller [3]. Thus, one way to mitigate FAC is by having suitable amounts of chromium, copper and molybdenum in carbon steel base material.

Component replacement and repair welding are also feasible mitigation techniques for BWR RPV and its internals. These approaches are described in more detail earlier in Sections 4.3.5 and B.4.5.

Report NUREG/CR-7153, Vol. 3 [170] presents a NPP component specific summary of mitigation actions and recommended guidelines against FAC degradation, based on experience concerning mainly/only U.S. BWR units up to 2014.

B.6.4 Inspection and monitoring of E/C and FAC

The inspection requirements and monitoring for BWR RPV and its internals are already described in detail in Section 4.3.6. Both visual examination and UT are used to detect FAC, e.g. in NPPs in Germany and France [239]. The NPPs in the U.S. have had FAC inspection programs for several years [8].

B.7 Mechanical wear

Wear is broadly characterized as mechanically induced or aided degradation mechanism. Degradation from small amplitude oscillatory motion between continuously rubbing surfaces is generally termed fretting. Vibration of relatively large amplitude, resulting in intermittent sliding contact between two parts, is termed sliding wear, or wear. Generally, wear results from concurrent effects of vibration and corrosion [1]. Wear is the loss of material, generally measured as the rate of removal of surface material, caused by the relative motion between adjacent metal surfaces or by the action of hard, abrasive particles in contact with a metal surface [3].

B.7.1 Computational modelling of wear

Wear as a degradation mechanism has been modelled extensively. It is estimated that by now hundreds of computational models have been developed to describe wear [241]. Many of these models are based on either friction or contact pressure approach. A commonly used simple model, which falls into the second category, has been developed by Archard [242]. With this model the volumetric amount of material loss due to wear, \( V \) [mm³], is computed as:

\[
V = \frac{K'Qs}{H}
\]  

(B.7.1-1)

where \( K' [-] \) is empirically determined wear coefficient, \( Q [N] \) is contact normal force, \( H [N/mm^2] \) is the hardness of the softer body, and \( s [mm] \) is the sliding distance. Obviously, the
value of $K'$ varies for different material pairs. It is usually determined by carrying out sliding wear tests using the materials of interest [243]. The following modification of this equation, where the hardness is related to the wear coefficient, is also commonly used [244,245,246]:

$$V = KQs$$  \hspace{1cm} (B.7.1-2)

where $K$ [mm²/N] is empirically determined wear coefficient.

Tribology is a field of science that focuses on modelling of wear and friction of interacting surfaces in relative motion. Tribology is also a quite new field of science, with most of the knowledge being gained after the second world war. In comparison, many basic engineering subjects, e.g. thermo-dynamics, mechanics and plasticity, are relatively old and well established. Tribology is still in an imperfect state and subject to some controversy which has impeded the diffusion of information to technologists in general [247].

The applicability of empirically defined wear models depends on the available measurement data. Once all available data for the analysis case has been collected, based on it one can choose an applicable wear model. It is difficult identify the most recommendable model. The accuracy of the wear analysis results depends also on the accuracy of the available measurement data.

### B.7.2 Susceptibility to mechanical wear

Mechanical wear has been identified as a potential degradation mechanism at specific locations in the RPV internals due to FIV [1]. Wear is a concern for some steam dryer components, such as vessel lugs, feedwater end brackets and BWR/6 shroud head studs [3]. Due to typical NPP monitoring systems on vibration and loose parts this degradation mechanism is of minor importance concerning the capability of RPV internals to perform their safety functions [1]. As for FIV, TVO has already examined the issue thoroughly for OL1/OL2 components. This information is reported in ref. [24].

Table B.7.2-1 presents a summary on the susceptibility of materials of BWR RPV and its internals to mechanical wear.

**Table B.7.2-1. Summary on susceptibility of materials of BWR RPV and its internals to mechanical wear [1,24]. Herein, in the left column the sign “X” corresponds to susceptibility, whereas the right column is independent of the left one.**

<table>
<thead>
<tr>
<th>Material type specific susceptibility</th>
<th>Necessary properties and/or conditions to allow/enhance susceptibility</th>
</tr>
</thead>
<tbody>
<tr>
<td>austenitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>cast stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>ferritic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>martensitic stainless steels</td>
<td>X</td>
</tr>
<tr>
<td>high chromium content nickel-base alloys</td>
<td>X</td>
</tr>
<tr>
<td>ferritic and low alloy steels</td>
<td>X</td>
</tr>
<tr>
<td>carbon steels and associated weld metals</td>
<td>X</td>
</tr>
<tr>
<td>low alloy RPV steels</td>
<td>X</td>
</tr>
</tbody>
</table>

- vibration and loose parts can cause mechanical wear, for a detailed description on susceptibility to FIV, see ref. [24].
B.7.3 Mitigation of mechanical wear

Generally, there are two approaches to mitigate wear. These are to eliminate or suppress/decrease the loading causing wear, or to apply a protective coating to the component surfaces.

Wear resistant coatings consist of carefully applied layers of usually hard materials, which are intended to give prolonged protection against wear. Abrasive wear, adhesive wear and fretting can be reduced by wear resistant coatings. There are many methods of applying hard materials. Many wear resistant materials are brittle or expensive, and can only be used as a coating [247].

There are many different methods of applying wear-resistant or hard coatings to a metal substrate currently in use [248,249]. The wear resistance of a surface can also be improved by localized heat treatment, i.e. thermal hardening, or by introducing alloying elements, such as nitriding or carburizing. Available techniques for modifying the component surface to improve its wear resistance include [247]:

- Physical and chemical vapour deposition: Thin discrete coating, no limitations on materials.
- Ion implantation: Thin diffuse coating, mixing with substrate inevitable.
- Surface welding: Suitable for very thick coatings only, limited to materials that remain stable at high temperatures, coated surfaces may need further preparation.
- Thermal spraying: Very thick coatings are possible but control of coating purity is difficult.
- Laser glazing and alloying: Thick coatings, coating material must be able to melt.
- Friction surfacing: Simple technology but limited to planar surfaces, produces thick metal coating.


B.7.4 Inspection and monitoring of mechanical wear

The inspection requirements and monitoring for BWR RPV and its internals are already described in detail in Section 4.3.6.

B.8 Modelling of interaction of degradation mechanisms

The interaction of degradation mechanisms is possible for the BWR RPV and its internals. The significant interacting degradation mechanisms described in Section 3.2.10 are irradiation-creep, corrosion-fatigue and creep-fatigue. As the operational temperatures of the BWR RPV and its internals are below the creep range, irradiation-creep and creep-fatigue are excluded here. As to stress relaxation the bolts in some joints do experience some, but as they are easily replaceable considering this degradation mechanism is excluded here. Thus, only the computational models for corrosion-fatigue are summarised here. The considered corrosion mode is SCC.

Many models which combine fatigue and stress corrosion effects have been suggested for predicting the corrosion-fatigue crack growth rate as a function of fatigue loading frequency. These models can be divided into three categories [25]:
1. superposition models,
2. competition models, and
3. models for environmentally modified material deformation and fatigue properties.

The superposition models and the competition models were developed with the assumption that an environmental fracture process is independent from the mechanical fatigue fracture process and that the latter process in a corrosive environment is identical with that in an inert environment. According to the superposition models, the environmental degradation and a pure fatigue fracture process occur simultaneously and independently on the same fracture surface, and have no interaction with each other.

The superposition model to describe the crack growth due to interaction of fatigue and SCC, \( \frac{da}{dt} \) [m/s] applicable to stainless steels is presented in report NUREG/CR-6176 [26], It can be written as follows:

\[
\frac{da}{dt} = \left( \frac{da}{dt} \right)_{SCC} + \left( \frac{da}{dt} \right)_{ENV} + \left( \frac{da}{dt} \right)_{AIR}
\]

where the three parts on the right side of the equality sign correspond to SCC, environmental corrosion-fatigue interaction and fatigue in air, as indicated by the associated subindices. These three parts are defined according to ref. [26]. Those can be written as:

\[
\frac{da}{dt} = 2.1 \times 10^{-13} \left( \frac{K_I}{K_{I,adj}} \right)^{2.161} \left( \frac{1}{\text{year}} \right)
\]  

with 8 ppm dissolved oxygen  

\[
\frac{da}{dt} = 7.0 \times 10^{-14} \left( \frac{K_I}{K_{I,adj}} \right)^{2.161} \left( \frac{1}{\text{year}} \right)
\]  

with 200 ppm dissolved oxygen  

\[
\frac{da}{dt} = 3.43 \times 10^{-12} \left( 1 + 1.18R \right) \left( \frac{\Delta K_{I,adj}}{K_{I,adj}} \right) \left( \frac{t_{adj}}{t_R} \right) \left( \frac{1}{\text{year}} \right)
\]

for \( R \leq 0.8 \)  

\[
\frac{da}{dt} = 3.43 \times 10^{-12} \left( -43.5 + 57.97R \right) \left( \frac{\Delta K_{I,adj}}{K_{I,adj}} \right) \left( \frac{t_{adj}}{t_R} \right) \left( \frac{1}{\text{year}} \right)
\]

for \( R > 0.8 \)  

\[
\frac{da}{dt} = 1.5 \times 10^{-4} \left( \frac{da}{dt} \right)^{0.5} \left( \frac{1}{\text{year}} \right)
\]

with 8 ppm dissolved oxygen
\[
\left( \frac{da}{dt} \right)_{\text{ENV}} = 4.5 \left(10^{-5} \right) \left( \frac{da}{dt} \right)^{0.5}_{\text{AIR}} \times \left( \frac{1 \text{ mm}}{\text{year}} \right) \text{ with 200 ppm dissolved oxygen} \quad (B.8.1-2f)
\]

where \( K_{I,adj} = 1.0 \text{ MPa}\sqrt{\text{m}} \), \( \Delta K_{I,adj} = 1.0 \text{ MPa}\sqrt{\text{m}} \), \( t_R \text{ [s]} \) is the load cycle specific rise time, \( t_{adj} = 1.0 \text{ s} \), and \( R [-] \) is again the stress ratio.

Kim et al. present a collection of quantitative models for corrosion-fatigue crack growth rates in metals [27]. These models reflect several environmental conditions. The models include empirical interpolative equations, linear superposition of mechanical fatigue and time based environmental cracking, as well as mechanism-based models. For several material-environment systems these models were incorporated in fracture mechanics based life prediction methods. However, Kim et al. reported [27] that considerable uncertainties are associated with these models.

As all steel and alloy types are more or less susceptible to fatigue but only some of them are susceptible to SCC and/or IASCC. It is concluded that the materials susceptible to corrosion-fatigue are those susceptible to SCC and/or IASCC. These material types are austenitic stainless steels, cast stainless steels and high chromium content nickel-base alloys, see Tables B.4.3-1 and B.4.4-1.

The mitigation, inspection and monitoring issues for interacting degradation mechanisms are excluded because they are already described for associated separate degradation mechanisms, i.e. fatigue, SCC and corrosion in its other forms.

One should be cautious when using the degradation mechanism interaction models due to their limited scope. As the actual mechanisms of joint action of degradation mechanisms are still not exactly known, the accuracy of the degradation mechanism interaction models is not very good.

**B.9 Summary on susceptibility to degradation mechanisms and corresponding protective actions**

A summary on susceptibility to and protective actions against degradation mechanisms concerning BWR RPV and its internals is presented in the following. Obviously loads, geometry and environment affect susceptibility too, e.g. temperature, pressure, flow rate, material interfaces, abrupt changes in geometry and water chemistry. The summary is based on the data presented in Appendices B.1 to B.7, and is presented here as tabulated, see Tables B.9.1-1, B.9.2-1 and B.9.3-1. It is not feasible here to try to assemble the material type specifically expressed susceptibility to degradation mechanisms into importance or significance classes, as that aspect is mainly dependent on the location specific loading conditions and of the environment. For instance, in some location a component of austenitic stainless steel can severely experience IGSCC, whereas in some other location none at all.

**B.9.1 Susceptibility**

A summary on the susceptibility of BWR RPV and its internals to relevant degradation mechanisms is presented in Table B.9.1-1 below.
Table B.9.1-1. Summary on susceptibility of BWR RPV and its internals to relevant degradation mechanisms. Here, susceptibility is denoted with "X".

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Material types</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>austenitic stainless steels</td>
</tr>
<tr>
<td>Irradiation embrittlement</td>
<td>X</td>
</tr>
<tr>
<td>Thermal embrittlement</td>
<td>X</td>
</tr>
<tr>
<td>Fatigue (i)</td>
<td>X</td>
</tr>
<tr>
<td>IGSCC</td>
<td>X</td>
</tr>
<tr>
<td>IASCC</td>
<td>X</td>
</tr>
<tr>
<td>General corrosion (ii)</td>
<td>X</td>
</tr>
<tr>
<td>Erosion-corrosion, FAC</td>
<td>X</td>
</tr>
<tr>
<td>Mechanical wear</td>
<td>X</td>
</tr>
</tbody>
</table>

(i): All metallic materials are in principle susceptible to fatigue, however, in most cases for BWR RPV and its internals the effect is negligible.
(ii): All metallic materials are in principle susceptible to general corrosion, however, for BWR RPV and its internals the effect is negligible.

B.9.2 Mitigation actions

A summary on the mitigation actions for BWR RPV and its internals against relevant degradation mechanisms is presented in Table B.9.2-1 below.

Table B.9.2-1. Summary on mitigation actions for BWR RPV and its internals against relevant degradation mechanisms. Here, applicable mitigation action is denoted with "X".

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Mitigation action</th>
</tr>
</thead>
<tbody>
<tr>
<td>Irradiation embrittlement</td>
<td>X</td>
</tr>
<tr>
<td>Thermal embrittlement</td>
<td>X</td>
</tr>
<tr>
<td>Fatigue</td>
<td>X</td>
</tr>
<tr>
<td>IGSCC</td>
<td>X</td>
</tr>
<tr>
<td>IASCC</td>
<td>X</td>
</tr>
<tr>
<td>General corrosion</td>
<td>X</td>
</tr>
<tr>
<td>Erosion-corrosion, FAC</td>
<td>X</td>
</tr>
<tr>
<td>Mechanical wear</td>
<td>X</td>
</tr>
</tbody>
</table>
### B.9.3 Inspection techniques

A summary on inspection techniques applicable to BWR RPV and its internals is presented in Table B.9.3-1 below.

*Table B.9.3-1. Summary on inspection techniques applicable to BWR RPV and its internals. Here, applicable inspection technique is denoted with "X".*

<table>
<thead>
<tr>
<th>Degradation mechanism</th>
<th>Surveillance capsules</th>
<th>NDT, UT, most techniques</th>
<th>NDT, Eddy current</th>
<th>NDT, liquid penetrant</th>
<th>NDT, UT, phased array</th>
<th>NDT, acoustic emission</th>
<th>NDT, visual examination</th>
</tr>
</thead>
<tbody>
<tr>
<td>Irradiation embrittlement</td>
<td>X</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Thermal embrittlement</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fatigue crack growth</td>
<td>X X X X X</td>
<td>X</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>IGSCC</td>
<td>X X X X</td>
<td>X</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>IASCC</td>
<td>X X X X</td>
<td>X</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>General corrosion</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Erosion-corrosion, FAC</td>
<td>X X X</td>
<td>X</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Mechanical wear</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Appendix C: Summary on characteristics and degradation susceptibility of considered components

A summary on the characteristics and degradation susceptibility of the OL1/OL2 RPV and its significant internals is presented in the following. These components are shown in Figure 5-1 in Section 5-1. The considered characteristics are the geometry and material regions. The degradation susceptibility issues are described after that.

Apart from the core shroud support skirt and the pump deck, which are welded to the RPV, all internals are removable [252].

The following symbols are used to denote the dimensions of the presented components:
- $L$ is length,
- $D_o$ is outer diameter, $D_i$ is inner diameter,
- $R_o$ is outer radius, $R_i$ is inner radius,
- $t$ is wall thickness,
- $h$ is height,
- $b$ is width.

Flange cooling spray piping (ID1), Long nozzle pipes in cooling spray piping (ID2)

Table C-1. The main dimensions and materials of flange cooling spray piping system [24,258].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>$L$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet pipe</td>
<td>88.9</td>
<td>3.6</td>
<td>NA</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Lower ringpipe</td>
<td>76.1</td>
<td>3.6</td>
<td>-</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Connection pipe</td>
<td>76.1</td>
<td>3.6</td>
<td>1254</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Upper ringpipe</td>
<td>76.1</td>
<td>3.6</td>
<td>-</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Long nozzle pipes in lower ringpipe</td>
<td>42.4</td>
<td>3.2</td>
<td>866</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Long nozzle pipes in lower ringpipe</td>
<td>42.4</td>
<td>3.2</td>
<td>826</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Long nozzles pipes in upper ringpipe</td>
<td>42.4</td>
<td>3.2</td>
<td>797</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Short nozzles in lower ringpipe</td>
<td>42.4</td>
<td>3.2</td>
<td>279</td>
<td>SIS 2333</td>
</tr>
</tbody>
</table>

Table C-2. Those degradation mechanisms for which flange cooling spray piping system is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>all pipes, austenitic stainless steel</td>
<td>FIV is possible [24], causes a large number of load cycles</td>
<td>material is susceptible, located in steam dome with little water, low stresses, low WRSs</td>
</tr>
</tbody>
</table>
Evacuation pipe (ID3)

Table C-3a. The dimensions and materials of evacuation pipe [24,259].

<table>
<thead>
<tr>
<th>Component</th>
<th>(D_o) [mm]</th>
<th>(t) [mm]</th>
<th>(L) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet pipe</td>
<td>88.9</td>
<td>3.6</td>
<td>NA</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>Evacuation pipe</td>
<td>76.1</td>
<td>3.6</td>
<td>NA</td>
<td>SIS 2333</td>
</tr>
</tbody>
</table>

Table C-3b. Those degradation mechanisms for which evacuation pipe is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>whole pipe, austenitic stainless steel</td>
<td>FIV is possible [24], causes a large number of load cycles</td>
<td>susceptible material, located in steam dome with little water, low stresses, low WRSs</td>
</tr>
</tbody>
</table>

Spring beams and support brackets (ID4)

Table C-4a. The dimensions and materials of spring beams and support brackets [261,265,263,264].

<table>
<thead>
<tr>
<th>Component</th>
<th>(b) [mm]</th>
<th>(t) [mm]</th>
<th>(h) [mm]</th>
<th>(L) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>beam</td>
<td>70</td>
<td></td>
<td>150</td>
<td>2165</td>
<td>Bofors CRO684</td>
</tr>
<tr>
<td>support bracket plate</td>
<td>30</td>
<td></td>
<td>268</td>
<td>298</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>

Table C-4b. Those degradation mechanisms for which spring beams and support brackets are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>all beams of austenitic stainless steel, brackets of nickel-base alloy</td>
<td>fatigue is possible, small number of load cycles</td>
<td>susceptible material, located in steam dome with little water, higher stresses at supports, low WRSs</td>
</tr>
</tbody>
</table>

Steam dryer (ID5)

Table C-5a. The dimensions and materials of steam dryer [267].

<table>
<thead>
<tr>
<th>Component</th>
<th>(D_o) [mm]</th>
<th>(t) [mm]</th>
<th>(h) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>whole construction (average)</td>
<td>5280</td>
<td></td>
<td></td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>whole construction</td>
<td></td>
<td></td>
<td>6477</td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>casing</td>
<td>6</td>
<td></td>
<td></td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>columns</td>
<td>6</td>
<td></td>
<td></td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>beams</td>
<td>6</td>
<td></td>
<td></td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>cover plates</td>
<td>6</td>
<td></td>
<td></td>
<td>Type 316 LNG</td>
</tr>
</tbody>
</table>
Table C-5b. Those degradation mechanisms for which steam dryer components are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>all components, austenitic stainless steel</td>
<td>FIV and acoustic resonance may cause high-cycle fatigue [24]</td>
</tr>
</tbody>
</table>

Steam outlet nozzles (ID6)

Table C-6a. The cross-section dimensions and materials of steam outlet nozzle [269,270,271]. The nozzle dimensions are given at the region where it joins the pipe.

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>508</td>
<td>23.5</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td></td>
<td>5</td>
<td>SIS-2333</td>
</tr>
</tbody>
</table>

Table C-6b. Those degradation mechanisms for which steam outlet nozzle is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle and welds, ferritic steel</td>
<td>material is moderately susceptible</td>
<td>low-cycle thermal fatigue is possible, significant WRSs</td>
</tr>
</tbody>
</table>

Steam separator stand pipes (ID7), Steam separator pipe bundles (ID8)

Table C-7. The cross-section dimensions and materials of steam separator stand pipes and pipe bundles [274,275,276].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>upper pipe part</td>
<td>250</td>
<td></td>
<td>Type 316 LNG</td>
</tr>
<tr>
<td>lower pipe part</td>
<td>168</td>
<td>3.4</td>
<td>Type 316 LNG</td>
</tr>
</tbody>
</table>

Table C-8. Those degradation mechanisms for which steam separator stand pipes and pipe bundles are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>all components, austenitic stainless steel</td>
<td>FIV is possible [24], causes large number of load cycles</td>
</tr>
</tbody>
</table>

Steam separator supports (ID9)

Table C-9a. The cross-section dimensions and materials of steam separator supports [276,278].

<table>
<thead>
<tr>
<th>Component</th>
<th>$b$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>support</td>
<td>120</td>
<td>45</td>
<td>Type 316 LNG</td>
</tr>
</tbody>
</table>
Table C-9b. Those degradation mechanisms for which steam separator supports are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>all components, austenitic stainless steel</td>
<td>FIV is possible [24], causes a large number of load cycles, mainly compressive stresses</td>
</tr>
</tbody>
</table>

Feedwater nozzles (ID10)

Table C-10a. The cross-section dimensions and materials of feedwater nozzle and connecting safe-end [256,269,271,280,282]. The nozzle dimensions are given at the region where it joins the safe-end.

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>360</td>
<td>35</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td></td>
<td>5</td>
<td>SIS-2333</td>
</tr>
<tr>
<td>buffer</td>
<td>360</td>
<td>35</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>nozzle to safe-end weld</td>
<td>360</td>
<td>35</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>safe-end</td>
<td>360</td>
<td>35</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>

Table C-10b. Those degradation mechanisms for which feedwater nozzle and connecting safe-end are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle, safe-end and welds, ferritic steel and austenitic stainless steels</td>
<td>nozzle material is susceptible, fluence level above negligible</td>
<td>nozzle material is moderately susceptible</td>
<td>thermal fatigue is possible for safe-end, elsewhere low-cycle fatigue is possible</td>
<td>safe-end and weld materials are susceptible, water environment, fluence level above negligible, significant WRSs</td>
</tr>
</tbody>
</table>
Feedwater spargers (ID11)

Table C-11a. The dimensions and materials of feedwater sparger [285].

<table>
<thead>
<tr>
<th>Component</th>
<th>b [mm]</th>
<th>L [mm]</th>
<th>D_o [mm]</th>
<th>t [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>front plate</td>
<td>606</td>
<td>3005</td>
<td>8</td>
<td>SS-2350-02</td>
<td></td>
</tr>
<tr>
<td>back plate</td>
<td>606</td>
<td>3157</td>
<td>8</td>
<td>SS-2350-02</td>
<td></td>
</tr>
<tr>
<td>cone</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>insert tube</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>cone</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>end plate 1</td>
<td>110</td>
<td>500</td>
<td>8</td>
<td>SS-2350-02</td>
<td></td>
</tr>
<tr>
<td>end plate 2</td>
<td>108</td>
<td>126</td>
<td>20</td>
<td>SS-2353-02</td>
<td></td>
</tr>
<tr>
<td>stiffener 1</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>stiffener 2</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>steel ring</td>
<td>130</td>
<td>12</td>
<td></td>
<td>SS-2353-02</td>
<td></td>
</tr>
<tr>
<td>onlay welding</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table C-11b. Those degradation mechanisms for which feedwater sparger is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>feedwater sparger components, austenitic stainless steels</td>
<td>large number of thermal load cycles</td>
<td>materials are susceptible, water environment, fluence level above negligible, low WRSs</td>
</tr>
</tbody>
</table>

Boron spray nozzles and piping (ID12)

Table C-12a. The cross-section dimensions and materials of boron spray nozzle and piping [24, 286]. The nozzle dimensions are given at the region where it joins the piping.

<table>
<thead>
<tr>
<th>Component</th>
<th>D_o [mm]</th>
<th>t [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>60</td>
<td>3.9</td>
<td>Inconel 600</td>
</tr>
<tr>
<td>piping</td>
<td>48.3</td>
<td>3.7</td>
<td>SA376 type 304</td>
</tr>
</tbody>
</table>

Table C-12b. Those degradation mechanisms for which boron spray nozzle and piping are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle, piping and weld, austenitic stainless steels</td>
<td>low-cycle fatigue is possible</td>
<td>materials are susceptible, water environment, fluence level above negligible, significant WRSs</td>
</tr>
</tbody>
</table>
Core spray piping outside core shroud cover (ID13)

Table C-13a. The cross-section dimensions and materials of core spray piping outside core shroud cover [276,288].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>piping</td>
<td>168</td>
<td>7.1</td>
<td>TP 316 LNG</td>
</tr>
</tbody>
</table>

Table C-13b. Those degradation mechanisms for which core spray piping outside core shroud cover is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>piping and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

Core spray piping inside core shroud cover (ID14)

Table C-14. The cross-section dimensions and materials of core spray piping inside core shroud cover [288].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>piping</td>
<td>168</td>
<td>7.1</td>
<td>TP 316 LNG</td>
</tr>
</tbody>
</table>

Table C-15. Those degradation mechanisms for which core spray piping inside core shroud cover is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>piping and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

Fuel assembly (ID15)

The main function of the fuel assemblies is obviously to provide the fuel to run the nuclear process that heats up the water brought to the RPV by the feedwater system. As the fuel assemblies do not carry external loads and are removed regularly, they are not described here further. They are, however, included here in the first place because they in general are among the significant RPV internals. So, they are included as a special case. Note, that the fuel assemblies are not included in the analyses.
### Control rods (ID16)

*Table C-16a. The main dimensions and materials of control rods [115].*

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>$b$ [mm]</th>
<th>$L$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>all together</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>6383</td>
</tr>
<tr>
<td>absorber cross</td>
<td></td>
<td></td>
<td>272</td>
<td></td>
<td>SIS 2352</td>
</tr>
<tr>
<td>absorber plate</td>
<td>8</td>
<td>132</td>
<td></td>
<td>3942</td>
<td>SIS 2352</td>
</tr>
<tr>
<td>rod</td>
<td>70</td>
<td></td>
<td></td>
<td>2406</td>
<td>SIS 2353</td>
</tr>
</tbody>
</table>

*Table C-16b. Those degradation mechanisms for which control rods are susceptible to.*

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>rod and plates, austenitic stainless steel</td>
<td>FIV and acoustic resonance may cause high-cycle fatigue [24]</td>
<td>material is susceptible, water environment, for top parts fluence above negligible, low WRSs</td>
</tr>
</tbody>
</table>

### Control rod guide tubes (ID17)

*Table C-17a. The main dimensions and materials of control rod guide tubes [293].*

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>$L$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>all together</td>
<td></td>
<td></td>
<td>3782</td>
<td>SIS 2333-02</td>
</tr>
<tr>
<td>guide tube</td>
<td>297</td>
<td>4.5</td>
<td></td>
<td>SIS 2333-02</td>
</tr>
</tbody>
</table>

*Table C-17b. Those degradation mechanisms for which control rod guide tubes are susceptible to.*

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>tubes, austenitic stainless steel</td>
<td>FIV and acoustic resonance may cause high-cycle fatigue [24]</td>
<td>material is susceptible, water environment, for top parts fluence above negligible, low WRSs</td>
</tr>
</tbody>
</table>

### Core shroud, Core shroud support (ID18)

*Table C-18a. The dimensions and materials of core shroud and its support [294,297,299,300].*

<table>
<thead>
<tr>
<th>Component</th>
<th>$h$ [mm]</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>$b$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>core shroud</td>
<td>4045</td>
<td>4340</td>
<td>25</td>
<td></td>
<td>SIS 2338</td>
</tr>
<tr>
<td>core shroud stand</td>
<td>4200</td>
<td>4120</td>
<td>25</td>
<td></td>
<td>SIS 2338</td>
</tr>
<tr>
<td>bracket</td>
<td></td>
<td>10</td>
<td></td>
<td></td>
<td>SIS 2333-02</td>
</tr>
</tbody>
</table>
Table C-18b. Those degradation mechanisms for which core shroud and its support are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base materials and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible, acoustic resonance may cause high-cycle fatigue [24]</td>
<td>materials are susceptible, water environment, fluence level above negligible, low WRSs</td>
</tr>
</tbody>
</table>

Pump deck (ID19)

Table C-19a. The cross-section dimensions and materials of pump deck [301].

<table>
<thead>
<tr>
<th>Component</th>
<th>b [mm]</th>
<th>t [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>pump deck</td>
<td>742</td>
<td>30</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>

Table C-19b. Those degradation mechanisms for which pump deck is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material and welds, austenitic stainless steel</td>
<td>acoustic resonance may cause high-cycle fatigue [24]</td>
<td>material is susceptible, water environment, significant WRSs</td>
</tr>
</tbody>
</table>

Main circulation pump nozzles (ID20)

Table C-20a. The cross-section dimensions and materials of main circulation pump nozzle and stretch tube [271,302,303,304].

<table>
<thead>
<tr>
<th>Component</th>
<th>D₀ [mm]</th>
<th>t [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>outer nozzle pipe</td>
<td>312</td>
<td>45</td>
<td>ASME SA-508, Class 2</td>
</tr>
<tr>
<td>inner nozzle pipe</td>
<td>246</td>
<td>33</td>
<td>ASME SA-508, Class 2</td>
</tr>
<tr>
<td>stretch tube</td>
<td>230</td>
<td>5.6</td>
<td>Inconel 600</td>
</tr>
<tr>
<td>outer cladding, nozzle top</td>
<td>3</td>
<td></td>
<td>Inconel 182</td>
</tr>
<tr>
<td>outer cladding, all but nozzle top</td>
<td>3</td>
<td></td>
<td>SIS 2333</td>
</tr>
<tr>
<td>inner cladding</td>
<td>3</td>
<td></td>
<td>Inconel 182</td>
</tr>
</tbody>
</table>

Table C-20b. Those degradation mechanisms for which main circulation pump nozzle and stretch tube are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle pipes, stretch tube, claddings and welds, ferritic steel, austenitic stainless steels and austenitic stainless steel welds</td>
<td>nozzle material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
<td>stretch tube is susceptible, water environment, welds are protected by cladding, low WRSs</td>
</tr>
</tbody>
</table>
Core shroud support legs (ID21)

Table C-21a. The dimensions and materials of core shroud support legs [298].

<table>
<thead>
<tr>
<th>Component</th>
<th>( h ) [mm]</th>
<th>( D_s ) [mm]</th>
<th>( t ) [mm]</th>
<th>( b ) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>support leg</td>
<td>450</td>
<td>25</td>
<td>220</td>
<td></td>
<td>Inconel 600, SIS 2338</td>
</tr>
</tbody>
</table>

Table C-21b. Those degradation mechanisms for which core shroud support legs are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material and welds, austenitic stainless steel</td>
<td>low-cycle fatigue is possible</td>
<td>materials are susceptible, water environment, significant WRSs</td>
</tr>
</tbody>
</table>

Instrumentation guide tubes and nozzles (ID22)

Table C-22a. The dimensions and materials of instrumentation guide tubes and nozzles [306,307,308].

<table>
<thead>
<tr>
<th>Component</th>
<th>( L ) [mm]</th>
<th>( D_s ) [mm]</th>
<th>( t ) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>guide tube</td>
<td>3809</td>
<td>58-82</td>
<td>6.5-13</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>guide nozzle, top part</td>
<td>1970</td>
<td>60-65</td>
<td>5-9.5</td>
<td>SIS 2333</td>
</tr>
<tr>
<td>guide nozzle</td>
<td>7056</td>
<td>25</td>
<td></td>
<td>SIS 2333</td>
</tr>
</tbody>
</table>

Table C-22b. Those degradation mechanisms for which instrumentation guide tubes and nozzles are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material and welds, austenitic stainless steel</td>
<td>FIV may cause high-cycle fatigue [24]</td>
<td>materials are susceptible, water environment, for top parts fluence above negligible, low WRSs</td>
</tr>
</tbody>
</table>

Control rod guide tubes and nozzles at RPV bottom (ID23)

Table C-23a. The dimensions and materials of control rod guide tube and nozzle [309,310,311]. For nozzle, the dimensions are given near the top weld.

<table>
<thead>
<tr>
<th>Component</th>
<th>( L ) [mm]</th>
<th>( D_s ) [mm]</th>
<th>( t ) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>tube, whole structure</td>
<td>6726</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>tube, at RPV bottom region</td>
<td>125</td>
<td>11.5</td>
<td></td>
<td>Inconel 600</td>
</tr>
<tr>
<td>tube, near RPV bottom region</td>
<td>124-198</td>
<td>12-21.5</td>
<td></td>
<td>SIS 2333</td>
</tr>
<tr>
<td>nozzle</td>
<td>159</td>
<td>16.25</td>
<td></td>
<td>SIS 2103</td>
</tr>
<tr>
<td>outer cladding of nozzle</td>
<td>161</td>
<td>2</td>
<td></td>
<td>Inconel 182</td>
</tr>
</tbody>
</table>
Table C-23b. Those degradation mechanisms for which control rod guide tubes and nozzles are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>tube material, nozzle material and welds, austenitic stainless steel, ferritic steel, alloy weld material</td>
<td>nozzle material is moderately susceptible</td>
<td>low-cycle fatigue is possible, acoustic resonance may cause high-cycle fatigue [24]</td>
<td>tube material is susceptible, water environment, significant WRSs</td>
</tr>
</tbody>
</table>

Cylindrical RPV shell (ID24)

Table C-24a. The cross-section dimensions and materials of cylindrical RPV shell [269,271].

<table>
<thead>
<tr>
<th>Component</th>
<th>(D_c) [mm]</th>
<th>(t) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV base material</td>
<td>5818</td>
<td>134</td>
<td>ASTM 533 Grade B, Class 1</td>
</tr>
<tr>
<td>RPV cladding</td>
<td>5</td>
<td>SIS 2333</td>
<td></td>
</tr>
</tbody>
</table>

Table C-24b. Those degradation mechanisms for which cylindrical RPV shell is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is susceptible, fluence level above negligible</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

RPV bottom (ID25)

Table C-25a. The dimensions and materials of RPV bottom [269,271].

<table>
<thead>
<tr>
<th>Component</th>
<th>(R_b) [mm]</th>
<th>(t) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV base material</td>
<td>2957</td>
<td>182</td>
<td>ASTM 533 Grade B, Class 1</td>
</tr>
<tr>
<td>RPV cladding</td>
<td>5</td>
<td>SIS 2333</td>
<td></td>
</tr>
</tbody>
</table>

Table C-25b. Those degradation mechanisms for which RPV bottom is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>
RPV support skirt (ID26)

Table C-26a. The dimensions and materials of RPV support skirt [313,314,315,316,317,318, 319].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV support skirt</td>
<td>5880-7100</td>
<td>35</td>
<td>ASTM 533 Grade B, Class 1</td>
</tr>
<tr>
<td>Base ring</td>
<td>7100</td>
<td></td>
<td>SIS 2103</td>
</tr>
<tr>
<td>Welds</td>
<td></td>
<td></td>
<td>A2-8018G-1, A2-S3NiMo-1</td>
</tr>
</tbody>
</table>

Table C-26b. Those degradation mechanisms for which RPV support skirt is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is moderately susceptible</td>
</tr>
</tbody>
</table>

RPV flange (ID27)

Table C-27a. The dimensions and materials of RPV flange [321,322].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV flange</td>
<td>6264</td>
<td>345</td>
<td>ASME SA-508, Class 2</td>
</tr>
</tbody>
</table>

Table C-27b. Those degradation mechanisms for which RPV flange is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

RPV-head (ID28)

Table C-28a. The dimensions and materials of RPV-head [269,271].

<table>
<thead>
<tr>
<th>Component</th>
<th>$R_i$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV-head base material</td>
<td>2770</td>
<td>80-133</td>
<td>ASTM 533 Grade B, Class 1</td>
</tr>
<tr>
<td>RPV-head cladding</td>
<td>5</td>
<td></td>
<td>SIS 2333</td>
</tr>
</tbody>
</table>
Table C-28b. Those degradation mechanisms for which RPV-head is susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

RPV-head bolts (ID29)

Table C-29a. The dimensions and materials of RPV-head bolts [323].

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$L$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV-head bolts</td>
<td>132</td>
<td>1570</td>
<td>SA-540 Grade B24, Class 3</td>
</tr>
</tbody>
</table>

Table C-29b. Those degradation mechanisms for which RPV-head bolts are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
</tr>
</thead>
<tbody>
<tr>
<td>base material, welds and cladding, ferritic steels and austenitic stainless steel</td>
<td>base material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
</tr>
</tbody>
</table>

Shutdown cooling nozzles (ID30)

Table C-30a. The cross-section dimensions and materials of Shutdown cooling nozzle and connecting safe-end [325,326,327]. The nozzle dimensions are given at the region where it joins the safe-end.

<table>
<thead>
<tr>
<th>Component</th>
<th>$D_o$ [mm]</th>
<th>$t$ [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>273</td>
<td>20</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td>5</td>
<td>20</td>
<td>SIS-2333</td>
</tr>
<tr>
<td>buffer</td>
<td>273</td>
<td>20</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>nozzle to safe-end weld</td>
<td>273</td>
<td>20</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>safe-end</td>
<td>220</td>
<td>20</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>

Table C-30b. Those degradation mechanisms for which Shutdown cooling nozzle and connecting safe-end are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle, safe-end and welds, ferritic steel and austenitic stainless steels</td>
<td>nozzle material is susceptible, fluence level above negligible</td>
<td>nozzle material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
<td>safe-end and weld materials are susceptible, water environment, fluence level above negligible, significant WRSs</td>
</tr>
</tbody>
</table>
Core spray nozzles (ID31)

Table C-31a. The cross-section dimensions and materials of Core spray nozzle and connecting safe-end [328,330,331,332]. The nozzle dimensions are given at the region where it joins the safe-end.

<table>
<thead>
<tr>
<th>Component</th>
<th>(D_o) [mm]</th>
<th>(t) [mm]</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle</td>
<td>220</td>
<td>24</td>
<td>ASTM A508-69, Class 2</td>
</tr>
<tr>
<td>cladding</td>
<td></td>
<td>5</td>
<td>SIS-2333</td>
</tr>
<tr>
<td>buffer</td>
<td>220</td>
<td>24</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>nozzle to safe-end weld</td>
<td>220</td>
<td>24</td>
<td>Inconel 182</td>
</tr>
<tr>
<td>safe-end</td>
<td>220</td>
<td>24</td>
<td>Inconel 600</td>
</tr>
</tbody>
</table>

Table C-31b. Those degradation mechanisms for which Core spray nozzle and connecting safe-end are susceptible to.

<table>
<thead>
<tr>
<th>Material region, material type</th>
<th>Irradiation embrittlement</th>
<th>Thermal embrittlement</th>
<th>Fatigue</th>
<th>IGSCC, IASCC</th>
</tr>
</thead>
<tbody>
<tr>
<td>nozzle, safe-end and welds, ferritic steel and austenitic stainless steels</td>
<td>nozzle material is susceptible, fluence level above negligible</td>
<td>nozzle material is moderately susceptible</td>
<td>low-cycle fatigue is possible</td>
<td>safe-end and weld materials are susceptible, water environment, fluence level above negligible, significant WRSs</td>
</tr>
</tbody>
</table>
Susceptibility of boiling water reactor pressure vessel and its internals to degradation

Otso Cronvall