Material characterization, modeling, and incorporation of the models in the machine simulation of large-diameter synchronous machines

Ismet Tuna Gürbüz
Material characterization, modeling, and incorporation of the models in the machine simulation of large-diameter synchronous machines

Ismet Tuna Gürbüz

A doctoral thesis completed for the degree of Doctor of Science (Technology) to be defended, with the permission of the Aalto University School of Electrical Engineering, at a public examination held at the lecture hall AS1 of the school on 12 January 2024 at 12 noon.

Aalto University
School of Electrical Engineering
Department of Electrical Engineering and Automation
Supervising professor
Prof. Anouar Belahcen, Aalto University, Finland

Thesis advisors
Dr. Floran Martin, Aalto University, Finland
Dr. Uğur Aydınlı, ABB Oy, Finland

Preliminary examiners
Prof. Nora Leuning, RWTH Aachen University, Germany
Prof. Jean-Philippe Lecointe, Université d’Artois, France

Opponents
Prof. Luc Dupré, Ghent University, Belgium
Prof. Jean-Philippe Lecointe, Université d’Artois, France
Abstract

This dissertation presents a comprehensive methodology for the realistic and computationally efficient simulation of large-diameter synchronous machines accounting for the effect of cutting on iron losses and magnetization. To achieve this, magnetic materials used in the machine parts are experimentally characterized, material models based on the characterization are developed, the developed models are incorporated into finite-element (FE) simulation software, and machine simulation is performed with the incorporated models.

Non-oriented punched electrical steel sheets used in the stator laminations are studied experimentally under different uniaxial stress conditions using a modified single-sheet tester. It is shown that the effect of stress on the iron losses of the punched samples differs based on the extent of degradation observed in the samples following the cutting procedure. Subsequently, in the material modeling, the focus is given to the modeling of punching. A continuous material modeling approach with an exponential deterioration profile is used for the magnetization, and iron losses are modeled similarly by modifying the coefficients of Jordan’s method.

Thick laser-cut steel laminations used in the rotor poles are studied experimentally using a ring-core measurement system. The characterization of the material properties and iron losses is then achieved by a 2-D axisymmetric FE modeling of the lamination cross-section with the inclusion of a continuous local material model using a quadratic deterioration profile. It is shown that the inclusion of the edge effects for the thick laminations is needed, which requires a 2-D modeling. In light of this, a simple 2-D analytical model is developed for eddy-current loss computation.

To achieve a computationally efficient and accurate implementation of cutting deterioration into electromagnetic FE simulation, a new methodology for numerical integration is proposed. The validity of this approach is confirmed by comparing it to the analytical solution for a 2-D beam geometry. Subsequently, the method is utilized in the 2-D FE simulation of transformers, resulting in an enhanced computational efficiency when compared to existing methods.

Time-stepping simulation of the studied large-diameter synchronous machine is achieved with the incorporated models developed for the stator laminations and rotor poles following the proposed methodology. The effect of cutting on the loss components and machine operating points is analyzed. The results demonstrate that accurate incorporation of the cutting effect in the machine simulation increases the machine’s losses by 16.4 kW, necessitating improved cooling capabilities.

Keywords Electrical steel sheets, cutting, eddy currents, finite-element modeling

ISSN (printed) 1799-4934 ISSN (pdf) 1799-4942
Location of publisher Helsinki Location of printing Helsinki Year 2023
Pages 153
Preface

This work was carried out in the Research Group of Electromechanics at Aalto University School of Electrical Engineering, Department of Electrical Engineering between September 2019 and July 2023. I am grateful to my supervisor, Prof. Anouar Belahcen, and instructors, Dr. Floran Martin and Dr. Ugur Aydin, for their excellent supervision and guidance throughout this research. I am also deeply grateful to Prof. Paavo Rasilo for his support and contributions during my research journey. The pre-examination of this thesis was performed by Prof. Jean-Philippe Lecointe and Prof. Nora Leuning, and I am thankful for their valuable time, comments, and feedback.

I would like to express my gratitude to ABB Switzerland Ltd. for funding my research project and providing invaluable support throughout my work. Additionally, I extend my thanks to the Finnish Foundation for Technology Promotion, Walter Ahlström Foundation, and Oy Strömberg Ab for their encouragement grants and incentive scholarships.

Special thanks to my colleagues in the Electromechanics Research Group for their work-related collaboration and valuable friendships throughout the entire journey. I will miss the coffee break discussions and Friday lunches we shared.

I would also like to thank my friends in Finland and Türkiye, and my parents for their unwavering support throughout my work. Lastly, I express my gratitude to my lovely wife, Tugce, for her endless trust, patience, and encouragement during these years.

Espoo, December 1, 2023,

Ismet Tuna Gürbüz
## Contents

1. **Introduction**  
   1.1 Background .............................................. 23  
   1.2 Definitions .............................................. 24  
   1.3 Aim and Focus of the Thesis ............................. 25  
   1.4 Scientific Contribution ................................... 25  
   1.5 Flow of the Research Work ............................... 26  
   1.6 Outline of the Thesis ..................................... 27  

2. **Review of Relevant Research**  
   2.1 Effect of Cutting and Mechanical Stress ............... 29  
      2.1.1 Magneto-Mechanical Concepts ......................... 29  
      2.1.2 Effect of Cutting .................................... 30  
      2.1.3 Effect of Mechanical Stress ......................... 32  
   2.2 Experimental Characterization of Effect of Cutting and  
       Uniaxial Stress at Macroscopic Scale ................... 33  
      2.2.1 Standard Magnetic Measurement Systems ............ 34  
      2.2.2 Experimental Characterization of Effect of Cutting  
       .......................................................... 34  
      2.2.3 Experimental Characterization of Effect of Uniaxial  
       Stress .................................................... 35  
   2.3 Modeling Iron Losses and Constitutive Material Law ... 36  
      2.3.1 Empirical Iron Loss Models ........................... 37  
      2.3.2 Theoretical Hysteresis Models ....................... 38  
      2.3.3 Physical Eddy-Current Models for Laminations ..... 40
<table>
<thead>
<tr>
<th>Section</th>
<th>Title</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.4</td>
<td>Modeling Cutting Deterioration</td>
<td>43</td>
</tr>
<tr>
<td>2.4.1</td>
<td>Magnetization Models with Cutting Deterioration</td>
<td>43</td>
</tr>
<tr>
<td>2.4.2</td>
<td>Iron Loss Models with Cutting Deterioration</td>
<td>44</td>
</tr>
<tr>
<td>2.5</td>
<td>Finite-Element Machine Simulation Including Cutting Deterioration</td>
<td>46</td>
</tr>
<tr>
<td>2.5.1</td>
<td>Modeling Iron Losses in Finite-Element Machine Simulation</td>
<td>46</td>
</tr>
<tr>
<td>2.5.2</td>
<td>Incorporation of Cutting Deterioration into Finite-Element Machine Simulation</td>
<td>48</td>
</tr>
<tr>
<td>2.5.3</td>
<td>Effect of Cutting on Machine Performance</td>
<td>49</td>
</tr>
<tr>
<td>2.6</td>
<td>Summary and Conclusions</td>
<td>50</td>
</tr>
<tr>
<td>3.</td>
<td>Methods</td>
<td>53</td>
</tr>
<tr>
<td>3.1</td>
<td>Measurement Systems</td>
<td>53</td>
</tr>
<tr>
<td>3.1.1</td>
<td>Measurements of Electrical Steel Sheets</td>
<td>53</td>
</tr>
<tr>
<td>3.1.2</td>
<td>Measurements of Thick Steel Laminations</td>
<td>55</td>
</tr>
<tr>
<td>3.2</td>
<td>Modeling Electrical Steel Sheets</td>
<td>57</td>
</tr>
<tr>
<td>3.2.1</td>
<td>Magnetization Model</td>
<td>57</td>
</tr>
<tr>
<td>3.2.2</td>
<td>Iron Loss Model</td>
<td>58</td>
</tr>
<tr>
<td>3.3</td>
<td>Modeling Thick Steel Laminations</td>
<td>59</td>
</tr>
<tr>
<td>3.3.1</td>
<td>2-D Axisymmetric Finite-Element Model</td>
<td>60</td>
</tr>
<tr>
<td>3.3.2</td>
<td>2-D Analytical Model</td>
<td>62</td>
</tr>
<tr>
<td>3.4</td>
<td>Incorporation of Cutting Deterioration into Electromagnetic Finite-Element Simulation</td>
<td>64</td>
</tr>
<tr>
<td>3.4.1</td>
<td>Formulation</td>
<td>65</td>
</tr>
<tr>
<td>3.4.2</td>
<td>Numerical Integration</td>
<td>65</td>
</tr>
<tr>
<td>4.</td>
<td>Applications and Results</td>
<td>69</td>
</tr>
<tr>
<td>4.1</td>
<td>Experimental Characterization of Electrical Steel Sheets</td>
<td>69</td>
</tr>
<tr>
<td>4.1.1</td>
<td>Measured Magnetic Properties and Iron Losses</td>
<td>69</td>
</tr>
<tr>
<td>4.1.2</td>
<td>Effect of Stress on Punched Samples</td>
<td>71</td>
</tr>
<tr>
<td>4.2</td>
<td>Verification of Models for Electrical Steel Sheets</td>
<td>72</td>
</tr>
<tr>
<td>4.2.1</td>
<td>Magnetization Model</td>
<td>73</td>
</tr>
<tr>
<td>4.2.2</td>
<td>Iron Loss Model</td>
<td>73</td>
</tr>
<tr>
<td>4.3</td>
<td>Verification of Characterization and Models for Thick Steel Laminations</td>
<td>74</td>
</tr>
<tr>
<td>4.3.1</td>
<td>2-D Axisymmetric Finite-Element Model</td>
<td>74</td>
</tr>
<tr>
<td>4.3.2</td>
<td>2-D Analytical Model</td>
<td>78</td>
</tr>
<tr>
<td>4.4</td>
<td>Verification of the Proposed Numerical Integration Method</td>
<td>80</td>
</tr>
<tr>
<td>4.4.1</td>
<td>Beam Geometry</td>
<td>81</td>
</tr>
<tr>
<td>4.4.2</td>
<td>Transformer</td>
<td>84</td>
</tr>
<tr>
<td>4.5</td>
<td>Synchronous Machine Studies</td>
<td>87</td>
</tr>
<tr>
<td>4.5.1</td>
<td>Studied Machine and Simulated Cases</td>
<td>88</td>
</tr>
<tr>
<td>4.5.2</td>
<td>Simulation Results and Analysis</td>
<td>89</td>
</tr>
</tbody>
</table>
5. **Discussion and Conclusions**

5.1 Discussions of the Methods and Results

5.1.1 Summary of the Findings

5.1.2 Significance of the Work

5.2 Suggestions for Future Work

5.2.1 Modeling Combined Effect of Stress and Cutting for Electrical Steel Sheets

5.2.2 Eddy-Current Modeling in Finite-Element Machine Simulation

5.2.3 Effect of Speed Fluctuations on Losses in Electrical Machines

5.3 Conclusions

References

Errata

Publications
This thesis consists of an overview and of the following publications which are referred to in the text by their Roman numerals.


Author’s Contribution

Publication I: “Experimental characterization of the effect of uniaxial stress on magnetization and iron losses of electrical steel sheets cut by punching process”

In this paper, the effect of uniaxial stress on iron losses of M400-50A grade non-oriented electrical sheets cut by punching process is studied based on the experimental magnetic measurements. Measurement results are analyzed and observations are done in detail. It is found that the effect of stress varies according to the level of degradation after punching.

The published work is a joint work among the authors. Ismet Tuna Gürbüz assembled the samples, performed the experimental measurements, structured and analyzed the measured data, and wrote the article. Flo- ran Martin, Ugur Aydin, and Paavo Rasilo helped with the measurement software. Ahamed Bilal Asaf Ali and Marta Chamosa helped with the material selection and preparation. Anouar Belahcen supervised the work and helped with the conceptualization. All the co-authors contributed to the paper with discussions and comments.

Publication II: “Finite-Element Modeling and Characterization of Iron Losses in 12 mm Thick Steel Laminations Including the Effect of Cutting”

In this paper, eddy-current and hysteresis losses of 12 mm thick laminations are modeled with a 2-D axisymmetric finite-element model based on the performed magnetic measurements including the effect of cutting. The presented results in the paper successfully show how iron losses occur in thick steel laminations and how they are affected by the cutting process.

The published work is a joint work among the authors. Ismet Tuna Gürbüz assembled the samples, performed the experimental measurements,
structured and analyzed the measured data, developed the model, and wrote the article. Paavo Rasilo helped with the development of the model from the software and conceptualization perspectives. Floran Martin and Ugur Aydin helped with the measurement software and measurements. Osaruyi Osemwinyen helped with the measurements. Ahamed Bilal Asaf Ali and Marta Chamosa helped with the material selection and preparation. Anouar Belahcen supervised the work and helped with the conceptualization. All the co-authors contributed to the paper with discussions and comments.

**Publication III: “2-D Analytical Model for Computing Eddy-Current Loss in Nonlinear Thick Steel Laminations”**

In this paper, a time-domain analytical model to compute the eddy-current loss of nonlinear thick steel laminations (3 mm–12 mm) based on the solution of the 2-D field problem is proposed. A simple frequency-domain eddy-current loss formulation for the nonlinear material is derived for the sinusoidal flux density case with the inclusion of a skin-effect correction factor.

The published work is a joint work among the authors. Ismet Tuna Gürbüz developed the proposed model, analyzed the results of the applications, and wrote the article. Paavo Rasilo helped with the development of the model and conceptualization of the work. Floran Martin and Osaruyi Osemwinyen helped with the measurements, analyses of the results, and conceptualization of the work. Anouar Belahcen supervised the work and helped with the conceptualization. All the co-authors contributed to the paper with discussions and comments.

**Publication IV: “A new methodology for incorporating the cutting deterioration of electrical sheets into electromagnetic finite-element simulation”**

In this paper, a novel method to incorporate cutting deterioration into electromagnetic finite-element simulation based on the re-computation of Gaussian quadrature weights and coordinates for the modeled exponential deterioration. The proposed method is computationally more efficient than the existing approaches and provides an easy adaption for any type of deterioration profile.

The published work is a joint work among the authors. Ismet Tuna Gürbüz developed the proposed model, analyzed the results of the applications, and wrote the article. Floran Martin helped with the development of the model from the software and conceptualization perspectives. Paavo Rasilo
helped with the conceptualization and organization of the work. Md Masum Billah helped with the simulations. Anouar Belahcen supervised the work and helped with the conceptualization. All the co-authors contributed to the paper with discussions and comments.
### Latin Symbols

- **a**: vector of nodal vector potentials
- **A<sub>b</sub>**: cross-sectional area of the B-coil
- **A<sub>H</sub>**: cross-sectional area of the H-coil
- **B**: local magnetic flux density (scalar)
- **B**: local magnetic flux density vector
- **B<sub>0</sub>**: average magnetic flux density (scalar)
- **B<sub>0</sub>**: average magnetic flux density vector
- **B<sub>dam</sub>**: magnetic flux density of the damaged material (scalar)
- **B<sub>m</sub>**: amplitude of the average magnetic flux density
- **B<sub>t</sub>**: threshold magnetic flux density value between the linear and nonlinear regions
- **B<sub>un</sub>**: magnetic flux density of the undamaged material (scalar)
- **c<sub>dam</sub>**: vector of Marrocco’s model parameters for the damaged material
- **c<sub>un</sub>**: vector of Marrocco’s model parameters for the undamaged material
- **d**: lamination thickness
- **d<sub>tot</sub>**: total thickness of the stacked laminations
- **E**: local electric field strength vector
- **e<sub>size</sub>**: finite-element size
List of Symbols and Abbreviations

\( f \)  load vector

\( F \) magnetomotive force created by the primary winding of the toroidal samples

\( F_{\text{cor}} \) proposed skin-effect correction factor for 2-D

\( F_{\text{lin}} \) linear part of the proposed 2-D skin-effect correction factor

\( F_{\text{non}} \) nonlinear part of the proposed 2-D skin-effect correction factor

\( F_{\text{skin}} \) skin-effect correction factor for linear materials in 1-D

\( H \) local magnetic field strength (scalar)

\( \mathbf{H} \) local magnetic field strength vector

\( H_{\text{eff}} \) effective magnetic field strength (scalar)

\( H_s \) magnetic field strength at the lamination surface (scalar)

\( \mathbf{H}_s \) magnetic field strength vector at the lamination surface

\( i_{\text{pri}} \) measured current in the primary winding of toroidal samples

\( \mathbf{J} \) electrical current density vector

\( k_{\text{dy}} \) dynamic loss density coefficient

\( k_{\text{dy,dam}} \) dynamic loss density coefficient for the damaged material

\( k_{\text{dy,un}} \) dynamic loss density coefficient for the undamaged material

\( k_{\text{hy}} \) hysteresis loss density coefficient

\( k_{\text{hy,dam}} \) hysteresis loss density coefficient for the damaged material

\( k_{\text{hy,un}} \) hysteresis loss density coefficient for the undamaged material

\( \tilde{k}_{\text{dy}} \) averaged dynamic loss coefficient over half of the sample width

\( \tilde{k}_{\text{hy}} \) averaged hysteresis loss coefficient over half of the sample width

\( L_{\text{av}} \) length of the flux path

\( m \) number of polynomial shape functions

\( M \) total magnetization (scalar)

\( M_{\text{an}} \) anhysteretic magnetization (scalar)

\( M_{\text{irr}} \) irreversible magnetization (scalar)

\( N_b \) number of turns in the B-coil
\( N_h \) number of turns in the H-coil

\( N_i, \ i = 1, \ldots, m \) finite-element shape functions

\( n_{el} \) order of finite-elements

\( n_{quad} \) order of quadrature

\( n_{int} \) number of integration points

\( n_{pol} \) order of the complete polynomial space

\( N_{pri} \) number of turns in the primary winding of toroidal samples

\( N_{sec} \) number of turns in the secondary winding of toroidal samples

\( \mathbf{p} \) vector of scalar Jiles-Atherton model parameters

\( p_{2D} \) 2-D eddy-current loss density

\( p_{2D,\text{low}} \) 2-D eddy-current loss density for low frequency

\( p_{\text{cl}} \) time-averaged eddy-current loss density

\( \mathbf{p}_{\text{dam}} \) vector of scalar Jiles-Atherton model parameters for the damaged material

\( p_{Fe} \) time-averaged iron loss density

\( p_{hy} \) time-averaged hysteresis loss density

\( \mathbf{p}_{un} \) vector of scalar Jiles-Atherton model parameters for the undamaged material

\( r_{in} \) inner radius of the toroidal samples

\( r_{out} \) outer radius of the toroidal samples

\( \mathbf{S} \) magnetic stiffness matrix

\( T \) magnitude of electric vector potential

\( \mathbf{T} \) electric vector potential

\( u_b \) induced voltage in the B-coil

\( u_h \) induced voltage in the H-coil

\( u_{pri} \) measured voltage in the primary winding of the toroidal samples

\( u_{sec} \) measured voltage in the secondary winding of the toroidal samples

\( w \) lamination width

\( w_k, \ k = 1, \ldots, n_{int} \) quadrature weights
Greek Symbols

δ degradation depth

Δν increase in the reluctivity of the magnetic material

Δk_{dy} increase in the dynamic loss coefficient

Δk_{hy} increase in the hysteresis loss coefficient

η_{dy} deterioration profile of the dynamic loss coefficient

η_{hy} deterioration profile of the hysteresis loss coefficient

η_{p} deterioration profile of the permeability

η_{r} deterioration profile of the reluctivity curve

λ model parameter for the 2-D analytical eddy-current model

μ permeability of the magnetic material

μ₀ permeability of free space

μ_{r} relative permeability of the magnetic material

ν reluctivity of the magnetic material

ν_{dam} reluctivity of the damaged material

ν_{un} reluctivity of the undamaged material

ρ mass density

σ electrical conductivity

τ scalar value of the mechanical stress

τ_{dy} model parameter for the exponential deterioration profile of the dynamic loss coefficient

τ_{g} generic representation of decay constant

τ_{hy} model parameter for the exponential deterioration profile of the hysteresis loss coefficient

τ_{r} model parameter for the exponential deterioration profile of the reluctivity curve

Ω reduced magnetic scalar potential

Ω_{d} finite-element solution domain
Subindices

av  average
cl  classical (loss)
dam damaged
dy  dynamic (loss)
ex  excess (loss)
Fe  iron (loss)
hy  hysteresis (loss) / hysteretic
in  inner / input
int integration
m   max
out outer / output
pri primary
sec secondary
sv  single-valued
tot total
un  undamaged

Abbreviations

1-D  one-dimensional
2-D  two-dimensional
3-D  three-dimensional
EDM  electrical discharge machining
FE   finite element
JA   Jiles-Atherton (model)
NO   non-oriented
PMSM permanent magnet synchronous motor
List of Symbols and Abbreviations

**RD**  rolling direction

**RSST**  rotational single-sheet tester

**SST**  single-sheet tester

**SV**  single-valued

**TD**  transverse direction
1. Introduction

1.1 Background

The global electricity demand has been steadily increasing due to population growth and advances in technology. In the first half of 2022, there was a 3% increase in demand globally compared to the previous years (Wiatros-Motyka and Dave, 2022). Along with this increase in demand, electricity prices have also risen due to global changes, reaching an average of €0.25 per kilowatt-hour (kWh) in the European Union in June 2022 (Eurostat, 2022). As a result of these increases in both demand and prices, huge expenditure on electricity consumption is increasing further.

Electrical motors are a significant contributor to electricity consumption, accounting for approximately 45% of global electricity use (Waide and Brunner, 2011). A considerable amount of the total power used by the electrical motors is lost due to different loss mechanisms including mechanical losses, copper losses, iron losses, and additional losses (IEC:60034-2-1, 2014), that occur during the operation of the device. Among others, one source of additional losses is attributed to the manufacturing processes of the ferromagnetic materials used in the machine parts. Therefore, besides optimizing the other loss mechanisms, accurate prediction of the additional losses caused by the manufacturing process can lead to significant global energy savings by increasing the efficiency of rotating machinery.

The manufacturing processes often involve cutting materials into specific shapes for use in electrical machines. However, this cutting process degrades the magnetic properties of the materials and increases iron losses (Moses et al., 2000). Additionally, when these materials are used in machines, they may be subjected to mechanical stresses due to the operating conditions of the machines, which can further impact iron losses (Gerada et al., 2011). Therefore, it is necessary to accurately characterize the effect of the cutting process and the impact of further mechanical stresses that these materials may experience to properly understand their impact on
iron losses. After the characterization, material models should be developed for further use in numerical simulation tools, which are commonly used to simulate and analyze electrical machines.

Even though thin electrical steel sheets (e.g., 0.30 mm–0.65 mm) are typically used in the stator laminations of the electrical machines to eliminate the eddy-current loss, the construction of the rotor poles of the large-diameter synchronous machines requires the usage of thick steel laminations (e.g., 1 mm–12 mm) for mechanical purposes. However, this leads to significant eddy-current loss, which classical loss models cannot predict due to the skin effects and the return path of the eddy currents. Thus, it is also essential to develop new characterization methods and numerically applicable material models to accurately represent how iron losses occur in thick materials.

After developing material models that account for the above-mentioned issues, the next step is to incorporate them into numerical simulation tools. This incorporation should be achieved in a way that is simple to implement, computationally efficient, and theoretically sound. Once the incorporation is complete, the machine can be simulated under certain conditions to evaluate the effect of incorporated models on iron losses and electrical and mechanical operating points.

This thesis aims to present an end-to-end methodology, involving experimental characterization of the effect of the cutting process, material modeling including cutting deterioration, incorporation of the material models into numerical machine simulation software, and the full simulation of the machine with the incorporated models, for realistic and computationally efficient simulations of a large-diameter synchronous machine. This methodology is expected to result in a design process where the final product conforms to the designed one.

1.2 Definitions

In this thesis, the term electrical steel sheets will refer to thin (0.30 mm to 0.65 mm) silicon-iron (Fe-Si) sheets typically used in the stator part of electrical machines. The term thick steel laminations will refer to the structural steel plates with a thickness greater than 1 mm, which are used in the rotor poles of large synchronous machines.

Macroscopic eddy currents will be referred to as eddy currents or classical eddy currents. The term low frequency or low-frequency approach will refer to the formulation of the eddy-current loss in the literature, for the frequencies where the skin effect is negligible, and the flux density is uniform across the width of the sheet.
1.3 Aim and Focus of the Thesis

This thesis aims to achieve an end-to-end methodology for realistic and computationally efficient simulations for large-diameter synchronous machines through four main objectives. The first objective is to experimentally characterize the combined effect of cutting and stress on M400-50A grade punched non-oriented (NO) electrical steel sheets and the effect of laser-cutting on 3 mm–12 mm thick S275JR grade structural steel laminations. The second objective is to develop magnetization and iron loss models to account for the cutting deterioration of the characterized materials, adapting existing classical models for the NO electrical steel sheets, and developing advanced magnetization and iron loss models for the thick steel laminations. Modeling the effect of stress is out of the scope of this thesis. The third objective is to develop a methodology for incorporating the developed material models for cutting deterioration into finite-element (FE) machine simulation software in a systematic, theoretically accurate, and computationally efficient manner. The final objective is to simulate a large-diameter synchronous machine to evaluate the effect of cutting on the machine's performance from various perspectives.

1.4 Scientific Contribution

The main scientific contributions of the thesis work can be listed as follows:

- A measurement procedure is developed to perform magnetic measurements to quantify the combined effect of punching and stress on electrical steel sheets using a modified single-sheet tester (SST).

- Effect of uniaxial stress on magnetic properties and iron losses of punched M400-50A grade NO electrical sheets were characterized based on the obtained measurement results through qualitative and quantitative analyses.

- A measurement procedure is developed to perform magnetic measurements to quantify the effect of laser-cutting on 3 mm–12 mm thick S275JR grade structural steel laminations with a ring-core measurement system.

- A 2-D axisymmetric FE model including the effect of cutting is proposed for thick steel laminations based on the measurement results of 12 mm thick samples. The eddy-current and hysteresis losses are modeled with high accuracy compared to the measurement results. The effect of cutting on the magnetization and the iron losses is quantified.
• A simple 2-D time-domain analytical model is proposed for eddy-current loss based on the solution of the 2-D field problem.

• A simple 2-D frequency-domain formula is derived for the special case of sinusoidal flux density. The skin effect is included with a correction factor using a phenomenological approach. The results are validated against a reference 2-D FE model.

• A novel methodology is proposed for the incorporation of cutting deterioration into electromagnetic FE simulation. The proposed approach is validated against the analytical solution for a 2-D beam with linear material properties. Then, the method is applied in the 2-D FE simulation of transformers, with a new systematic approach for selecting the required element size. Improved computational efficiency is achieved compared to the existing approaches.

• FE simulations of a large-diameter synchronous machine are performed with the incorporation of the developed models. The effect of cutting on the losses and operating points of the machine is analyzed by segregating the effect of each incorporated model.

1.5 Flow of the Research Work

A figure illustrating the flow of the presented research work is provided in Fig. 1.1. Each box in the figure shows the relationship between the work and related publications included in this dissertation.

Figure 1.1. The flow of the completed research work and its relationship to the publications included in this dissertation. Publications I–IV are denoted as (P1)–(P4).
1.6 Outline of the Thesis

This thesis is organized into five chapters. The current chapter serves as an introduction to the topic of the thesis, clarifying the aim and focus of the research, and summarizing the key scientific contributions. Chapter 2 reviews the relevant literature on the topic. Chapter 3 describes the methods used in the thesis work, including the measurement systems and numerical and analytical modeling tools used. Chapter 4 presents the applications of these methods and the obtained results from the performed studies. Finally, Chapter 5 includes the discussions on the findings and conclusions, and offers recommendations for future work.
2. Review of Relevant Research

This chapter provides a review of the research related to the focus of this thesis. The chapter begins by reviewing the effects of cutting and mechanical stress on ferromagnetic materials, followed by their experimental characterization. Next, the chapter covers the background information on modeling iron losses and constitutive material laws and their use for modeling cutting deterioration. Finally, the chapter discusses the use of these models in finite-element machine simulations and their impact on the performance of the machine.

2.1 Effect of Cutting and Mechanical Stress

This section provides an overview of the research on the effects of cutting processes and mechanical stress on ferromagnetic materials, with a particular focus on experimental studies. The findings are presented from a macroscopic perspective, and for clarity, frequently used magneto-mechanical concepts are briefly introduced at the beginning.

2.1.1 Magneto-Mechanical Concepts

The following are short definitions of frequently used concepts that will be mentioned in the upcoming subsections:

- **Plasticity** or **plastic deformation** refers to the ability of a solid material to undergo a permanent and irreversible change in shape in response to an external force (Lubliner, 2008).

- **Elasticity** or **elastic deformation** refers to the ability of a solid material to return to its original shape after the removal of an external force (Lubliner, 2008).

- **Magnetostriction** is the phenomenon in which applied magnetization causes mechanical deformation in magnetic materials. It was first observed by Joule (1847).
• **Villari effect** or **inverse magnetostriction** is the phenomenon in which the applied mechanical loading causes changes in the magnetic properties of ferromagnetic materials. It was first observed by Villari (1865).

• **Magnetic domains** are regions within a ferromagnetic material that have a uniform magnetization orientation. They were first proposed by Weiss (1906).

### 2.1.2 Effect of Cutting

Ferromagnetic materials used in electrical machinery are typically cut into specific shapes using one of four techniques: water-jet cutting, electrical discharge machining (EDM), laser cutting, and mechanical cutting methods like guillotining and punching. While EDM can only cut a single sheet at a time along a predetermined line, both laser and water-jet cutting can cut multiple sheets at once. Punching, on the other hand, can cut through a whole surface of a single sheet in one go using dies. There have been numerous studies that have examined the various effects of these cutting processes in terms of different aspects. This part of the thesis provides a summary of these findings based on research from the literature.

One of the earliest known studies on the effects of cutting on magnetic properties was conducted by Schmidt (1976). He measured the magnetic properties of Epstein samples punched into different numbers of strips and found that cutting increased losses and decreased the average permeability of the material. Later, Nakata et al. (1992) used search coils to measure flux density distributions along the electrical steel sheets. Other studies in the late 90s and early 21st century, including those by Smith and Edey (1995), Moses et al. (2000), and Boglietti et al. (2001), also observed negative impacts on iron losses and magnetization curves, although they used different measurement methodologies and had different objectives. Boglietti et al. (2001) quantified the increase in losses for different numbers of punched toroidal concentric rings and found that the increase in losses does not follow a proportional trend with the punched length or number of punched concentric rings. Smith and Edey (1995) also found that the effect of cutting is larger on materials with larger grain sizes.

Maurel et al. (2003) explains that the change in magnetic properties of materials during punching occurs through two mechanisms: plasticity, which causes a decrease in permeability along the cut edge, and geometry-dependent residual stresses caused by in-homogeneous plastic strains. While the reliability of measurement methods for residual stress is not clear (Kandil et al., 2001; Leuning et al., 2017), various methods have been used to study the magnetic properties of the plastically deformed zone resulting from the cutting process, such as a Kerr effect microscope (Senda
et al., 2006) and micro-hardness measurements (Hubert and Hug, 1995; Pulnikov et al., 2003; Araujo et al., 2010; Siebert et al., 2014). These studies have shown that sample geometry can affect the deterioration caused by the cutting process, and have observed changes in domain patterns and flux density distributions in punched and laser-cut samples.

Annealing, or the process of heating and cooling materials to alter their physical properties, has been shown to improve the magnetic properties of electrical steel sheets after cutting. For instance, Nakata et al. (1992) observed that annealing laser-cut samples resulted in improved magnetic properties. The exact mechanism by which annealing improves the magnetic properties of cut electrical steel sheets is not fully understood, but it is thought to involve the relaxation of residual stresses and the realignment of disordered magnetic domains within the material.

Several studies have compared different cutting techniques to assess their impact on magnetization and iron losses. Schoppa et al. (2003) and Manescu et al. (2020) compared water-jet cutting to mechanical cutting, while Sundaria et al. (2019) compared water-jet, mechanical, and laser cutting. These studies generally concluded that mechanical and laser cutting have more negative effects than water-jet cutting. Similarly, Boubaker et al. (2019) and Sundaria et al. (2020) demonstrated that EDM exhibits fewer negative effects than laser cutting, and Vandenbossche et al. (2010) showed that it has fewer negative effects than mechanical cutting. Loisos and Moses (2005), Araujo et al. (2010), Siebert et al. (2014), and Bali and Muetze (2015) have also compared mechanical and laser cutting and found that they exhibit different deterioration mechanisms. For example, Siebert et al. (2014) discovered that mechanical cutting pushes flux to the center of the sample, while laser cutting does not.

While there is a general agreement among studies on the negative effects of cutting on material properties, there are some discrepancies in the findings, such as the depth of degradation and the increase in iron losses. In their review, Bali and Muetze (2017) attribute these differences to various factors, including the material being studied, the cut geometry, and the conditions and settings of the cutting tool. The first two factors are quite clear and partially addressed before, while the impact of the cutting tool’s conditions and settings has also been studied. For example, Schmidt (1976) and Harstick et al. (2014) found that sharper punching tools have fewer negative effects, and Kuo et al. (2015) found that larger punching clearances lead to larger plastic deformations in the material, particularly in materials with larger grain sizes. Crevecoeur et al. (2008) also observed significant differences in the permeability of materials cut at different speeds, with lower speeds resulting in more deterioration. These factors should be considered when analyzing the effects of cutting on materials.

Existing literature has extensively analyzed the effects of various cutting techniques on electrical steel sheets, including comparisons between
different methods and discussions of external factors that influence cutting-related measurements. However, there has been limited investigation into the effects of additional elastic stress on cut samples due to mechanical loading, and the impact of cutting on thick materials has not been thoroughly studied.

2.1.3 Effect of Mechanical Stress

The magnetic materials used in energy conversion devices are subjected to both internal residual stress and external mechanical stress. The residual stress is often caused by manufacturing processes such as cutting, shrink fitting, and welding (Aydin, 2018; Singh, 2016). The external mechanical stress, on the other hand, is caused by magneto-mechanical forces that occur during the operation of the devices (Borisavljevic et al., 2010; Smith et al., 2010; Gerada et al., 2011). These two phenomena and their impact on the magnetization of magnetic materials have garnered significant attention in the literature and have been studied by many researchers. This part of the thesis provides a summary of these findings based on research from the literature.

Observations of the magneto-mechanical phenomena, such as magnetostriction by Joule (1847), inverse magnetostriction by Villari (1865), and the existence of the magnetic domains by Weiss (1906) led to numerous studies on the effect of mechanical stress on the domains of the magnetic materials. For instance, Bozorth and Williams (1945) conducted research on the impact of repeatedly applied small stress in the presence of a constant magnetic field and observed changes in the induction. Brown Jr (1949) derived a formula for an equivalent field to represent the effect of applied stress on domain walls under steady, low fields using the experimental observations made by Lange and Fink (1943). Then, he emphasized the effect of stress in relation to the orientation of the domain walls. In these studies, along with the others, the effect of tension and compression has been one focus of the discussions and investigations.

Bozorth (1951) stated that the magnetostriction behavior of a material affects its response to compression and tension. Materials with a positive magnetostriction constant experience a decrease in magnetization under compression and an increase in magnetization under tension along the magnetization direction. Conversely, materials with a negative magnetostriction constant experience an increase in magnetization under compression and a decrease in magnetization under tension along the magnetization direction.

After the developed theoretical background in the early studies, several experimental studies have been conducted in recent years to investigate the effect of mechanical stress on the magnetic properties and iron losses of the electrical steel sheets. While Miyagi et al. (2009, 2010) studied
the effect of compression, Leuning et al. (2016), Karthaus et al. (2017),
and Perevertov et al. (2015) studied the effect of tension. Additionally,
numerous researchers, including Perevertov (2017), LoBue et al. (2000),
Kai et al. (2011, 2012), Baghel et al. (2019), Rekik et al. (2014), and Aydin
(2018) investigated the effect of both compression and tension from vari-
ous aspects, such as the magnetization type (rotational and alternating),
direction of the applied stress (uniaxial and multiaxial), and material type
(grain oriented and non-oriented).

The results obtained from these investigations indicate that compression
leads to deterioration in magnetization and a rise in iron losses along the
direction of the applied stress. Conversely, low levels of tension improve
magnetization and reduce iron losses, while high levels of tension have simi-
lar effects to those of compression in the direction of applied stress. This
can be attributed to the change in behavior of a positive magnetostrictive
material beyond the Villari reversal point towards negative magnetostrictive
behavior (Moses, 1979). Furthermore, Miyagi et al. (2010), Leuning
et al. (2016), and Baghel et al. (2019) reported that the effect of stress on
magnetization and iron losses can vary depending on the orientation of
the magnetic field in relation to the rolling direction (RD) and transverse
direction (TD) of the material, which is a result of its anisotropic behavior
due to its crystallographic texture (Hubert et al., 2003).

While the literature studies selected for this review provide valuable
insights into how stress affects electrical steel sheets in terms of magne-
tization and iron losses, they do not specifically address the degradation
caused by cutting, or quantify the effect of stress on cut samples in relation
to the varying levels of deterioration caused by cutting. In order to un-
derstand the magneto-mechanical interactions within magnetic materials
more, it is more accurate to analyze the combined effects of cutting and
mechanical stress.

2.2 Experimental Characterization of Effect of Cutting and Uniaxial
Stress at Macroscopic Scale

This section provides an overview of the measurement systems used to
characterize the macroscopic magnetic properties of ferromagnetic mate-
rials used in energy conversion devices. Standard measurement systems
are first explained, along with key issues related to their use. The use of
these measurement systems in the characterization of the effects of cutting
and mechanical stress on the macroscopic scale is then described. Within
the scope of this thesis, the focus will be given to the uniaxial case for
mechanical stress.
2.2.1 Standard Magnetic Measurement Systems

There are three main standard measurement systems used for magnetic measurements: (i) Epstein frame (ASTM:A343/A343M-03, 2003; IEC:60404-2, 2008), (ii) ring-core system (ASTM:A927/A927M-99, 1999), and (iii) SST (IEC:60404-3, 2008) for alternating excitation. While there are no explicit standards for rotational excitation, rotational single-sheet testers (RSST) have been developed and used by various researchers, including Hasenzagl et al. (1996), Fonteyn and Belahcen (2008), and Aydin et al. (2019).

Each measurement system typically requires a programmable AC power supply to magnetize the sample through magnetizing cores. Because magnetic measurements are performed under specific magnetization conditions and materials have nonlinear characteristics, the flux density waveform over the measured area must be controlled by a computer through the power supply using often data acquisition device signals.

The magnetic characteristics of a material are measured once the desired waveform has been achieved. The quantities that are often of interest in these measurements are the average magnetic flux density $B_0$ and the magnetic field strength at the lamination surface $H_s$. In the Epstein frame and ring-core systems, $B_0$ is obtained from the induced back-electromotive force in the secondary winding terminals, and $H_s$ is obtained from the magnetomotive force created in the primary winding. In the SST measurement system, $B_0$ is usually determined using a search coil, known as a B-coil, that surrounds the sample being measured, or by using needle probes. $H_s$ is typically measured using a search coil, known as an H-coil, placed on the sample surface.

2.2.2 Experimental Characterization of Effect of Cutting

The characterization of magnetic materials that have undergone cutting processes is commonly performed using standard measurement systems. There are two main methods for these measurements: (i) local measurement of magnetization quantities and (ii) measurement of magnetization quantities averaged over the entire sample. This part of the thesis gives an overview of these methods.

The first approach involves partitioning the sample into different local regions and using search coils to measure the magnetization quantities within each region. This method was, for instance, used to measure the flux density distribution of NO electrical steel sheets cut by shears by Nakata et al. (1992) and by Peksoz et al. (2008).

The second approach involves using one set of search coils to measure the magnetization quantities averaged over the entire sample. This method has been used in two ways. The first way involves measuring only two sample groups: one that is partially damaged (either annealed or EDM-cut)
and another that is fully damaged (usually cut into several pieces). Gmyrek et al. (2013) used this approach to characterize 0.50 mm thick punched materials using ring-core measurements, and Bali et al. (2016) used it to characterize mechanically cut Epstein frame samples. The second way involves assembling several sample groups with the same external geometry from different numbers of equally wide pieces (strips or concentric rings) and measuring them. This method is used to obtain magnetization quantities from samples with different levels of cutting deterioration and investigate the variation as a function of the deterioration. Examples include Vandenbossche et al. (2010) who measured 0.35 mm thick sample groups with an 80 mm × 260 mm external geometry cut into 1–22 strips, Elfgen et al. (2016) who measured 0.35 mm thick laser-cut sample groups assembled from 1–30 strips with a 120 mm × 120 mm external geometry, and Sundaria et al. (2020) who measured 0.50 mm thick laser-cut Epstein frame samples assembled from 1–10 strips.

In these studies, the experiments were typically conducted under stress-free conditions and did not consider the effects of additional mechanical loading. Additionally, most of the research on the effects of cutting has focused on thin electrical steel sheets with limited research on thick materials.

### 2.2.3 Experimental Characterization of Effect of Uniaxial Stress

The characterization of magnetic materials under mechanical loading requires the use of measurement systems with the ability to apply stress. As there are no established standards in the presence of mechanical loading, different approaches exist in terms of the configuration of the measurement system and the method of applying stress. This part of the thesis gives an overview of these approaches.

In terms of the measurement system, it is common to use self-made or modified SST with the capability of stress application using typically rectangular samples. Many studies including Dabrowski and Zgodzinski (1989), LoBue et al. (2000), Permiakov et al. (2002), Pulnikov et al. (2003), Miyagi et al. (2010), Kanada et al. (2011), and Singh (2016) have used these devices to measure the magnetic properties of 0.30 mm to 0.50 mm thick rectangular electrical steel sheets with various dimensions under mechanical loading. Krell et al. (2000) used a different approach, measuring the magnetic properties of a hexagonal electrical steel sheet in an RSST under uniaxial mechanical loading.

In their studies, LoBue et al. (2000) and Singh (2016) utilized spring mechanisms to apply a predetermined level of stress, which was measured using a load cell. Pulnikov et al. (2003) applied stress using two pairs of brass grips with abrasive layers that were insulated from each other. Krell et al. (2000) employed a clamping device mounted on the sample to
apply stress, while Kanada et al. (2011) used a moving stress load unit to apply the stress in their study. The other studies by Dabrowski and Zgodzinski (1989), Permiakov et al. (2002), and Miyagi et al. (2010), did not explicitly detail the stress application mechanism. To ensure uniform stress distribution, Krell et al. (2000) and Miyagi et al. (2010) used strain gauges located in the measurement areas.

The average magnetic flux density $B_0$ was measured using B-coils (Miyagi et al., 2010; Dabrowski and Zgodzinski, 1989; Kanada et al., 2011; Singh, 2016) or needle probes (Krell et al., 2000; Permiakov et al., 2002; Pulnikov et al., 2003). The magnetic field strength at the lamination surface $H_s$ was often measured using H-coils (Dabrowski and Zgodzinski, 1989; Krell et al., 2000; Permiakov et al., 2002; Pulnikov et al., 2003; Miyagi et al., 2010; Kanada et al., 2011). Alternatively, Singh (2016) used tunneling magneto-resistance sensors embedded on a printed circuit board to measure $H_s$.

In these studies, the explicit reference to the degradation resulting from cutting was absent, and the effect of stress on the cut samples was not quantified in relation to the varying levels of deterioration caused by cutting. Considering that the cutting deterioration and the effects of additional mechanical stress often exist together in reality, it is important to study their combined effect on magnetization and iron losses.

2.3 Modeling Iron Losses and Constitutive Material Law

Iron losses in ferromagnetic materials can be broadly divided into two categories: static hysteresis and dynamic losses. Hysteresis loss occurs as a result of the discontinuous domain wall motion within the material under magnetization, a phenomenon attributed to impurities present in the material. Dynamic loss, on the other hand, is caused by time-varying magnetization and can be further divided into classical eddy-current loss and excess loss. Classical eddy-current loss is caused by macroscopic eddy currents, which are not influenced by the material's domain structure, while excess loss is a result of the movement of domain walls during magnetization (Bertotti, 1988).

Constitutive material law for ferromagnetic materials is often used to express the relation between the vectors of magnetic flux density $B$ and magnetic field strength $H$. Although the relation itself is hysteretic by nature, the single-valued (SV) constitutive law is also commonly used in the modeling for simplicity. In terms of the hysteretic constitutive laws used in the modeling, different approaches exist, and the most common ones will be detailed in the following subsections.

The remainder of this section provides an overview of the different approaches to modeling iron losses in the literature. Empirical formulations commonly used in the post-processing of simulations are discussed first.
Then, theoretical models for hysteresis and physical models for eddy-current losses are examined. In this thesis, excess losses are used as a part of the empirical iron loss models, and advanced modeling of excess losses is beyond the scope of this thesis.

### 2.3.1 Empirical Iron Loss Models

The development of the empirical formulations for iron losses in the literature starts with the Steinmetz equation (Steinmetz, 1892), which expresses the iron loss density $p_{Fe}$ of a material under sinusoidal excitation as a function of frequency $f$ and flux density amplitude $B_m$ such that

$$p_{Fe} = k_{SE} B_m^{a_{SE}} f^{\beta_{SE}}$$  \hspace{0.5cm} (2.1)

where $k_{SE}$, $a_{SE}$, and $\beta_{SE}$ are the coefficients that need to be identified based on the measurement data. This model was later extended by Jordan (1924), who separated the iron loss density $p_{Fe}$ into hysteresis loss $p_{hy}$ and dynamic eddy-current loss $p_{dy}$ components with coefficients $k_{hy}$ and $k_{dy}$ by

$$p_{Fe} = k_{hy} B_m^2 f^2 + k_{dy} B_m^2 f^2.$$  \hspace{0.5cm} (2.2)

Bertotti (1988) then proposed a statistical three-component formula by further separating $p_{Fe}$ into hysteresis loss $p_{hy}$, classical loss $p_{cl}$, and excess loss $p_{ex}$ components with coefficients $k_{hy}$, $k_{cl}$, and $k_{ex}$ such that

$$p_{Fe} = k_{hy} B_m^2 f^2 + k_{cl} B_m^2 f^2 + k_{ex} B_m^{1.5} f^{1.5}.$$  \hspace{0.5cm} (2.3)

The classical loss $p_{cl}$ in this formula is generally calculated using the analytical formula presented by Lammeraner and Štafl (1966), which expresses $p_{cl}$ for laminations with a thickness $d$ significantly smaller than the lamination width $w$ (i.e. $d << w$) in the case of a uniform sinusoidal flux density as follows:

$$p_{cl} = \frac{\sigma d^2 \pi^2}{6 \rho} B_m^2 f^2$$  \hspace{0.5cm} (2.4)

where $\sigma$ is the electrical conductivity and $\rho$ is the mass density, and $k_{cl}$ is usually referred to as the (classical) Foucault eddy-current loss coefficient in the literature. On the other hand, the determination of the excess and hysteresis loss coefficients $k_{ex}$ and $k_{hy}$ in (2.3) is based on a fitting procedure using several measurements under different sinusoidal magnetization levels and frequencies (Pluta, 2010; Rasilo, 2012).

The classical loss formula in (2.4) was developed for frequencies where the skin effect is negligible. However, this calculation becomes less accurate
for frequencies where the skin effect becomes dominant (Bertotti, 1998). To account for the skin effect in the loss calculation, Gyselinck et al. (1999) included a skin effect correction factor $F_{\text{skin}}$, developed for linear magnetic materials by Lammeraner and Štafl (1966), as follows:

$$p_{\text{Fe}} = k_{\text{hy}} B_m^2 f + F_{\text{skin}} k_{\text{cl}} B_m^2 f^2 + k_{\text{ex}} B_m^{1.5} f^{1.5}.$$  

(2.5)

Due to the poor performance of Bertotti’s model for the large fields where the magnetic flux density saturates, modifications to the classical eddy-current loss term were proposed by Jacobs et al. (2009) and Eggers et al. (2012). With the inclusion of the skin effect correction factor $F_{\text{skin}}$, Eggers et al. (2012) expressed the formula, so-called IEM-Formula, such that

$$p_{\text{Fe}} = a_1 B_m^2 f + F_{\text{skin}} a_2 B_m^2 f^2 (1 + a_3 B_m^{a_4}) + a_5 B_m^{1.5} f^{1.5}$$  

(2.6)

where $a_1 - a_5$ are obtained from the mathematical fitting using the measured data. Here, $(1 + a_3 B_m^{a_4})$ factor is used to account for the saturation in the magnetic flux density. The same factor was also used by Jacobs et al. (2009) as a correction factor for Jordan’s formula.

Among these presented empirical formulas, Bertotti’s three-components formula (2.3) is the most widely used one in the literature for estimating iron losses under sinusoidal excitation. However, this model does not accurately estimate losses for non-sinusoidal flux density (Fiorillo and Novikov, 1990; Kowal et al., 2015). To address this limitation, Fiorillo and Novikov (1990), Amar and Kaczmarek (1995), and Roshen (2007) have proposed methods for incorporating non-sinusoidal waveforms in loss calculations. Another issue with Bertotti’s model is its poor estimation of losses in the saturation region, which has been addressed by Jacobs et al. (2009) and Eggers et al. (2012) and continues to be a topic of active research.

### 2.3.2 Theoretical Hysteresis Models

Theoretical hysteresis models used for the ferromagnetic materials are mathematical representations of the hysteretic constitutive law between the vectors of magnetic flux density $B$ and magnetic field strength $H$. In this thesis, existing hysteresis models in material modeling studies have been utilized. Therefore, this section provides a brief overview of hysteresis models and regarding concepts, followed by a summary of commonly used models and their variations, and final discussions on their performance.

#### Background and Concepts

Modeling of the hysteresis phenomenon for magnetic materials is commonly based on the modeling approaches suggested by Preisach (1935) and Jiles and Atherton (1984), as well as variations of these approaches. For both approaches, several categories exist, including the ones based on scalar/vector models and forward/inverse models.
In the scalar models, \( \mathbf{B} \) and \( \mathbf{H} \) are treated as unidirectional, and their scalar components \( B \) and \( H \) are used in the modeling. In contrast, vector models take into account the rotational flux that occurs in electrical machines and include the relationship between the \( \mathbf{B} \) and \( \mathbf{H} \) in the model.

In the forward models, \( \mathbf{H} \) is the input, and \( \mathbf{B} \) is obtained through modeling. In contrast, in the FE modeling of the magnetic materials and electrical machines, \( \mathbf{B} \) is typically used as the input to the constitutive law, i.e., \( \mathbf{H}(\mathbf{B}) \). Therefore, inverse models are developed to take \( \mathbf{B} \) as an input and output the corresponding \( \mathbf{H} \).

The following subsections provide a summary of commonly used Jiles-Atherton (JA) and Preisach models, along with their variations. A special focus will be given to the formulation of the scalar JA model, which is used in the modeling of thick steel laminations as a part of this thesis.

**Jiles-Atherton Model and its Variations**

JA model, initially introduced by Jiles and Atherton (1984), is based on expressing the relationship between the magnetic domains and externally applied magnetic field. Jiles et al. (1992) explained the governing equations of the scalar JA model, which requires the identification of five model parameters. The scalar JA model, with a small modification made by Bergqvist (1996), can be summarized with the following equations:

\[
H_{\text{eff}} = H + \alpha M
\]

\[
M = cM_{\text{an}} + (1 - c)M_{\text{irr}}
\]

\[
M_{\text{an}} = M_s \left( \coth \left( \frac{H_{\text{eff}}}{a} \right) - \frac{a}{H_{\text{eff}}} \right)
\]

\[
\frac{dM_{\text{irr}}}{dH_{\text{eff}}} = \begin{cases} \frac{|M_{\text{an}} - M_{\text{irr}}|}{k}, & \text{if } dH \cdot (M_{\text{an}} - M_{\text{irr}}) > 0 \\ 0, & \text{otherwise} \end{cases}
\]

\[
B = \mu_0 \cdot (H + M)
\]

where \( H \) and \( H_{\text{eff}} \) are considered as applied and effective magnetic field strengths, respectively. \( M, M_{\text{an}}, \) and \( M_{\text{irr}} \) represent total, anhysteretic, and irreversible magnetizations, respectively. \( \mathbf{p} = [\alpha \ c \ k \ M_s \ a]^T \) is a vector of model parameters to be identified. Later, Sadowski et al. (2002) inverted the scalar JA model for a simpler implementation into FE analysis of electromagnetic field problems. Leite et al. (2004) then proposed an inverse vector JA model based on this study. Subsequent research has aimed to improve the parameter identification (Marion et al., 2008; Toman et al., 2008) and the implementation of the model into FE simulations (Hoffmann et al., 2017).
**Preisach Model and its Variations**

Preisach model, initially introduced by Preisach (1935), is a mathematical model that describes the hysteresis behavior of ferromagnetic materials in response to changes in the applied magnetic field. It is based on modeling the relationship between the magnetic field strength and magnetic flux density with the combination of elementary hysteresis operators. The geometric interpretation of the Preisach model was presented by Mayergoyz (1986) with the introduced Everett function. Then, Mayergoyz (1988) proposed a vector version of the Preisach model. Dlala et al. (2006) inverted the Preisach model for a simpler implementation into FE analysis of electromagnetic field problems and compared the results with the forward Preisach model. Later, Dlala, Belahcen and Arkkio (2010) also proposed a method to improve the loss properties of the Mayergoyz vector hysteresis model. Hussain and Lowther (2017) reported that while the Preisach model is highly accurate, it can be computationally expensive and suggested an efficient implementation method.

**Discussions and Further Considerations**

Benabou et al. (2003) compared the Preisach model with the Everett function and the JA model in terms of their accuracy and incorporation into FE analysis using three different materials under different excitation. It was reported that while the Preisach model outperforms in terms of accuracy for all materials and excitations, the JA model outperforms in terms of identification effort and computational effort for the numerical incorporation. In this thesis, due to its light computation and simple identification procedure, the scalar JA model is used to model the hysteretic behavior of thick steel laminations as a part of FE modeling.

**2.3.3 Physical Eddy-Current Models for Laminations**

Eddy currents in a conducting medium are caused by time-varying magnetic flux density according to Faraday’s law and linked with the magnetic field strength through Ampere’s law. Therefore, the physical eddy-current loss problem along the lamination cross-section is expressed by combining Ampere’s and Faraday’s laws, for which the solutions are obtained using different analytical or numerical methods based on the magnetic vector potential formulations. This section begins by explaining the general formulation of the problem. Then, it focuses on the suggested approaches to solve the eddy-current problems, followed by final discussions and further considerations.
General Formulation

Considering a general quasi-static 3-D problem in the Cartesian coordinate system, Ampere’s and Faraday’s laws can be written as follows:

\[
\nabla \times \mathbf{H}(x,y,z,t) = \mathbf{J}(x,y,z,t) \quad (2.12)
\]

\[
\nabla \times \mathbf{E}(x,y,z,t) = -\frac{\partial \mathbf{B}(x,y,z,t)}{\partial t} \quad (2.13)
\]

where \( \mathbf{H}, \mathbf{J}, \mathbf{E}, \) and \( \mathbf{B} \) are vectors of magnetic field strength, electric current density, electric field strength, and magnetic flux density, respectively. Considering the constitutive law \( \mathbf{J} = \sigma \mathbf{E} \), where \( \sigma \) is the electrical conductivity and constant in the region, the combination of (2.12) and (2.13) yields the eddy-current problem to be solved

\[
\nabla \times \nabla \times \mathbf{H}(x,y,z,t) = -\sigma \frac{\partial \mathbf{B}(x,y,z,t)}{\partial t}. \quad (2.14)
\]

As solving the 3-D problems is computationally expensive (Silva et al., 1996), a solution to (2.14) is typically sought in the lower dimensional problems. Typically, the lamination is considered to lie in a 2-D plane, along which \( \mathbf{B} \) and \( \mathbf{H} \) are aligned. The eddy currents are considered to flow perpendicular to \( \mathbf{B} \) and \( \mathbf{H} \). Correspondingly, \( \mathbf{J} \) and \( \mathbf{E} \) are aligned.

Mostly, the lamination width \( w \) is considered to be so much larger than the lamination thickness \( d \) (i.e., \( w >> d \)). The edge effects, i.e., the return path of the eddy currents at the lamination edges, are ignored. Consequently, the eddy currents are modeled in 1-D (see Fig. 2.1(a)). Rarely, the edge effects are included in the problem setting, and the eddy currents are modeled in 2-D (see Fig. 2.1(b)).

![Figure 2.1. An example problem setting for the eddy-current problems (a) for 1-D and (b) 2-D cases.](image)

The following subsections provide a summary of 1-D and 2-D modeling of the eddy-currents with the formulations and the solution methods based on the problem setting shown in Fig. 2.1. More focus will be given to 1-D modeling, which is more commonly used in the literature. 2-D modeling for eddy currents has been used so far for the validation of the 1-D models.
Review of Relevant Research

1-D Eddy-Current Modeling

Considering a similar problem setting in Fig. 2.1(a), (2.14) becomes

\[
\nabla \times \nabla \times H(z, t) = -\sigma \frac{\partial B(z, t)}{\partial t} \tag{2.15}
\]

which was defined by Vecchio (1982) as a 1-D diffusion equation with

\[
\frac{\partial^2 H(z, t)}{\partial z^2} = \sigma \frac{\partial B(z, t)}{\partial t}. \tag{2.16}
\]

Bertotti (1998), in his low-frequency approach, assumed a uniform flux density \(B_0(t)\) along the lamination cross-section and obtained the solution of (2.16) from the double integration by satisfying the material law \(H_{Fe}(B)\) on average such that

\[
H_s(t) = H_{Fe}(B_0(t)) + \sigma \frac{d^2 B_0(t)}{dt^2}, \tag{2.17}
\]

where \(H_s\) is the magnetic field strength at the lamination surface. The average iron loss density \(p_{Fe}\) over one period \(T\) of a closed cycle can be computed using (2.17) such that

\[
p_{Fe} = \frac{1}{\rho T} \int_0^T H_s(t) \frac{d B_0(t)}{dt} dt, \tag{2.18}
\]

where \(\rho\) is the mass density. When \(B_0(t)\) is sinusoidal, the integration of the dynamic term of \(H_s(t)\) gives the classical loss in (2.4).

Bottauscio et al. (2000), Bottauscio and Chiampi (2002), and Dlala et al. (2008) accounted for the skin effect by numerically solving the 1-D eddy-current problem coupled with a 2-D magnetic field problem using a hysteretic constitutive law. Alternatively, Gyselinck et al. (2006) and Dular (2008) proposed homogenization approaches to account for the skin effect. The magnetic flux density along the lamination cross-section was approximated using a set of orthogonal basis functions, and SV constitutive law was considered. Rasilo et al. (2011) adopted a similar approach using a hysteretic constitutive law and reported that the model reduces to (2.17) when the magnetic flux density is uniform. Later, Krüttgen et al. (2017) proposed a parametric algebraic model to reduce the degree of freedom for the incorporation of the 1-D lamination models suggested by Gyselinck et al. (2006) and Rasilo et al. (2011) into 2-D FE simulations.

2-D Eddy-Current Modeling

Considering a similar problem setting in Fig. 2.1(b), (2.14) becomes

\[
\nabla \times \nabla \times H(y, z, t) = -\sigma \frac{\partial B(y, z, t)}{\partial t}. \tag{2.19}
\]

To validate the results of their 1-D eddy-current models, Bottauscio et al. (2000) and Rasilo et al. (2019) numerically solved a similar problem to
Review of Relevant Research

(2.19) in cylindrical coordinates assuming a unidirectional \( B \) perpendicular to the 2-D axisymmetric cross-section. Similarly, Elfgen et al. (2020) also solved the problem in (2.19) numerically, assuming a unidirectional \( B \) perpendicular to the 2-D lamination cross-section in the \( yz \)-plane for comparison with the 1-D case.

The analytical solution of (2.19) has been studied only for linear materials. The solution can be obtained through the separation of variables method, as detailed by Voipio (1987). However, an analytical solution of (2.19) for nonlinear materials has not been studied.

Discussions and Further Considerations

The literature review shows that 1-D eddy-current models have been studied widely due to their simplicity compared to higher dimensional modeling. On the other hand, 2-D eddy-current models have been used for mostly validation purposes for 1-D models. Although 1-D eddy-current models have given satisfactory results for thin laminations compared to experimental measurements when \( w \gg d \), their applicability for thick laminations is questionable due to the edge effects when \( w \approx d \).

2.4 Modeling Cutting Deterioration

This section provides an overview of the macroscopic models that have been developed to take into account the effect of cutting deterioration. These models will be divided into two categories: magnetization models and iron loss models. The term "magnetization" is used as a general expression for the material law, which may take the form of SV permeability or reluctivity expressions, or be based on the theoretical hysteresis models.

2.4.1 Magnetization Models with Cutting Deterioration

Magnetization models in the literature attempt to analytically describe the change of the material law within a cut material. To achieve this, two major modeling approaches have been adopted: (i) modeling based on the magnetic measurements of the field quantities (i.e., \( B_0 \) and \( H_s \)) and (ii) modeling based on the local stress/strain measurements or simulations. This part of the thesis gives an overview of these approaches.

One popular approach to modeling based on magnetic measurements of field quantities is to consider damaged and undamaged zones in the cut material, with homogeneous material laws and the identification of a degradation depth. This approach, used by Gmyrek et al. (2013) and Holopainen et al. (2017), has resulted in satisfactory results mathematically. However, the physical soundness of this approach is controversial due to the assumption of homogeneous material laws in the identified
regions.

An alternative approach is to use continuous local material modeling, which is based on modeling the magnetization of the material with a deterioration profile beginning from the cut edge and extending toward the middle of the material, with the cut edge being the most damaged part. For instance, Schoppa (2001) modeled the magnetic polarization over the strip width using a hyperbolic formula based on the conservation of energy principle. Peksoz et al. (2008) modeled the flux density distribution from the cutting edge based on local measurements with an exponential deterioration profile. Vandenbossche et al. (2010) applied continuous modeling in the form of permeability, expressing the permeability drop using a quadratic profile. Sundaria et al. (2020) adopted a similar approach to Vandenbossche et al. (2010) but with an exponential deterioration profile. Elfgen et al. (2016) compared the methods suggested by Schoppa (2001) and Vandenbossche et al. (2010) in terms of their applicability in FE simulation and found the latter more applicable, using it to model a toroidal core. Elfgen et al. (2017) also used a continuous local modeling approach, expressing the flux density as a combination of two flux density expressions for damaged and undamaged materials to be identified.

In addition to the developed models based on average magnetization quantities, stress/strain-dependent models have also been used in several studies. These models express the constitutive material law based on modeling of plastic strain inside the material after the cutting process, using measurements or simulations of the plastic strain distribution. For example, Ossart et al. (2000) expressed the flux density distribution as a function of magnetic field strength and plastic strain, modeled based on hardness measurements. Crevecoeur et al. (2008) obtained the magnetic properties by modeling the plastic strain as a function of distance from the cutting edge. M’zali et al. (2020) simulated the punching process, identified the average plastic strain with an exponential profile, and then modeled the material degradation due to punching using a magneto-plastic model.

So far, the main modeling approaches and principles have been summarized. It can be seen that different deterioration profiles are used in the modeling (e.g., exponential and quadratic). While their superiority is controversial, they are both widely used. In the methods of this work, a judgment on their physical superiority was not made, and both profiles were used for different applications.

### 2.4.2 Iron Loss Models with Cutting Deterioration

The cutting degradation has been included in the iron loss calculation of thin electrical steel sheets often by introducing correction factors to the empirical iron loss formulations covered in Section 2.3.1. The parameters of the empirical iron loss formulations and correction factors are typically
identified based on the magnetic measurements. In the development of these correction factors, two main approaches have been widely used: (i) adjusting the loss coefficients based on the sample geometry and (ii) expressing the loss coefficients as a function of distance from the cutting edge. Another method for determining iron losses is through the use of finite element analysis to solve for the field quantities such as magnetic field strength, magnetic flux density, and electric current density. This part of the thesis gives an overview of these approaches.

Correction factors developed based on sample geometry often incorporate the sample width or cutting length into their formulation. For example, Liu et al. (2008) introduced a "building factor" that depends on the sample width and two fitting coefficients, which is applied to Bertotti’s loss calculation to account for samples with different widths than Epstein frame samples. Steentjes et al. (2014), on the other hand, expressed the hysteresis coefficient of the IEM-Formula as a function of cutting length and per unit mass.

The second type of correction factors developed for thin electrical sheets consider the materials fully damaged in the vicinity of the cutting edge and models the loss coefficient continuously from the cutting edge with deterioration profiles. Vandenbossche et al. (2010) used this approach to modify the hysteresis loss coefficient of the formula developed by Jacobs et al. (2009). Sundaria et al. (2020) adopted a similar approach, introducing increases in the hysteresis and excess loss coefficients of Bertotti’s formula that vary exponentially from the cut edge. These modeled coefficients were identified based on the magnetic measurements of the electrical steel sheets assembled from different numbers of cut strips. Similarly, Martin et al. (2018) expressed the loss coefficients of Bertotti’s formula with different cumulative Gumbel distributions, which were identified based on the measurements and 2-D FE modeling of the electrical steel sheets.

While it is often simpler to calculate iron losses including the effect of cutting using empirical correction factors in post-processing, these formulas may not be accurate for thick materials or at high frequencies due to the influence of the skin effect. A more accurate, but computationally more expensive approach is to calculate the iron losses from the FE solution of the field quantities with the full inclusion of loss mechanisms (hysteresis and eddy currents) and cutting deterioration in the magnetization. For example, Elfgen et al. (2020) calculated hysteresis and eddy-current losses along the lamination cross-section from a 2-D FE solution of the field quantities using a hysteretic constitutive law with a continuous material model that accounts for cutting deterioration. However, this approach was studied only for thin electrical steel sheets.

While several models for predicting iron losses in thin electrical steel sheets that account for cutting deterioration have been developed, there is
still ongoing work to improve the prediction of these losses in the saturation region and to account for the skin effect. Furthermore, the cutting effect on iron losses in thick materials, where skin and edge effects are significant even at low frequencies, has not been extensively studied. Further research is needed in this area to address these gaps in understanding.

2.5 Finite-Element Machine Simulation Including Cutting Deterioration

To accurately simulate energy conversion devices, it is necessary to properly incorporate material models for cutting deterioration into FE simulations. However, incorporation of these models can be challenging, as they can cause issues with numerical integration, choice of appropriate FE order, and computational time. Once these incorporation challenges are addressed, machine simulations can be performed, allowing the impact of cutting deterioration on machine performance to be evaluated from various perspectives.

The remainder of this section first provides an overview of the approaches used to calculate iron losses in FE simulations. It then focuses on the existing methods for addressing the challenges in the incorporation of cutting deterioration, as well as the key findings on the impact of cutting deterioration on machine performance.

2.5.1 Modeling Iron Losses in Finite-Element Machine Simulation

The methods for modeling the iron losses in FE machine simulation can be classified into two categories: (i) iron-loss calculation in the post-processing of FE field solution and (ii) incorporation of the loss mechanisms into FE field solution. Both methods have been studied widely and used in the field, and there have also been some comparative studies on their effectiveness and limitations. This part of the thesis provides a summary of these models as well as the findings of the comparative studies.

Iron loss calculation in the post-processing involves solving the field solution of a 2-D FE machine problem typically with an SV constitutive relationship, obtaining the magnetic flux density distribution, and using the empirical iron loss models (e.g., Bertotti, Jordan, and IEM formulas) covered in Section 2.3.1 with pre-identified coefficients. Due to its simplicity in the implementation and computational advantage, it is by far the most used approach by several studies, including Atallah et al. (1992), Ionel et al. (2007), Hargreaves et al. (2012), Fratila et al. (2017), and Mohammadi et al. (2022). However, some drawbacks have also been noted with this approach, such as the limitation of empirical loss models for
low-frequency applications and the independence of the loss calculation from the magnetic properties of the material (Dlala, 2009).

To address the issues with the post-processing calculation, the incorporation of the loss mechanisms into the FE field solution has been investigated using different implementation approaches. One approach has been coupling a 1-D FE eddy-current model along the lamination with the 2-D FE field solution. This approach has been adopted by Dlala et al. (2008) with the vector hysteresis model of Mayergoyz, and Pippuri and Arkkio (2009) with complex reluctivity for hysteresis in the simulation of induction machines. Narita et al. (2015) used a similar approach to account for the eddy currents in the FE simulation of a switched reluctance motor. As an alternative to using the 1-D FE eddy-current model, Rasilo et al. (2011) used a homogenization technique to approximate the flux density distribution along the lamination cross-section and then coupled it with the 2-D FE field solution, which was obtained with an inverse vector Preisach hysteresis model-based constitutive law.

An alternative approach for the incorporation of loss mechanisms into the FE field solution has been modifying the applied magnetic field expression through the inclusion of the contribution of magnetic field strength for iron loss components. Gyselinck et al. (1999) used this approach to account for the classical eddy-current loss for an SV reluctivity case, and Gyselinck et al. (2000) further included inverse vector Preisach model to account for the hysteresis loss in the simulation of induction motors. Similarly, Dlala (2009) modeled classical eddy-current and excess losses with Mayergoz’s vector hysteresis model-based constitutive law to simulate an induction motor, which he referred to as a hybrid approach due to its simplicity and stability, similar to the post-processing approach, as well as its accuracy and generality, similar to the 1-D/2-D coupling approach.

Comparative studies have also been conducted to investigate the importance of incorporating the loss mechanisms into FE field solutions. Dlala, Belahcen and Arkkio (2010) compared two cases for the simulation of four induction machines at different rated powers, one using an SV constitutive law and without including the eddy-current and excess losses, one using a Mayergoz’s vector hysteresis model-based constitutive law with the inclusion of eddy-current and excess losses through the applied magnetic field. The study showed that the inclusion of the loss mechanisms into the FE field solution improves the accuracy of loss estimates, but does not significantly affect other quantities such as power factor, input power, electric current, and speed.

Rasilo et al. (2012) conducted a similar study for three cases, one with the full inclusion of eddy-current and excess losses using an inverse vector Preisach hysteresis model-based constitutive law, one with the inclusion of eddy-current loss using an SV constitutive law and calculation of the other losses in the post-processing, and one with neglecting the iron losses.
in the field solution and calculating them in the post-processing. The study showed similar findings to the one conducted by Dlala, Belahcen and Arkkio (2010) in terms of the effect on the electrical and mechanical operating characteristics. Additionally, it was reported that while the inclusion of the eddy currents is important for predicting iron losses and the electromagnetic torque accurately, the inclusion of hysteretic material properties does not have a significant effect on the iron losses or global quantities.

2.5.2 Incorporation of Cutting Deterioration into Finite-Element Machine Simulation

To accurately simulate electrical machines, it is necessary to incorporate magnetization and iron loss models that include cutting deterioration into the FE machine simulation. While the iron loss models accounting for the cutting deterioration have been commonly used in the post-processing of the FE simulations of the machine, there are two main implementation practices for the magnetization models: (i) dividing the simulated geometry into small regions with a unique magnetic property and (ii) continuously modeling the magnetic property as a function of distance from the cutting edge. While the first approach is easier to implement, the second approach is physically more accurate. However, it has implementation challenges in terms of different aspects, including numerical integration, the choice of appropriate FE order, and computational time. This part of the thesis discusses the implementation approaches used in previous literature, as well as strategies for addressing implementation challenges, with a particular focus on continuous modeling approaches.

In the first approach, multiple regions or layers are created within the machine geometry and characterized by unique magnetization curves. Vandenbossche et al. (2013) used this approach in the FE simulation of an induction machine with 5 regions in the stator and 5 regions in the rotor. Bali et al. (2014) used a similar approach in the FE simulation of a capacitor motor and a synchronous generator with an unspecified number of layers. Lazari et al. (2015) performed FE simulations for a permanent magnet-assisted synchronous reluctance motor, including the effect of cutting on the magnetization of the stator material, using 3 distinct regions. Sano et al. (2016) also analyzed the effect of cutting on a permanent magnet synchronous motor (PMSM) through FE simulations by dividing the stator into 6 layers and applying specific magnetization curves to each layer. None of these studies provided information on the order of the elements or the selection of the Gauss integration points.

The second approach requires sensitive calculation of the distance to the cutting edge, which is then used for obtaining the local magnetization characteristics for each FE in a continuous manner. Elfgen et al. (2017)
modeled the reluctivity curve with a quadratic deterioration profile, simulated several reluctivity curves at different distances, and stored the distance to the cutting edge for each FE for the simulation of a PMSM. During the FE solution, the reluctivity curves for each FE were extracted through interpolation. However, no information was provided about the mesh, the order of the elements, or the selection of Gauss integration points. Sundaria et al. (2017) studied the use of higher-order elements to reduce the computational time of the FE modeling of exponential cutting deterioration in the simulation of an induction motor. Second-order polynomials were found suitable for representing the flux density and third-order elements were chosen accordingly. It was stated that "an adequate" number of integration points are used, but no specific details were given. Later, Sundaria et al. (2018) improved the computational efficiency of the FE simulation further with the use of mixed-order elements, i.e., second-order elements in the vicinity of the cutting edge and first-order elements elsewhere. The use of "the same" number of Gauss integration points for all elements was mentioned, but no further details were provided. More recently, Mohammadi et al. (2022) included the exponential deterioration profile in the simulation of fractional-kW PMSM using a "very fine mesh" with first-order elements. No information was given about the number of Gauss integration points used or how they were selected.

The literature review shows that there have been several studies on the implementation of cutting deterioration into FE machine simulation using either discrete modeling based on region subdivision or continuous modeling. However, these studies have not provided details on the selection of integration points for Gaussian quadrature, and have not considered the impact of the deterioration term on numerical integration. However, the deterioration term, whether exponential or quadratic, has an explicit dependency on the space and thus alters the form of the function to be integrated. Therefore, this dependency should be taken into account in the Gaussian quadrature to ensure more precise numerical integration.

2.5.3 Effect of Cutting on Machine Performance

After the suitable selection of material models that includes the effect of cutting, and then incorporating these models into FE machine simulation, the performance of the machine has been investigated from various perspectives by several studies. These investigations primarily focused on the impact of cutting on iron losses, torque, and various operating parameters of the machine. These parameters included $d$-axis and $q$-axis inductances, magnetizing current, and back electromotive force (EMF).

In a study conducted by Vandenbossche et al. (2013), the effect of cutting on iron losses was analyzed for a 3 kW induction machine with a slotless rotor under two scenarios: (i) a blocked rotor with a pulsating field and (ii)
synchronous speed with a rotating field. In both cases, the cutting-induced iron losses were found to be 33% and 24% respectively, with hysteresis losses constituting the majority. Similarly, BALI et al. (2014) investigated the impact of cutting on iron losses in a capacitor motor and a synchronous generator, revealing an increase of 8.2% and 4.4% respectively in hysteresis loss.

Elfgen et al. (2017) investigated the effect of cutting on a fractional-kW PMSM and reported that the increase in iron losses due to cutting depended on the machine's operating point. The study attributed a portion of the increased iron losses to the change in local flux distribution, which fell within the range of 10%. Additionally, a rise in direct current demand was observed for a constant operating point. The total torque exhibited a slight decrease, which was also observed by Mohammadi et al. (2022) for another fractional-kW PMSM as 0.5% to 1.5% and was considered negligible.

Sundaria et al. (2019) studied the effect of different cutting techniques on a 4 kW axial flux machine. Significant rises in iron losses were noted, with percentages ranging from 120% to 180% for laser cutting, 40% to 50% for punching, and 6% for water-jet cutting. On the other hand, the effect on torque and generated EMF was found negligible. In a later study by Sundaria et al. (2020) on a 37 kW induction machine topology, a 13%–16% increase in the iron losses of stator lamination was reported as a result of laser cutting. Furthermore, an increase in the no-load current was observed, while the change in full-load current and torque was found negligible. In another study on the 37 kW induction machine, M'zali et al. (2020) observed a 4% increase in magnetizing current due to punching.

The literature review shows the impact of cutting on machine performance has been extensively investigated from various perspectives by multiple studies. The findings have demonstrated that cutting-induced losses can vary significantly depending on the machine type, operating point, and cutting technique employed. While the increase in iron losses has been observed in most cases, the magnitude of the increase ranges from moderate to substantial, with hysteresis losses being the dominant contributor. The effect on torque has generally been found to be negligible. However, it should be noted that the existing studies primarily focus on machines of smaller dimensions and lower power ratings, and further research is needed to understand the specific effects of cutting on large-diameter high-power machines.

2.6 Summary and Conclusions

The study above reviewed the effect of cutting and mechanical stress on ferromagnetic materials, as well as the experimental characterization of
these effects. It also covered modeling approaches for iron loss components and constitutive material law, and their use for modeling cutting deterioration. Finally, the study examined the incorporation of cutting deterioration into FE machine simulations and its impact on machine performance.

The literature has extensively studied the experimental characterization of the individual effects of cutting and mechanical stress on magnetization and iron losses but has not yet characterized their combined effect. Methods for modeling iron losses and inclusion of the effect of cutting deterioration into these models are well established for thin electrical steel sheets and often involve the use of empirical iron loss models. However, there is a lack of models for the iron losses and methods for the inclusion of cutting deterioration into iron loss models for thick steel laminations, where the skin effect and edge effect are significant. Additionally, there is no commonly accepted methodology for incorporating the effects of cutting deterioration into FE machine simulations, including the selection of Gauss integration points and numerical integration.

To characterize the combined effect of punching and uniaxial stress on M400-50A grade electrical steel sheets, an experimental methodology will be introduced with the use of a modified SST constructed by Singh (2016). Based on the experimental results, to model the cutting deterioration in electrical steel sheets, a magnetization model based on the continuous local material modeling approach of Elfgen et al. (2016), and an iron loss model based on the modification of Jordan’s method, similar to the approach used by Sundaria et al. (2020), will be developed. The effect of stress will not be modeled within the scope of this thesis.

To characterize the effect of laser-cutting on 3 mm–12 mm thick S275JR grade structural steel laminations, a combined experimental and numerical methodology will be introduced. The experimental part will be based on the use of a ring-core measurement system. The numerical part will be based on 2-D axisymmetric FE modeling of the lamination cross-section using the model of Rasilo et al. (2019) and developing it further with the implementation of a scalar JA hysteresis model (Jiles et al., 1992) and inclusion of a continuous local material model of Elfgen et al. (2016). Then, based on the results of this combined experimental and numerical methodology, a 2-D analytical model will be developed for computing eddy-current loss in nonlinear thick steel laminations based on the analytical solution of the 2-D field problem.

To incorporate the developed material models into FE machine simulation, a methodology will be developed to account for the cutting deterioration in numerical integration. The method will involve the modification of Gaussian quadrature weights and coordinates for a given deterioration profile in a pre-processing manner. The validation of the method will be based on the use of developed material models for the electrical steel sheets in the electromagnetic FE simulations. The proposed method will
be applicable to any type of deterioration profile.

To evaluate the effect of cutting in the machine performance the developed material models will be incorporated into FE machine simulation based on the proposed methodology for incorporation. The machine will be simulated with and without the effect of cutting in different parts, and the variation in the losses and operating points of the machine will be investigated.
3. Methods

This chapter presents the methods used for the experimental characterization of the materials, modeling of the materials, and incorporation of the models into FE simulation. First, the measurement systems used for the electrical steel sheets and thick steel laminations are described. Then, the magnetization and iron loss models developed for each material type are explained. Finally, a systematic approach to incorporate the effect of cutting deterioration into FE simulation is presented. As these methods have been previously presented in Publications I–IV, this chapter provides an overview of the main parts and includes missing details in the publications.

3.1 Measurement Systems

The magnetic properties of the materials used in the electrical machine parts were characterized using two different measurement systems. The first system, a modified SST, was used to measure the magnetic properties of punched NO electrical steel sheets with and without applied uniaxial stress. The second system, a ring-core measurement system, was used to measure the magnetic properties of thick steel laminations. The details of these measurement systems can be found in Publications I and II. This section will provide a summary of the main points presented in these publications.

3.1.1 Measurements of Electrical Steel Sheets

Five different sample groups were assembled from M400-50A grade NO electrical steel sheets cut by EDM and punching to $1 \times 24$ mm, $2 \times 12$ mm, $3 \times 8$ mm, and $4 \times 6$ mm wide strips with a length of 280 mm and thickness of 0.50 mm. After the assembly, magnetic measurements were performed with an in-house modified SST constructed by Singh (2016). Fig. 3.1 presents the geometry of the assembled samples, including an image of the
modified SST used in the measurements and the arrangement of a single sheet sample and search coils on the device.

**Figure 3.1.** (a) Geometry of the assembled samples for the measurements, (b) the modified SST, and (c) positioning of a single sheet sample and search coils on the device.

The SST includes a magnetizing core, a stressing mechanism, and a single sheet sample. The sample is positioned alongside the stress mechanism by securing its ends with metal plates. Furthermore, to prevent the sheet from folding or collapsing when subjected to compression, it is upheld from above and below by glass fiber retention plates. The average magnetic flux density \( B_0(t) \) and magnetic field strength at the lamination surface \( H_s(t) \) are measured via a B-coil with 10 turns and an H-coil with 400 turns and 24 mm \( \times \) 24 mm \( \times \) 0.50 mm dimensions, which are placed in the vicinity of the middle of the sample. To achieve precise control of stress resolution, a screw mechanism, aided by a spring, is utilized to apply stress to the samples. The magnitude of the stress is measured using a load cell.

Magnetic measurements were conducted on the sample at different magnetization levels (between 0.63 T and 1.5 T), with varying frequency and stress values. \( B_0(t) \) over the samples was controlled to be sinusoidal. \( B_0(t) \) and \( H_s(t) \) were calculated from the induced voltages in the B-coil \( u_b(t) \) and H-coil \( u_h(t) \) using Faraday’s law such that

\[
B_0(t) = \frac{1}{N_b A_b} \int_0^T u_b(t) dt \tag{3.1}
\]

\[
H_s(t) = \frac{1}{N_h A_h \mu_0} \int_0^T u_h(t) dt \tag{3.2}
\]

54
where \( N_b \) and \( N_h \) are the number of turns in the B-coil and H-coil, and \( A_b \) and \( A_h \) are the cross-sectional areas of the coils. Finally, average iron loss density per unit mass \( p_{Fe} \) during one supply period \( T \) was obtained by

\[
p_{Fe} = \frac{1}{\rho T} \int_0^T H_s(t) \frac{dB_0(t)}{dt} dt
\]

(3.3)

where \( \rho \) is the mass density of the material. The flux density, frequency, and stress values of the performed measurements are given in Table 3.1.

**Table 3.1.** Flux density, frequency, and stress values of the performed measurements.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flux density</td>
<td>0.63, 0.83, 1.04, 1.25, 1.5 T</td>
</tr>
<tr>
<td>Frequency</td>
<td>10, 20, 50, 100 Hz</td>
</tr>
<tr>
<td>Stress</td>
<td>-30, -20, -10, 0, 10, 20, 30, 40, 60, 80 MPa</td>
</tr>
</tbody>
</table>

The details of the measurement setup, measurement procedure, and control mechanism of the flux density are given in Publication I. In Section 4.1 of the thesis, the measurement results and analyses are given.

### 3.1.2 Measurements of Thick Steel Laminations

Measurements were conducted on toroidal samples cut from typical 3 mm, 6 mm, and 12 mm S275JR grade structural steel laminations. To vary the amount of cutting surfaces to investigate the effect of laser-cutting, five groups (A–E) of samples with the same external dimensions were assembled from different numbers of laser-cut concentric rings and layers. The concentric rings were insulated from each other to prevent galvanic contact. The assembled samples for the measurements are shown in Fig. 3.2, and their specifications are listed in Table 3.2.

**Table 3.2.** Specifications of the assembled samples.

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer radius ( r_{out} )</td>
<td>100 mm</td>
</tr>
<tr>
<td>Inner radius ( r_{in} )</td>
<td>60 mm</td>
</tr>
<tr>
<td>Total thickness of the cross-section ( d_{tot} )</td>
<td>12 mm</td>
</tr>
<tr>
<td>Length of the flux-path ( L_{av} = \pi(r_{out} + r_{in}) )</td>
<td>503 mm</td>
</tr>
<tr>
<td>Cross-sectional area of the flux-path ( A )</td>
<td>480 mm²</td>
</tr>
<tr>
<td>Number of primary turns ( N_{pri} )</td>
<td>800</td>
</tr>
<tr>
<td>Number of secondary turns ( N_{sec} )</td>
<td>200</td>
</tr>
<tr>
<td>Mass density ( \rho )</td>
<td>7750 kg/m³</td>
</tr>
<tr>
<td>Electrical conductivity ( \sigma )</td>
<td>5.6 MS/m</td>
</tr>
</tbody>
</table>
Figure 3.2. Top view and cross-section of the assembled samples using (a) a stack of 12 mm thick lamination, (b) two stacks of 6 mm thick laminations, and (c) four stacks of 3 mm thick laminations. In total, 15 samples were assembled for the measurements. Each sample has the same external dimensions.

Magnetic measurements were conducted at magnetization levels ranging from 0.25 T to 1.5 T under the quasi-static case and sinusoidal excitations of 5 Hz and 10 Hz frequencies. The primary side of the samples was connected to a power supply and the secondary side was left open-circuited. The average magnetic flux density $B_0$ over the samples was calculated by using the measured induced back-electromotive force $u_{sec}$ in the secondary
winding with $N_{sec}$ turns and $A$ cross-sectional area by

$$B_0(t) = \frac{1}{N_{sec}A} \int u_{sec}(t)dt. \quad (3.4)$$

Magnetomotive force $F$ created by the primary winding with $N_{pri}$ turns was calculated from the measured primary current $i_{pri}$ such that

$$F(t) = N_{pri}i_{pri}(t). \quad (3.5)$$

Finally, the average iron loss density per unit mass (W/kg) $p_Fe$ during one supply period $T$ was obtained by

$$p_Fe = \frac{N_{pri}}{N_{sec}AL_{av}\rho T} \int_0^T i_{pri}(t)u_{sec}(t)dt \quad (3.6)$$

where $\rho$ is the mass density. In addition to the magnetic measurements, electrical conductivity measurements were also performed. The conductivity of the materials was found to be 5.6 MS/m.

The measurement setup and procedures for magnetic and electrical conductivity measurements are fully described and explained in Publication II.

### 3.2 Modeling Electrical Steel Sheets

The magnetization and iron losses of M400-50A grade punched NO electrical steel sheets were modeled based on the experimental results presented in Publication I, for which a summary is given in Section 3.1.1. The magnetization was modeled based on the continuous local modeling approach proposed by Elfgen et al. (2016) and the iron losses were modeled based on Jordan’s two-components formula (Jordan, 1924). The effect of punching was included in both models by expressing the model parameters as a function of the distance from the cutting edge. The developed models for the electrical steel sheets and their implementation into FE machine simulation are presented in Publication IV. This section provides a summary of the main methodologies used in these models.

#### 3.2.1 Magnetization Model

Following the local material modeling approach proposed by Elfgen et al. (2016), the SV reluctivity $v$ is expressed as a function of magnetic flux density norm $B$ and the distance from the cutting edge $r(x,y)$ using two reluctivity curves, one for the case where the material would be fully undamaged $v_{un}(B)$ and one for the case where the material would be fully damaged $v_{dam}(B)$ such that

$$v(B, r) = v_{un}(B) + \underbrace{(v_{dam}(B) - v_{un}(B))} \Delta v(B) \eta_r(r) \quad (3.7)$$

57
where $\eta_r(r)$ is the deterioration profile for the reluctivity curve. It is considered to be an exponential profile such that

$$\eta_r(r) = e^{-r/\tau_r},$$  \hspace{1cm} (3.8)

where $\tau_r$ is a model parameter to be identified. The reluctivity curves are parametrized by the equation proposed by Marrocco (1977), which can be expressed as

$$\nu(B) = \frac{B^{2c_1}}{B^{2c_1} + c_2} (c_3 - c_4) + c_4.$$  \hspace{1cm} (3.9)

Here, $c = [c_1 \ c_2 \ c_3 \ c_4]^T$ is a vector of model parameters and can be obtained by fitting. Consequently, the vectors of model parameters for the undamaged $c_{un}$ and damaged $c_{dam}$ parts of the materials to be identified.

**Parameter Identification Steps**

Based on the formulations in (3.7)–(3.9), $c_{un}$, $c_{dam}$, and $\tau_r$ need to be identified. The identification procedure can be implemented by the following steps:

1. Assume that the least damaged sample is undamaged. Run a least-squares algorithm to fit the parameters $c_{un}$ for the undamaged curve $\nu_{un}(B)$ using the measurement data of the least damaged sample at the lowest measured frequency.

2. Using the undamaged curve $\nu_{un}(B)$ found in step 1, run a least-squares algorithm to fit $c_{dam}$ parameters for the damaged curve $\nu_{dam}(B)$ as well as the model parameter $\tau_r$ such that the modeled curves match with the ones obtained from the measurements at the lowest measured frequency.

**3.2.2 Iron Loss Model**

The average iron loss density $p_{Fe}$ under sinusoidal excitation of frequency $f$ and average flux density amplitude $B_m$ can be approximated using Jordan’s two-components formula in (2.2). Following a similar approach to Sundaria et al. (2020), the effect of cutting in the loss coefficients are included by expressing them locally as a function of distance from the cutting edge $r(x,y)$ as shown in (3.10)–(3.12):

$$p_{Fe} = k_{hy}(r)B_m^2 f + k_{dy}(r)B_m^2 f^2$$  \hspace{1cm} (3.10)

$$k_{hy}(r) = k_{hy,un} + \left(\frac{k_{hy,dam} - k_{hy,un}}{\Delta k_{hy}}\right) \eta_{hy}(r)$$  \hspace{1cm} (3.11)

$$k_{dy}(r) = k_{dy,un} + \left(\frac{k_{dy,dam} - k_{dy,un}}{\Delta k_{dy}}\right) \eta_{dy}(r)$$  \hspace{1cm} (3.12)
Methods

where \( k_{hy,un} \) and \( k_{hy,dam} \) are hysteresis loss density coefficients for undamaged and fully damaged cases. Similarly, \( k_{dy,un} \) and \( k_{dy,dam} \) are dynamic loss density coefficients for undamaged and fully damaged cases. \( \eta_{hy}(r) \) and \( \eta_{dy}(r) \) are deterioration profiles for the loss coefficients, which are considered to be exponential profiles such that

\[
\eta_{hy}(r) = e^{-r/\tau_{hy}} \quad (3.13)
\]

\[
\eta_{dy}(r) = e^{-r/\tau_{dy}} \quad (3.14)
\]

for which \( \tau_{hy} \) and \( \tau_{dy} \) are model parameters to be identified. With the defined formulations in (3.10)–(3.14), the averaged coefficients \( \tilde{k}_{hy} \) and \( \tilde{k}_{dy} \) over the half of the sample width \( w/2 \) can be calculated such that

\[
\tilde{k}_{hy} = \frac{2}{w} \int_0^{w/2} k_{hy}(r) dr \quad (3.15)
\]

\[
\tilde{k}_{dy} = \frac{2}{w} \int_0^{w/2} k_{dy}(r) dr \quad (3.16)
\]

Parameter Identification Steps

Based on the formulations in (3.10)–(3.14), the coefficients \( k_{hy,un}, k_{hy,dam}, k_{dy,un}, k_{dy,dam}, \tau_{hy}, \) and \( \tau_{dy} \) need to be identified. The identification procedure can be implemented by the following steps:

1. Using the measured average iron loss density \( p_{Fe} \) of each sample, run a least-squares fitting algorithm to fit \( k_{hy} \) and \( k_{dy} \) for each sample using Jordan’s formula in (2.2).

2. Assume that the least damaged sample is undamaged. Fix \( k_{hy,un} \) and \( k_{dy,un} \) to the fitted coefficients for this sample.

3. Run a least-squares fitting algorithm to fit \( k_{hy,dam}, k_{dy,dam}, \tau_{hy}, \) and \( \tau_{dy} \) in such a way that the obtained coefficients \( k_{hy} \) and \( k_{dy} \) for each sample in step 1 match the averaged coefficients \( \tilde{k}_{hy} \) and \( \tilde{k}_{dy} \) for each sample individually.

In Section 4.2.1 of the thesis, the verification of the magnetization and iron loss models for the electrical steel sheets will be given based on the results.

3.3 Modeling Thick Steel Laminations

Two methods were developed to model the magnetization and iron losses of thick steel laminations. Initially, a 2-D axisymmetric FE model was developed to model the lamination cross-section including the effect of cutting with a continuous local material modeling approach. With the
developed model, the iron losses were segregated successfully and validated against the measurements. The results and analyses indicated that the eddy-current loss of thick laminations is significantly affected by the lamination width $w$.

After accurately segregating the iron losses with the proposed FE model, considering the need for inclusion of the lamination width $w$ in the eddy-current loss calculation, a 2-D analytical model was developed in the Cartesian coordinate system to compute the eddy-current loss in a faster way by using the loss segregation from the FE simulations as a reference. The analytical model also provided a simple form that could be easily incorporated into machine simulations. Both of these methods were described in detail in Publications II and III. This section summarizes the main methodology used in these models.

### 3.3.1 2-D Axisymmetric Finite-Element Model

A 2-D axisymmetric FE model coupled with a cutting model presented in Publication II was developed based on the axisymmetric modeling suggested by Rasilo et al. (2019) and continuous local material modeling suggested by Elfgen et al. (2016). The remainder of this section summarizes the main theory behind the axisymmetric FE model and the inclusion of cutting deterioration.

**Axisymmetric FE Model**

The model considers the cross-section of the lamination in the $rz$-plane, along which the electrical field strength $E = E_r(r,z,t)\hat{r} + E_z(r,z,t)\hat{z}$ and electrical current density $J = J_r(r,z,t)\hat{r} + J_z(r,z,t)\hat{z}$ are aligned. The magnetic flux density $B = B(r,z,t)\hat{\phi}$, the magnetic field strength $H = H(r,z,t)\hat{\phi}$, and the electric vector potential $T = T(r,z,t)\hat{\phi}$ vectors are all oriented in the $\phi$ direction. The problem setting for the modeled geometry is illustrated in Fig. 3.3 for (a) sample A constructed with a 12 mm thick concentric ring and (b) sample C constructed with 12 mm thick 3 concentric rings.

![Figure 3.3. Problem setting for the modeled geometry with axisymmetric FE model for 12 mm thick (a) sample A and (b) sample C.](image)

The numerical modeling is based on the $T\Omega$ formulation. Here, $\Omega$ is the...
reduced magnetic scalar potential and is considered to be \( \Omega(\phi, t) = F(t)\phi/2\pi \), where \( F \) is the magnetomotive force created by the primary winding, to formulate the magnetic field strength \( H = H(r, z, t) \) such that

\[
H(r, z, t) = T(r, z, t) + \nabla \Omega(\phi, t)
\]

(3.17)

where \( H_s(r, t) = F(t)/2\pi r \) corresponds to the magnetic field strength value at the surface of the geometry when \( T \) is set to 0 on the surface by a homogeneous Dirichlet condition. Combining Ampere’s and Faraday’s laws for the axisymmetric case yields

\[
-\frac{\partial^2 T}{\partial z^2} - \frac{\partial}{\partial r} \left( \frac{1}{r} \frac{\partial (rT)}{\partial r} \right) + \sigma \frac{\partial B}{\partial t} = 0.
\]

(3.18)

\( F(t) \) from the measurements is used as the source of the problem and (3.18) is discretized by using the Galerkin FE method with test functions \( T` \) such that

\[
\int_{-\frac{d}{2}}^{\frac{d}{2}} \int_{r_{in}}^{r_{out}} \left[ \frac{\partial T`}{\partial z} \frac{\partial T}{\partial z} + \left( \frac{T}{r} + \frac{\partial T}{\partial r} \right) \left( \frac{T`}{r} + \frac{\partial T`}{\partial r} \right) + \sigma T` \frac{\partial B}{\partial t} \right] r dr dz = 0.
\]

(3.19)

The constitutive material law \( B(H) \) is based on the scalar JA model summarized in Section 2.3.2 with (2.7)–(2.11). The vector of JA model parameters \( p = [\alpha \; c \; k \; M_s \; a]^T \) is obtained by fitting, for which the procedure will be explained in the upcoming subsections.

From the FE solution, time-averaged hysteresis \( p_{hy} \) and eddy-current \( p_{cl} \) loss densities per supply period \( T \) are obtained as

\[
p_{hy} = \frac{1}{T} \int_0^T \left[ \frac{2\pi}{\rho V} \int_{-\frac{d}{2}}^{\frac{d}{2}} \int_{r_{in}}^{r_{out}} H \frac{\partial B}{\partial t} r dr dz \right] dt
\]

(3.20)

\[
p_{cl} = \frac{1}{T} \int_0^T \left[ \frac{2\pi}{\rho V\sigma} \int_{-\frac{d}{2}}^{\frac{d}{2}} \int_{r_{in}}^{r_{out}} \|J\|^2 r dr dz \right] dt.
\]

(3.21)

**Modeling the Effect of Cutting**

The effect of cutting is modeled with a continuous local modeling approach suggested by Elfgen et al. (2016). The constitutive material law \( B(H) \) is expressed as

\[
B(H, x) = B_{un}(p_{un}, H)(1 - \eta_p(x)) + B_{dam}(p_{dam}, H)\eta_p(x)
\]

(3.22)

where \( x \) is the distance from the cutting edge, and \( B_{un}(p_{un}, H) \) and \( B_{dam}(p_{dam}, H) \) are undamaged and damaged curves, which need to be identified based on the vectors \( p_{un} \) and \( p_{dam} \) obtained from the JA hysteresis model. \( \eta_p(x) \) is the deterioration profile for the permeability, which is considered to be quadratic as

\[
\eta_p(x) = \begin{cases} 
1 - \frac{2x}{\delta} + \left( \frac{x}{\delta} \right)^2 & \text{for } 0 \leq x \leq \delta \\
0 & \text{for } x > \delta 
\end{cases}
\]

(3.23)
where \( \delta \) is the degradation depth, beyond which the degradation disappears.

**Parameter Identification Steps**

Based on the formulations in (3.22)–(3.23), the undamaged curve \( B_{\text{un}}(H) \), the damaged curve \( B_{\text{dam}}(H) \), and \( \delta \) need to be identified for the hysteretic and SV cases. These two curves cannot be measured directly. Instead, both curves are described by the JA model with different parameters \( p_{\text{un}} \) and \( p_{\text{dam}} \) that are identified iteratively by comparing the simulation results against the measurements. The identification procedure can be implemented by the following steps:

1. Assume that the least damaged sample is undamaged. Run a least-squares algorithm to fit the JA parameters \( p_{\text{un}} \) for the hysteretic undamaged curve \( B_{\text{un,hy}}(H) = B_{\text{hy}}(p_{\text{un}}, H) \) such that the FE-simulated B-H loops match the ones measured from the least damaged sample at different frequencies and amplitudes.

2. Using the hysteretic undamaged curve \( B_{\text{un,hy}}(H) \) obtained in step 1, run a least-squares algorithm to fit the JA parameters \( p_{\text{dam}} \) for the hysteretic damaged curve \( B_{\text{dam,hy}}(H) = B_{\text{hy}}(p_{\text{dam}}, H) \), and the degradation depth \( \delta \) such that the FE-simulated B-H loops match the ones measured for the other samples at different frequencies and amplitudes.

3. Obtain the SV undamaged curve \( B_{\text{un,sv}}(H) = B_{\text{sv}}(p_{\text{un}}, H) \) and damaged curve \( B_{\text{dam,sv}}(H) = B_{\text{sv}}(p_{\text{dam}}, H) \) from the identified \( B_{\text{un,hy}}(H) \) and \( B_{\text{dam,hy}}(H) \) curves.

The details of the model and the results are shown and explained comprehensively in Publication II. In Section 4.3.1 of the thesis, the verification of the model will be given based on the results.

### 3.3.2 2-D Analytical Model

A 2-D analytical model for computing eddy-current loss presented in Publication III was developed based on the solution of the 2-D field problem. Initially, a simple 2-D time-domain analytical model is proposed. Then, a simple 2-D frequency domain, similar to the low-frequency approach of Bertotti (Bertotti, 1998), was derived for the sinusoidal excitation. Lastly, a skin-effect correction factor was included in the derived formula. The remainder of this section summarizes the main theory behind the model.

The model considers the cross-section of the lamination in the \( yz \)-plane, along which the electrical field strength \( E = E_y(y, z, t)\hat{y} + E_z(y, z, t)\hat{z} \) and electrical current density \( J = J_y(y, z, t)\hat{y} + J_z(y, z, t)\hat{z} \) are aligned. The magnetic flux density \( B = B(y, z, t)\hat{x} \), the magnetic field strength \( H = H(y, z, t)\hat{x} \), and
the electric vector potential $T = T(y,z,t)\hat{x}$ vectors are considered in the $x$ direction. The problem setting for the modeled geometry is illustrated in Fig. 3.4 for (a) sample A constructed with a concentric ring having thickness $d = 12$ mm and width $w = 40$ mm and (b) sample C constructed with 3 concentric rings having thickness $d = 12$ mm and width $w = 13.3$ mm.

Considering the problem setting defined in Fig. 3.4, 2-D Poisson’s equation can be expressed as

$$\nabla^2 H(y,z,t) = \sigma \frac{\partial B(y,z,t)}{\partial t}. \quad (3.24)$$

where $\sigma$ is electrical conductivity. To solve the equation in (3.24), $H(y,z,t)$ needs an initial form. Therefore, a quadratic spatial dependency for $H(y,z,t)$ is assumed such that

$$H(y,z,t) = H_s(t) + \lambda(t) \left( y - \left(\frac{w}{2}\right)^2 \right) \left( z - \left(\frac{d}{2}\right)^2 \right) \quad (3.25)$$

where $H_s(t)$ is the magnetic field strength at the lamination surface. The parameter $\lambda(t)$ is solved by requiring the average of $\sigma^{-1}\nabla^2 H(y,z,t)$ over $\left[-\frac{w}{2}, \frac{w}{2}\right] \times \left[-\frac{d}{2}, \frac{d}{2}\right]$ to be equal to the rate-of-change of the desired average flux density $B_0(t)$, which gives

$$\lambda(t) = -\frac{3}{d^2 + w^2} \frac{dB_0(t)}{dt} \quad (3.26)$$

Then, the material law $H_{Fe}(B)$ is expressed weakly by requiring the average of $H(y,z,t)$ to be equal to $H_{Fe}(B_0)$, which finally yields

$$H_s(t) = H_{Fe}(B_0(t)) + \frac{\sigma d^2 w^2}{12(d^2 + w^2)} \frac{dB_0(t)}{dt}. \quad (3.27)$$

This resembles the 1-D low-frequency approach for eddy-current loss in (2.17) (Bertotti, 1998) and reduces to it when $w \gg d$. The average iron loss density per unit mass $p_{Fe}$ during one supply period $T$ can be obtained from

$$p_{Fe} = \frac{1}{\rho T} \int_0^T H_s(t) \frac{dB_0(t)}{dt} dt. \quad (3.28)$$
When $B_0(t)$ is sinusoidal such that $B_0(t) = B_m \sin(2\pi f t)$, the integration of the dynamic term of $H_s(t)$ reduces to

$$p_{2D,\text{low}}(f, B_m) = \frac{\sigma \pi^2}{6\rho} \frac{d^2 w^2}{d^2 + w^2} f^2 B_m^2$$

(3.29)

where $p_{2D,\text{low}}$ is the 2-D eddy-current loss density for low frequency.

**Skin-Effect Correction Factor**

To account for the skin effect in the calculation of the eddy-current loss, a skin-effect correction factor $F_{\text{cor}}(f, B_m)$ is defined such that the 2-D eddy-current loss density $p_{2D}$ can be expressed as

$$p_{2D}(f, B_m) = p_{2D,\text{low}}(f, B_m) F_{\text{cor}}(f, B_m).$$

(3.30)

$F_{\text{cor}}(f, B_m)$ is a continuous correction factor defined piece-wisely for linear and nonlinear regions of the material such that

$$F_{\text{cor}}(f, B_m) = \begin{cases} 
F_{\text{lin}}(f), & B_m \leq B_t \text{(linear region)} \\
F_{\text{non}}(f, B_m), & B_m \geq B_t \text{(nonlinear region)} 
\end{cases}$$

(3.31)

where $F_{\text{lin}}(f)$ and $F_{\text{non}}(f, B_m)$ are the correction factor for the linear and nonlinear regions of the permeability, respectively.

$F_{\text{lin}}(f)$ is obtained from the exact solution of the 2-D Poisson’s equation with a linear permeability $\mu$, while $F_{\text{non}}(f, B_m)$ is derived phenomenologically based on the observed $p_{2D,\text{FE}}/p_{2D,\text{low}}$ ratio as a function of relative permeability $\mu_r$ such that

$$F_{\text{non}}(f, B_m) = k + 1 - e^{\tau(f)(\mu(B_m)-\mu_0)}$$

$$\text{with } \tau(f) = \frac{\ln(k + 1 - F_{\text{lin}}(f))}{\mu(B_t) - \mu_0}$$

(3.32)

where $B_t$ is the threshold between the linear and nonlinear regions, $k$ is the fitting coefficient, and $\tau$ is the coefficient that ensures the continuity at $B_m = B_t$ so that $F_{\text{lin}} = F_{\text{non}}$. $F_{\text{non}}(f, B_m)$ extrapolates to $k$ when $\mu = \mu_0$.

The details of the model and the results compared to the FE reference are shown and explained comprehensively in Publication III. In Section 4.3.2 of the thesis, the verification of the model with the comparisons will be given based on the results.

### 3.4 Incorporation of Cutting Deterioration into Electromagnetic Finite-Element Simulation

The developed material models with the inclusion of the effect of cutting should be implemented into the electromagnetic FE simulation of the energy conversion devices properly. One crucial aspect of this process is the use of Gaussian quadrature for numerical integration. To account for the explicit spatial dependency of the deterioration term on the coordinates
during the numerical integration, a method was developed based on the re-computation of the Gaussian quadrature weights and coordinates in each FE for a modeled deterioration in a pre-processing manner. This method is presented in Publication IV in detail. The remainder of this section summarizes the main theory behind the developed method.

3.4.1 Formulation

In a 2-D FE simulation, the Galerkin-discretized electromagnetic field problem can be expressed as

\[ S(a)a = f \]  \tag{3.33} 

where \( S \) is the stiffness matrix, \( a \) contains the nodal vector potentials, and \( f \) is the load vector. If the cutting deterioration is included in the reluctivity using (3.7), the entries of the stiffness matrix \( S_{ij} \) with the nodal shape functions \( N_i \) and \( N_j \) in domain \( \Omega_d \) can be represented in two parts by

\[ S_{ij} = \int_{\Omega_d} v_{un}(B)(\nabla N_i) \cdot (\nabla N_j) d\Omega_d + \int_{\Omega_d} (\Delta v(B)\eta_r)(\nabla N_i) \cdot (\nabla N_j) d\Omega_d, \]  \tag{3.34} 

which needs to be integrated numerically. As the reluctivity term in \( S_{1,ij} \) does not depend explicitly on coordinates, the numerical integration can be accurately performed with 2-D Gaussian quadrature for triangular elements with the well-known weights and coordinates given by Cowper (1973). However, in \( S_{2,ij} \), there is a weighting function \( \eta_r \) (exponential or polynomial), which has an explicit spatial dependency. Therefore, the same Gaussian quadrature used for the integration of \( S_{1,ij} \) cannot yield accurate results for \( S_{2,ij} \). The weights and coordinates of the integration points should be re-computed taking the deterioration function into account.

3.4.2 Numerical Integration

In a triangular domain \( \Omega_d \) in \( \xi\eta \) reference coordinate system with \( \Omega_d = \{ \xi,\eta : 0 \leq \xi,\eta,\xi + \eta \leq 1 \} \), the quadrature rule for the integration of function \( f(\xi,\eta) \) weighted with a deterioration function \( \eta_r(\xi,\eta) \) is expressed such that

\[ \int_{\Omega_d} \eta_r(\xi,\eta)f(\xi,\eta)d\xi d\eta \approx \frac{1}{2} \sum_{k=1}^{n_{\text{int}}} w_k f(\xi_k,\eta_k) \]  \tag{3.35} 

where \( n_{\text{int}} \) is the number of integration points, \( w_k \) are the weights, and \( (\xi_k,\eta_k) \) are the coordinates of the integration points in the reference element. By definition, the quadrature should be exact for all polynomials in the complete polynomial space for degree \( n_{\text{pol}} \) \{\( \xi^i\eta^j, 0 \leq i, j, i + j \leq n_{\text{pol}} \)\}, when the polynomials replace \( f(\xi,\eta) \) (Cowper, 1973; Dunavant, 1985). Following
this definition, a nonlinear system of equations can be built by replacing \( f(\xi, \eta) \) in (3.35) with each of the polynomials in the complete polynomial space \( P_1(\xi, \eta), P_2(\xi, \eta), \ldots, P_m(\xi, \eta) \) with \( m = n_{\text{pol}}(n_{\text{pol}} + 1)/2 \) as follows:

\[
\int_{\Omega_d} \eta_s(\xi, \eta) P_1(\xi, \eta) d\xi d\eta = \frac{1}{2} \sum_{k=1}^{n_{\text{int}}} w_k P_1(\xi_k, \eta_k) \\
\int_{\Omega_d} \eta_s(\xi, \eta) P_2(\xi, \eta) d\xi d\eta = \frac{1}{2} \sum_{k=1}^{n_{\text{int}}} w_k P_2(\xi_k, \eta_k) \\
\vdots \\
\int_{\Omega_d} \eta_s(\xi, \eta) P_m(\xi, \eta) d\xi d\eta = \frac{1}{2} \sum_{k=1}^{n_{\text{int}}} w_k P_m(\xi_k, \eta_k). \tag{3.36}
\]

If the terms on the left-hand side are computed accurately, the resulting nonlinear equation system can be solved to obtain \( n_{\text{int}}, w_k, \) and \( (\xi_k, \eta_k) \) for domain \( \Omega_d \).

Following this property, in the pre-computation stage of the FE simulations, the complete nonlinear system of equations in (3.36) is built for each domain of a meshed geometry by computing the left-hand side integrals numerically with an adaptive quadrature method. Then, (3.36) is solved by using a nonlinear solver based on Levenberg–Marquardt algorithm by keeping \( n_{\text{int}} \) the same as in the Gaussian quadrature in (Cowper, 1973). Next, the computed weights and coordinates are stored, and then the shape functions and their derivatives at the computed coordinates for each domain are computed and stored. The entire process can be done in parallel in the pre-computation stage of the FE solution.

During the FE solution, the numerical integration of \( S_{1,ij} \) is performed with the Gaussian quadrature weights and coordinates, and shape functions and their derivatives at those coordinates. On the other hand, the numerical integration of \( S_{2,ij} \) is performed using the re-computed and stored weights and coordinates, and shape functions and their derivatives for the corresponding domain with the same \( n_{\text{int}} \) as in \( S_{1,ij} \). Thus, for each FE domain, including the mapping between \( \xi \eta \) reference coordinate system and \( xy \) global coordinate system, the calculation of \( S_{2,ij} \) is obtained as follows:

\[
S_{2,ij} = \frac{1}{2} \sum_{k=1}^{n_{\text{int}}} w_k \left[ v(B) \left( J^{-1} \nabla_{\xi \eta} N_j^i \right) \cdot \left( J^{-1} \nabla_{\xi \eta} N_i^j \right) \right] \left| J \right| \tag{3.37}
\]

where

\[
J = \begin{bmatrix} \frac{\partial x}{\partial \xi} & \frac{\partial y}{\partial \xi} \\ \frac{\partial x}{\partial \eta} & \frac{\partial y}{\partial \eta} \end{bmatrix}, \quad \nabla_{\xi \eta} = \begin{bmatrix} \frac{\partial}{\partial \xi} \\ \frac{\partial}{\partial \eta} \end{bmatrix} \tag{3.38}
\]
and $N'_i, N'_j$ are the shape functions at the re-computed coordinates. In the remainder of the dissertation, the following terminology is adopted:

- **Proposed method**: The numerical integration of $S_{1,ij}$ with the Gaussian quadrature weights and coordinates, and shape functions and their derivatives at those coordinates; and the numerical integration of $S_{2,ij}$ with the re-computed and stored weights and coordinates, and shape functions and their derivatives at re-computed coordinates in the pre-computation stage. The same terminology is also used for the numerical integration of similar forms.

- **Classical method**: The numerical integration of $S_{1,ij}$ and $S_{2,ij}$ in a combined form as $S_{ij}$ in (3.34) with the Gaussian quadrature weights and coordinates, and shape functions and their derivatives at those coordinates. The same terminology is also used for the numerical integration of similar forms.
4. Applications and Results

This chapter presents the applications of the used methods and obtained results. As these applications and results have been previously presented predominantly in Publication I–IV, this chapter provides an overview of the main parts and includes missing details in the publications.

4.1 Experimental Characterization of Electrical Steel Sheets

This section summarizes the results of the magnetic measurements for the punched electrical steel sheets under different mechanical loadings. Initially, measured B-H characteristics and the effects of punching and stress on these characteristics are discussed. The iron losses for the measured cases are then presented and analyzed shortly. Afterward, the effect of stress on the punched samples is analyzed in a coupled way from the iron loss perspective. It should be noted that these results are obtained from the measurements explained in Section 3.1.1 and the details of the measurement system and the full results can be found in Publication I.

4.1.1 Measured Magnetic Properties and Iron Losses

B-H characteristics for each measured case were obtained for sinusoidal flux densities. In this section, examples of these B-H curves are given, and the effect of punching and stress on them are discussed briefly.

Fig. 4.1 demonstrates the measured B-H curves for samples A and E, cut along the (a) RD and (b) TD. The figure reveals that as the degradation in the material increases, the average permeability decreases. Furthermore, the behavior remains consistent in both the RD and TD, although the extent of the changes differs due to the material’s anisotropic properties.

Fig. 4.2 shows examples of the B-H curves for sample A at different stress levels and a frequency of 10 Hz. Under a low tensile stress of 20 MPa, the material exhibits an improvement in average permeability, whereas a high tensile stress of 80 MPa reduces the average permeability. Additionally,
Figure 4.1. Measured B-H characteristics of samples A and E at the stress-free case at 10 Hz. Cutting reduces the average permeability for both RD and TD.

Figure 4.2. Measured B-H characteristics of sample A at 10 Hz under different stress levels. The effect of stress on the permeability varies for different levels. Observe the differences between RD and TD.

The compressive stress of -30 MPa decreases the average permeability and significantly increases the coercive field. It is also evident that the effect of stress on the magnetic properties differs depending on the applied stress level and the material’s orientation, with samples cut along the RD and TD exhibiting distinct responses. Previous studies (Miyagi et al., 2010; Leuning et al., 2016; Baghel et al., 2019) have also reported similar findings, highlighting the different effects of stress on the magnetic properties of NO electrical steel sheets along the RD and TD.

Fig. 4.3 illustrates the average iron loss densities $p_{Fe}$ for samples cut along the RD and TD, as obtained from magnetic measurements at frequencies of 10 Hz and 100 Hz. The figure demonstrates that as the degradation resulting from the punching process increases, the iron losses increase. Additionally, the application of compressive stress further amplifies the losses across all samples. On the other hand, when tensile stress is applied to samples with fewer cutting edges (samples A, B, and C), the losses decrease until a certain stress threshold is reached. Beyond this threshold, the losses begin to increase again. However, for samples with a higher
number of cutting edges (samples D and E), the application of tensile stress consistently reduces the losses within the range of stress achievable in the current setup. These findings suggest that the impact of stress on the iron losses of punched samples varies based on the number of cutting edges present.

![Graphs showing iron loss density](image)

Figure 4.3. Measured average iron loss density $p_{Fe}$ under different stress levels and frequencies. The discrete points in each curve correspond to the measured losses of each sample with different levels of cutting degradation.

### 4.1.2 Effect of Stress on Punched Samples

To quantify the effect of stress on the iron losses of the punched samples, the percentage variation of the losses for different stress levels was calculated for each sample using the following equation:

$$\Delta p = \frac{p(\tau) - p(\tau = 0)}{p(\tau = 0)}$$  \hspace{1cm} (4.1)

where $p(\tau)$ is the iron loss density of the sample at stress level $\tau$, and $p(\tau = 0)$ is the iron loss density of the sample at its stress-free case $\tau = 0$. Fig. 4.4 shows the percentage variation of the samples for different stress levels calculated using (4.1).

As shown in Fig. 4.4, the effect of stress varies based on the degradation level of the samples after the punching process. Compression has a lesser effect on increasing the losses as the degradation resulting from punching increases. This is due to the limited additional degradation caused by compression when the samples are already significantly deteriorated after
Figure 4.4. Percentage variation of the losses under different stress levels. It should be noted that (0,0) is the reference point of each sample and the value of the percentage variation is 0.

punching. On the other hand, tension initially reduces the losses until specific threshold levels are reached, after which the losses begin to increase. These threshold levels become higher as the degradation resulting from the punching process increases.

The analysis reveals that the percentage variation of the losses is more prominent when the samples cut along the RD are subjected to compression, whereas it is higher under tension for the samples cut along the TD. Similar findings were reported in previous studies (Miyagi et al., 2010; Leunin et al., 2016; Baghel et al., 2019; Aydin et al., 2018, 2019) for different grades of NO electrical steel sheets. These results were attributed to the anisotropic properties of the materials. A more detailed and comprehensive analysis of the percentage variation of the losses and its dependency on frequency was conducted and is presented in Publication I.

4.2 Verification of Models for Electrical Steel Sheets

The magnetization and iron losses of the punched electrical steel sheets were modeled using the methods in Section 3.2.1 and Section 3.2.2 based on the experimental measurement results presented in Publication I (see Sec-
Applications and Results

This section presents the application of these methods and the results obtained.

4.2.1 Magnetization Model

The EDM-cut sample shown in Fig. 3.1(a) was assumed to be undamaged, and the deterioration profile parameter \( \tau_r \) was obtained as \((1/640)\) in m by following the steps described in Section 3.2.1. The reluctivity model parameters were obtained as shown in Table 4.1. Fig. 4.5 shows the modeled SV B-H curves based on the identified model parameters and the deterioration profiles.

**Table 4.1.** Identified model parameters of the identified SV B-H curves.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>( c_{\text{un}} )</th>
<th>( c_{\text{dam}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>( c_1 )</td>
<td>8.3</td>
<td>4.0</td>
</tr>
<tr>
<td>( c_2 )</td>
<td>( 5.3 \times 10^5 \ \text{T}^{2c_1} )</td>
<td>( 1.6 \times 10^5 \ \text{T}^{2c_1} )</td>
</tr>
<tr>
<td>( c_3 )</td>
<td>( 2.9 \times 10^5 \ \text{m/H} )</td>
<td>( 7.6 \times 10^6 \ \text{m/H} )</td>
</tr>
<tr>
<td>( c_4 )</td>
<td>121 m/H</td>
<td>507 m/H</td>
</tr>
</tbody>
</table>

Figure 4.5. Samples B, D, and E are electrical steel sheets cut into 1, 3, and 4 strips, respectively. The details of the samples and measurements are available in Section 3.1. The figure illustrates the measured data and modeled reluctivity curves for Samples B, D, and E using identified \( \nu_{\text{un}}(B) \) and \( \nu_{\text{dam}}(B) \) with the deterioration profile \( e^{-r/\tau_r} \).

4.2.2 Iron Loss Model

The EDM-cut sample in Fig. 3.1(a) was assumed to be undamaged, and the parameters \( k_{\text{hy,un}}, k_{\text{hy,dam}}, k_{\text{dy,un}}, k_{\text{dy,dam}}, \tau_{\text{hy}}, \) and \( \tau_{\text{dy}} \) were identified by following the steps described in Section 3.2.2. Fig. 4.6 shows the identified coefficients and the modeled losses based on these coefficients.

It can be seen in Fig. 4.6 that the losses are obtained with good accuracy with the used method compared to the measurements, with a mean relative
error of less than 8.5% for each sample.

4.3 Verification of Characterization and Models for Thick Steel Laminations

The magnetization and iron losses of the thick steel laminations were modeled using the methods described in Section 3.3.1 and Section 3.3.2 based on the experimental measurements presented in Section 3.1.2. This section presents the applications of the methods and the results obtained.

4.3.1 2-D Axisymmetric Finite-Element Model

In Publication II, the proposed axisymmetric model was developed using the experimental results of toroidal samples constructed with 12 mm thick laminations, and the corresponding applications and results were presented. The study was then extended to also simulate samples constructed with 3 mm and 6 mm thick laminations, which resulted in changes in the model parameters. However, the effect of this change on the simulated losses of the samples constructed with 12 mm thick laminations was found
Applications and Results

to be less than 1%, which is considered negligible. To avoid confusion, the results will be presented separately for the following two cases:

– **Case I**: Parameter identification based on the experimental results of samples constructed with only 12 mm thick laminations. This case is published in Publication II.

– **Case II**: Parameter identification based on the experimental results of samples constructed with 3 mm, 6 mm, and 12 mm thick laminations.

**Case I**: Sample A constructed with 12 mm thick laminations shown in Fig. 3.2 was assumed to be undamaged, and the model parameters for $p_{un}$ and $p_{dam}$ were obtained by following the identification steps described in Section 3.3.1. The degradation depth $\delta$ was obtained as 4.1 mm and the JA model parameters were obtained as presented in Table 4.2. Fig. 4.7 shows the identified B-H curves based on the fitted JA model parameters and the deterioration profiles.

**Table 4.2.** Fitted JA parameters of the identified hysteretic B-H curves from the experimental results of the samples constructed with 12 mm thick laminations.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>$p_{un}$</th>
<th>$p_{dam}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$M_s$</td>
<td>$1.4 \times 10^6$ A/m</td>
<td>$1.4 \times 10^6$ A/m</td>
</tr>
<tr>
<td>$a$</td>
<td>345 A/m</td>
<td>2190 A/m</td>
</tr>
<tr>
<td>$k$</td>
<td>630 A/m</td>
<td>1950 A/m</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>$6.4 \times 10^{-4}$</td>
<td>$3.2 \times 10^{-3}$</td>
</tr>
<tr>
<td>$c$</td>
<td>0.18</td>
<td>0.49</td>
</tr>
</tbody>
</table>

Based on the identified parameters, eddy-current loss coupled with the hysteresis loss and uncoupled eddy-current loss were simulated. Fig. 4.8 shows examples of simulated B-H curves of samples A and E constructed with 12 mm thick laminations. The simulated curves for the quasi-static case show that cutting reduces the average permeability significantly, which affects the hysteresis loss. It can be also observed that eddy-current loss is dominant at 5 Hz, and its significance is strongly affected by the sample assembly, which will be analyzed in the upcoming parts.

Fig. 4.9 shows the simulated losses of samples A, C, and E constructed with 12 mm thick laminations for the quasi-static case and 5 Hz frequency. The results for the quasi-static case, which consists of only hysteresis loss, show that the hysteresis loss increases as the damage caused by cutting increases. For instance, at 1.5 T, the hysteresis loss of sample E is 20.4% larger than the hysteresis loss of sample A. When the frequency increases to 5 Hz, eddy-current loss becomes more dominant than the hysteresis loss.
When the number of concentric rings increases, the eddy-current loss decreases at the same frequency level. This is due to the fact that the
Applications and Results

Figure 4.9. Simulated and measured losses of samples A, C, and E constructed with 12 mm thick laminations. Since there is no frequency in the quasi-static case, the results are given in J/kg and they correspond to energy loss density.

electromotive force that gives rise to eddy currents in each ring decreases with the increased number of concentric rings. However, the total resistance experienced by the eddy-current loop in the cross-section of the ring remains relatively constant. This is because the resistance along the radial direction decreases while the resistance along the thickness increases. Consequently, the decreased electromotive force, together with the approximately constant overall resistance, causes a lower current density, resulting in a reduction in eddy-current loss.

Furthermore, the comparison between the coupled and uncoupled eddy-current losses reveals differences in their values and behaviors at different flux density amplitudes. These differences indicate that the distribution of eddy currents in the materials is slightly influenced by the hysteretic properties of the materials. These results are consistent with the findings of Dlala, Belahcen, Pippuri and Arkkio (2010), which studied the dependence of eddy currents on hysteresis.

**Case II:**
In the extended study, 15 toroidal samples cut from the same material shown in Fig. 3.2 were analyzed. A single set of parameters was used for the undamaged and damaged curve parameters, denoted as $p_{\text{un}}$ and $p_{\text{dam}}$, respectively. However, due to the different thicknesses of the laminations used in the samples, the degradation depths were considered to be independent of each other. These degradation depths were represented as $\delta_3$, 

77
Applications and Results

$\delta_6$, and $\delta_{12}$ for samples constructed with 3 mm, 6 mm, and 12 mm thick laminations, respectively, and were organized into a vector of parameters denoted as $\delta = [\delta_3 \ \delta_6 \ \delta_{12}]^T$.

Based on the experimental findings, cutting damage was considered to be proportional to the thickness of the samples, and Sample A constructed with 3 mm thick laminations was assumed to be undamaged. The model parameters for $p_{un}$ and $p_{dam}$ were identified by following the identification steps described in Section 3.3.1. The vector of parameters for the degradation depths was found to be $\delta = [10.4 \ 36.9 \ 38.9]^T$, and the JA model parameters were obtained as presented in Table 4.3.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>$p_{un}$</th>
<th>$p_{dam}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$M_s$</td>
<td>$1.44 \times 10^6$ A/m</td>
<td>$1.44 \times 10^6$ A/m</td>
</tr>
<tr>
<td>$a$</td>
<td>232 A/m</td>
<td>502 A/m</td>
</tr>
<tr>
<td>$k$</td>
<td>776 A/m</td>
<td>976 A/m</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>$5.2 \times 10^{-4}$</td>
<td>$1.4 \times 10^{-3}$</td>
</tr>
<tr>
<td>$c$</td>
<td>0.13</td>
<td>0.38</td>
</tr>
</tbody>
</table>

Based on the identified parameters, eddy-current loss coupled with the hysteresis loss and uncoupled eddy-current loss were simulated. Fig. 4.10 shows the simulated coupled eddy-current loss of the samples A, C, and E constructed with 3 mm, 6 mm, and 12 mm thick laminations.

As shown in the figure, the eddy-current loss increases significantly for all samples as the thickness $d$ increases. Additionally, the eddy-current loss decreases as the number of concentric rings increases and the width $w$ of these rings decreases due to the return path of the eddy currents.

It is important to note that the quantitative changes in eddy-current loss vary as a function of both $d$ and $w$. Therefore, it is necessary to model eddy-current loss in 2-D to accurately predict the loss in thick steel laminations.

4.3.2 2-D Analytical Model

The outcomes in the axisymmetric FE model presented in Publication III demonstrated that 2-D FE modeling of the eddy currents provides highly accurate results and enables proper segregation of the losses. However, coupling these 2-D models along the lamination with the 2-D field solution of an electrical machine can be challenging and computationally inefficient.

To address this issue, a 2-D analytical model was developed and presented in Publication III. This model allows for easy computation of eddy-current loss and efficient coupling with the 2-D field solution of an elec-
Applications and Results

Figure 4.10. Simulated coupled eddy-current loss density of samples A, C, and E constructed with 3 mm, 6 mm, and 12 mm thick laminations, respectively. Note that each sample has concentric rings with different widths, denoted by \( w \). Observe the variation of the eddy-current loss as a function of both \( d \) and \( w \).

trical machine. This section presents the results of the developed model, for which the formulations are given in Section 3.3.2. It should be noted that the model development was made in the Cartesian coordinate system to facilitate coupling with the 2-D field solution commonly used in the analysis of electrical machines. To obtain a reference for the comparisons and model development, the eddy-current loss coupled with hysteresis loss was simulated along the lamination cross-section with a 2-D Cartesian FE model by removing the axisymmetry of the 2-D axisymmetric FE model. The segregated eddy-current loss from the simulations was then used as the reference.

Initially, the eddy-current loss for 5 Hz and 10 Hz was approximated using the derived 2-D low-frequency approximation \( p_{2D,\text{low}} \) in (3.29). Fig. 4.11 shows the comparison of \( p_{2D,\text{low}} \) with the 2-D Cartesian FE reference \( p_{2D,\text{FE}} \), and the 1-D low-frequency approximation/classical eddy-current loss \( p_{1D,\text{low}} \) in (2.4). Fig. 4.11 demonstrates that \( p_{2D,\text{low}} \) accurately captures the pattern of eddy-current loss as a function of \( w \). When \( w \) approaches infinity, \( p_{2D,\text{low}} \) and \( p_{1D,\text{low}} \) are similar. However, when \( w \) is approximately equal to \( d \), the estimation of \( p_{1D,\text{low}} \) is poor while \( p_{2D,\text{low}} \) accurately represents the pattern. However, although the pattern is correct, when the skin effect becomes significant, the \( p_{2D,\text{low}} \) overestimates the losses in the linear region and underestimates the losses in the nonlinear region.

To improve accuracy, a skin-effect correction factor \( F_{\text{cor}}(f, B_m) \) was de-
Applications and Results

![Graphs showing comparison of eddy-current loss](image)

**Figure 4.11.** Comparison of $p_{1D,\text{low}}$ (dashed lines) and $p_{2D,\text{low}}$ (solid lines) with the $p_{2D,\text{FE}}$ (circles) for 5 and 10 Hz. Each graph illustrates the eddy-current loss for samples A, C, and E with the same $d$.

derived, for which the final equations are given in (3.30)–(3.32). The derivation of this factor is detailed in Publication III. Fig. 4.12 shows the approximated 2-D eddy-current loss $p_{2D}$ with the inclusion of this skin-effect correction factor, such that $p_{2D}(f, B_m) = p_{2D,\text{low}}(f, B_m)F_{\text{cor}}(f, B_m)$. It is seen in Fig. 4.12 that $p_{2D}$ estimates the eddy-current loss for all cases successfully. The mean relative error for all the cases is 8.1%. For the nonlinear part, where $B_m > 0.75$ T, the mean relative error is 5.1%, indicating the validity of the derived mathematical expression in (3.32).

### 4.4 Verification of the Proposed Numerical Integration Method

The proposed numerical integration method in Section 3.4 was validated through two applications:

i) Beam geometry: Exponential deterioration in the magnetization (see Section 3.2.1) of the electrical steel sheets was modeled for the linear material case. An analytical solution was derived and used as a reference to evaluate the accuracy of the FE solutions and the numerical integration methods.

ii) Transformer: Exponential deterioration in both the magnetization (see Section 3.2.1) and iron losses (see Section 3.2.2) was modeled using a
nonlinear material law in 2-D FE simulations. The element size was selected based on the results from the beam geometry, and the magnetostatic problem in the transformer core was coupled with circuit equations in the windings. Then, a time-stepping analysis was performed to evaluate the accuracy of the numerical integration methods in calculating iron losses and investigate the computational performance of these methods.

These applications are presented in Publication IV in detail. This section summarizes the key results of these applications.

4.4.1 Beam Geometry

A 2-D beam geometry in the $xy$-plane (see Fig. 4.13(a)) was studied in the linear case using the *classical* and *proposed* methods for the numerical integration and the results were validated against the analytical solution, for which the derivation is detailed in Publication IV. The problem in Fig. 4.13(a) with the dimensions $h = L = 10$ mm was simulated for meshes with both second-order and third-order triangular elements, labeled as $n_{\text{el}} = 2$ and $n_{\text{el}} = 3$, for a wide range of element sizes $e_{\text{size}} = [L/8, L]$ (see Fig. 4.13(b) for $e_{\text{size}} = L/8$) and a wide range of decay constants $\tau_g = [1/10^4, 1/10]$ m, where $\tau_g$ is a generic representation of decay constant to distinguish it...
Applications and Results

Figure 4.13. (a) Studied beam geometry in the $xy$-plane with the deteriorated edges along the $y$-axis. (b) Mesh of the simulated beam geometry for $h = L = 10$ mm and $e_{\text{size}} = L/8$. (c) Comparison of $B(x)$ obtained from the analytical reference solution and FE simulations using the proposed and classical methods with $e_{\text{size}} = L/8$, $n_{\text{el}} = 2$, $n_{\text{quad}} = 2$, and $n_{\text{int}} = 3$ for $B_p = 1$ T and $\tau_g = \tau_r = (1/640)$ m. The results match successfully.

Simulation Results and Analysis

To evaluate the accuracy of the numerical integration method in reflecting the increase of iron losses in deteriorated materials caused by variations in the square of the RMS flux density $B_{\text{rms}}^2$ (since $p_{\text{Fe}} \propto B_{\text{rms}}^2$), two equations were computed to represent the variation from the square of the RMS flux density of the undamaged case $B_{\text{un}}^2$ for both FE-simulated cases and the corresponding analytical solution as follows:

$$\Delta B_{\text{FE}}^2 = B_{\text{FE}}^2 - B_{\text{un}}^2$$  \hspace{1cm} (4.2)

$$\Delta B_{\text{A}}^2 = B_{\text{A}}^2 - B_{\text{un}}^2$$  \hspace{1cm} (4.3)
Figure 4.14. The figure illustrates the relative error, denoted by $\epsilon$, between the analytical reference solution and the numerical solution in the calculation of the variation of $B_{2m}^2$ from the undamaged case for the beam geometry in Fig. 4.13. The simulations were obtained using both second-order and third-order triangular elements, labeled as $n_{el} = 2$ and $n_{el} = 3$. The used values of $n_{el}$, $n_{quad}$, and $n_{int}$ are indicated inside the figures. The red dashed lines inside the figures indicate where $\epsilon = \pm 5\%$.

Here, $B_{FE}^2$ and $B_{A}^2$ refer to the squares of the RMS flux densities derived from the FE and analytical solutions, respectively. Following this, the relative error $\epsilon$ between the FE-simulated and analytical solutions was calculated by

$$\epsilon = \left( \frac{\Delta B_{FE}^2}{\Delta B_{A}^2} \right).$$

(4.4)

Fig. 4.14 illustrates $\epsilon$ calculated for each FE-simulated case, which was performed using both proposed and classical methods for the numerical integration.

Fig. 4.14 shows that for a fixed decay constant $\tau_g/L$, $\epsilon$ decreases as the element size $e_{size}/L$ decreases. This is expected as denser meshes provide better accuracy in the FE simulations. Additionally, as the decay constant $\tau_g/L$ decreases, the value of the required element size $e_{size}/L$ to reach the
acceptable accuracy decreases. The reason is that as the decay constant \( \tau_g/L \) decreases, the decay becomes steeper, which can be captured with the use of smaller elements. In the same manner, for a fixed element size \( e_{\text{size}}/L \), \( \epsilon \) decreases as decay constant \( \tau_g/L \) increases.

For a fixed decay constant \( \tau_g/L \) and utilizing the same method for the numerical integration, whether the proposed or classical, FE simulations performed with the third-order triangular elements \( (n_{\text{el}} = 3) \) always yield better accuracy than those with the second-order triangular elements \( (n_{\text{el}} = 2) \) due to a better representation of the approximated quantities with the shape functions. It is observed that the same accuracy level can be reached with the use of much coarser meshes.

For a fixed order of triangular elements \( n_{\text{el}} \) and using the same order of quadrature \( n_{\text{quad}} \), the proposed method typically yields better accuracy than the classical method for the same simulated case. In most cases, the same level of accuracy can be achieved with the use of bigger element size \( e_{\text{size}} \), and thus coarser meshes.

For a fixed order of triangular elements \( n_{\text{el}} \) and using the classical method, accuracy increases when the order of quadrature \( n_{\text{quad}} \) and correspondingly the number of integration points \( n_{\text{int}} \) increases. As the results show, the use of the proposed method with for instance \( n_{\text{el}} = 2 \) and \( n_{\text{quad}} = 2 \) and the use of the classical method with \( n_{\text{el}} = 2 \) and \( n_{\text{quad}} = 8 \) yields a similar level of accuracy, and require a similar \( e_{\text{size}}/L \) to reach a specific accuracy. Nonetheless, for \( n_{\text{quad}} < 8 \), the proposed method predominantly yields better accuracy than the classical method for the same simulated case.

The selection of the optimal element size \( e_{\text{size}} \) required to achieve \(|\epsilon| < 5\%\) for a given decay constant \( \tau_g \) can be employed based on the results in Fig. 4.14 and the analysis made. The detailed procedure is given in Publication IV. This procedure is used in the FE simulation of the transformer, which will be discussed next.

### 4.4.2 Transformer

A transformer geometry in the \( xy \)-plane was studied through 2-D FE simulations (Fig. 4.15(a)). In these simulations, the deterioration in the magnetization and iron losses were modeled using the presented models for the electrical steel sheets in Section 3.2, for which the results are given in Section 4.2. The dimensions of the simulated transformers were chosen such that \( h_{\text{in}} = 20 \) mm, \( h_{\text{out}} = 40 \) mm, and \( L = 10 \) mm. All edges of the transformer were considered to be deteriorated by cutting and the minimum distance to the closest cutting edge was used for \( r(x, y) \) in the material modeling (see (3.7) and (3.10)).

To investigate the precision of the numerical methods for calculating iron losses and compare their computational efficiency, a time-stepping
analysis was performed. This involved solving the circuit equations in the transformer windings concurrently with the magnetostatic problem in the magnetic core. The primary and secondary windings of the transformer were subjected to sinusoidal voltages with a frequency of 50 Hz and an amplitude of $240\sqrt{2}$ V, with the number of turns adjusted to produce a current density of $5 \times 10^6$ A/m$^2$. The parameters required for the circuit equations of both windings are provided in Table 4.4.

Table 4.4. Parameters used in the circuit equations.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Amplitude of supply voltages</td>
<td>$240\sqrt{2}$ V</td>
</tr>
<tr>
<td>Frequency of excitations</td>
<td>50 Hz</td>
</tr>
<tr>
<td>Number of turns</td>
<td>5955</td>
</tr>
<tr>
<td>Resistance of windings</td>
<td>0.30 Ω</td>
</tr>
</tbody>
</table>

The deterioration in the magnetization was included in the FE solution of the electromagnetic field problem using the formulation in (3.7) with the identified parameters in Fig. 4.5. The total iron losses $P_{Fe}$ were computed in a post-processing manner after obtaining the FE solution utilizing the iron loss model in (3.10) with the identified parameters in Fig. 4.6(a). To determine the contribution of each harmonic in the losses, Fourier harmonic analysis was performed for each element. Similar to Zhu and Ramsden (1998) and Zhu et al. (2020), the total hysteresis and dynamic losses $P_{hy}$ and $P_{dy}$ over the volume $V$ were calculated by
\[ P_{\text{hy}} = \int_V \left[ \sum_{n=1}^{\infty} \left( k_{\text{hy},\text{un}} + \Delta k_{\text{hy}} e^{-r/\tau_{\text{hy}}} \right) B_{m,n}^2 (nf_s) \right] dV, \quad (4.5) \]

\[ P_{\text{dy}} = \int_V \left[ \sum_{n=1}^{\infty} \left( k_{\text{dy},\text{un}} + \Delta k_{\text{dy}} e^{-r/\tau_{\text{dy}}} \right) B_{m,n}^2 (nf_s)^2 \right] dV. \quad (4.6) \]

Here, \( f_s \) is the supply frequency, \( n \) is the harmonic number and \( B_{m,n} \) is the peak value of the flux density norm for the \( n \)th harmonic for the corresponding element. Both of these integrals can be numerically integrated using the proposed and classical methods, as they have a similar form to (3.34).

For accurate estimation of the iron losses within an error range of 5\%, the suggested procedure in Publication IV for the optimal element size \( e_{\text{size}} \) was employed, for which the details are given in Publication IV. Afterward, the simulations were performed for both second and third-order triangular elements \( (n_{\text{el}} = 2 \text{ and } n_{\text{el}} = 3) \) using the proposed and classical methods for the numerical integration within the FE simulation and also in the post-processing for the computation of the total iron losses over the volume \( P_{\text{Fe}} \). Table 4.5 shows the details of the simulated cases, referred to as Cases 1–6 in the upcoming parts, and the selected values of element size \( e_{\text{size}} \).

**Table 4.5.** Details of the simulated cases.

<table>
<thead>
<tr>
<th>( n_{\text{el}} )</th>
<th>( n_{\text{quad}} )</th>
<th>Method</th>
<th>( e_{\text{size}} )</th>
<th>No. of nodes in iron core</th>
</tr>
</thead>
<tbody>
<tr>
<td>Case 1</td>
<td>2</td>
<td>2</td>
<td>Classical L/12</td>
<td>76416</td>
</tr>
<tr>
<td>Case 2</td>
<td>2</td>
<td>8</td>
<td>Classical L/6</td>
<td>19104</td>
</tr>
<tr>
<td><strong>Case 3</strong></td>
<td>2</td>
<td>2</td>
<td><strong>Proposed</strong> L/6</td>
<td>19104</td>
</tr>
<tr>
<td>Case 4</td>
<td>3</td>
<td>4</td>
<td>Classical L/6</td>
<td>31840</td>
</tr>
<tr>
<td>Case 5</td>
<td>3</td>
<td>8</td>
<td>Classical L/3</td>
<td>7960</td>
</tr>
<tr>
<td><strong>Case 6</strong></td>
<td>3</td>
<td>4</td>
<td><strong>Proposed</strong> L/3</td>
<td>7960</td>
</tr>
</tbody>
</table>

**Time-Stepping Analysis**

The FE simulations were performed for all the cases in Table 4.5 using a time-stepping analysis for two supply periods, discretizing each period into 400 steps. The simulations were performed in MATLAB software using Intel\textsuperscript{®} Core\textsuperscript{TM} i5-11600 @ 2.80 GHz processor with 32 GB RAM. The iterations were set to stop when the norm of the nodal vector potentials reached below \( 1 \times 10^{-5} \). During the simulations, CPU times for pre-computation, time-stepping, and post-processing stages were recorded. Table 4.6 shows the recorded CPU time data for each simulated case, denoted as Cases 1–6 (see Table 4.5 for the details of the cases).
### Table 4.6. Details of the CPU time data of the simulated cases.

<table>
<thead>
<tr>
<th>Case</th>
<th>Av. no. of iterations per step</th>
<th>Pre-computation</th>
<th>Time-stepping</th>
<th>Post-processing</th>
<th>Total</th>
</tr>
</thead>
<tbody>
<tr>
<td>Case 1</td>
<td>2.5</td>
<td>—</td>
<td>28378 s</td>
<td>104.5 s</td>
<td>28482 s</td>
</tr>
<tr>
<td>Case 2</td>
<td>2.5</td>
<td>—</td>
<td>3017 s</td>
<td>27.9 s</td>
<td>3045 s</td>
</tr>
<tr>
<td><strong>Case 3</strong></td>
<td>2.5</td>
<td>0.5 s</td>
<td>2719 s</td>
<td><strong>39.9 s</strong></td>
<td><strong>2759 s</strong></td>
</tr>
<tr>
<td>Case 4</td>
<td>2.5</td>
<td>—</td>
<td>4306 s</td>
<td>25.9 s</td>
<td>4332 s</td>
</tr>
<tr>
<td>Case 5</td>
<td>2.5</td>
<td>—</td>
<td>355.9 s</td>
<td>7.8 s</td>
<td>363.7 s</td>
</tr>
<tr>
<td><strong>Case 6</strong></td>
<td>2.5</td>
<td>0.8 s</td>
<td>313.1 s</td>
<td>11.9 s</td>
<td><strong>325.8 s</strong></td>
</tr>
</tbody>
</table>

Table 4.6 demonstrates that the majority of the computational time is spent in the time-stepping stage, while the pre-computation stage takes less than 1 second and is not significant in the overall analysis of computational time. In cases where \( n_{el} = 2 \) and \( n_{el} = 3 \), the time required for the time-stepping stage is significantly less when the classical method with \( n_{quad} = 8 \) and the proposed method, as compared to the classical method with \( n_{quad} = 2 \) and \( n_{quad} = 4 \), respectively. This significant difference is due to the increased degree of freedom that denser meshes provide.

When the proposed method is used, there is a 9.9% and 12% reduction in computational time during the time-stepping stage for cases with \( n_{el} = 2 \) and \( n_{el} = 3 \), respectively, compared to the classical method with \( n_{quad} = 8 \). Although the post-processing stage takes longer when using the proposed method, it is not significant enough to impact the overall computational time. Therefore, the overall difference in computational time is 9.4% and 10.4% for cases with \( n_{el} = 2 \) and \( n_{el} = 3 \), respectively.

Comparing the simulated cases with \( n_{el} = 2 \) and \( n_{el} = 3 \), it is observed that using \( n_{el} = 3 \) results in shorter computational time, despite the higher degree of freedom that typically leads to longer computational time for a fixed \( e_{size} \). However, as \( n_{el} = 2 \) requires a smaller \( e_{size} \) to achieve the required accuracy, it results in higher computational time in the studied cases, as shown in Table 4.5.

### 4.5 Synchronous Machine Studies

This section presents the simulation results for a large-diameter synchronous machine with the incorporation of the developed material models for thin electrical steel sheets (see Section 3.2 and Section 4.2) and thick steel laminations (see Section 3.3 and Section 4.3). For model incorporation, the proposed methodology in Section 3.4 was utilized, for which the
Applications and Results

applications were demonstrated in Section 4.4.

4.5.1 Studied Machine and Simulated Cases

The studied machine is a 28–MW synchronous ring motor used in mining applications. The ratings of the machine are given in Table 4.7 in detail. A part of the meshed machine geometry including two rotor poles are illustrated in Fig. 4.16.

Table 4.7. Ratings of the simulated machine.

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power</td>
<td>28 MW</td>
</tr>
<tr>
<td>Voltage</td>
<td>5730 V</td>
</tr>
<tr>
<td>Current</td>
<td>2974 A</td>
</tr>
<tr>
<td>Frequency</td>
<td>5.9 Hz</td>
</tr>
<tr>
<td>Number of pole pairs</td>
<td>76</td>
</tr>
<tr>
<td>Number of stator slots</td>
<td>540</td>
</tr>
<tr>
<td>Stator outer diameter</td>
<td>15.8 m</td>
</tr>
<tr>
<td>Stator inner diameter</td>
<td>15 m</td>
</tr>
<tr>
<td>Rotor inner diameter</td>
<td>14 m</td>
</tr>
<tr>
<td>Air gap length</td>
<td>16 mm</td>
</tr>
<tr>
<td>Connection</td>
<td>Star</td>
</tr>
</tbody>
</table>

The SV constitutive law was utilized for both the stator laminations and rotor poles. Iron losses were calculated during the post-processing stage. The stator laminations consisted of M400-50A grade electrical steel sheets, while S275JR grade steel laminations with a thickness of 3 mm were used for the rotor poles. The developed models for magnetization (see (3.7)–(3.9)) and iron losses (see (3.10)–(3.14)) for electrical steel sheets were directly implemented with the identified parameters and exponential deterioration profile in Section 4.2.

For the thick steel laminations, the developed magnetization model (see (3.22)–(3.23)) was inverted from the $B(H)$ form to the $\nu(B)$ form, similar to (3.7), to make it compatible with the 2-D electromagnetic field problem, which typically uses the $\nu(B)$ form. The iron losses for the thick steel laminations were calculated similarly to the electrical steel sheets, using equations (3.10)–(3.12), but with a quadratic deterioration profile in (3.23), which was identified for the magnetization model (see Section 4.3.1 Case II). The hysteresis loss coefficients were determined based on the quasi-static measurement results presented in Publication II and partially in Fig. 4.9. On the other hand, the dynamic loss was implemented by referencing the developed 2-D analytical model in (3.29). To simplify the implementation,
the $w \gg d$ assumption was made.

In the FE simulations, four cases were studied:

1. No cutting: The effect of cutting on the magnetization and iron losses was disregarded in both stator and rotor parts. The identified undamaged magnetization curves and loss coefficients were used throughout the respective regions.

2. Cutting in stator: The effect of cutting on the magnetization and iron losses was implemented solely in the stator part. Identified undamaged magnetization curves and loss coefficients were used in the rotor poles.

3. Cutting in rotor: The effect of cutting on the magnetization and iron losses was implemented solely in the rotor part. Identified undamaged magnetization curves and loss coefficients were used in the stator laminations.

4. Cutting in stator and rotor: The effect of cutting on the magnetization and iron losses was implemented in both stator and rotor parts.

### 4.5.2 Simulation Results and Analysis

Voltage supply was used in the FE simulations. Initially, static simulations were carried out to adjust the input parameters. The stator terminal was fed with a sinusoidal voltage of 5730 V, while the DC rotor voltage and the
Applications and Results

The initial position of the rotor were tuned to attain the desired shaft power and power factor. Once all the input parameters were finely tuned, time-stepping analyses were conducted for two supply periods, with each period discretized into 400 steps. This procedure was repeated for all simulated cases to ensure consistent shaft power and power factor while accounting for the effect of cutting. Table 4.8 demonstrates the results obtained from the simulations.

**Table 4.8.** Machine simulation results for four different cases.

<table>
<thead>
<tr>
<th>No cutting</th>
<th>Cutting in stator</th>
<th>Cutting in rotor</th>
<th>Cutting in stator and rotor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Terminal voltage (V)</td>
<td>5730</td>
<td>5730</td>
<td>5730</td>
</tr>
<tr>
<td>Terminal current (A)</td>
<td>2846.8</td>
<td>2850.2</td>
<td>2841.8</td>
</tr>
<tr>
<td>Rotor voltage (V)</td>
<td>552</td>
<td>554</td>
<td>557</td>
</tr>
<tr>
<td>Rotor current (A)</td>
<td>495.2</td>
<td>497</td>
<td>499.7</td>
</tr>
<tr>
<td>Air-gap torque (MNm)</td>
<td>27.9</td>
<td>27.9</td>
<td>27.9</td>
</tr>
<tr>
<td>Air-gap flux density (T)</td>
<td>1.39</td>
<td>1.39</td>
<td>1.39</td>
</tr>
<tr>
<td>Shaft power (MW)</td>
<td>27.2</td>
<td>27.2</td>
<td>27.2</td>
</tr>
<tr>
<td>Power factor</td>
<td>0.98</td>
<td>0.98</td>
<td>0.98</td>
</tr>
<tr>
<td>Stator hysteresis loss (kW)</td>
<td>75.2</td>
<td>87.9</td>
<td>75.2</td>
</tr>
<tr>
<td>Stator dynamic loss (kW)</td>
<td>3.4</td>
<td>3.8</td>
<td>3.4</td>
</tr>
<tr>
<td>Rotor hysteresis loss (kW)</td>
<td>13.8</td>
<td>13.6</td>
<td>16.5</td>
</tr>
<tr>
<td>Rotor dynamic loss (kW)</td>
<td>75.3</td>
<td>74.2</td>
<td>70</td>
</tr>
<tr>
<td>Stator resistive losses (kW)</td>
<td>244.9</td>
<td>245.5</td>
<td>244.1</td>
</tr>
<tr>
<td>End winding losses (kW)</td>
<td>205.1</td>
<td>205.6</td>
<td>204.3</td>
</tr>
<tr>
<td>Rotor resistive losses (kW)</td>
<td>274.6</td>
<td>276.6</td>
<td>279.6</td>
</tr>
<tr>
<td>Total losses (kW)</td>
<td>892.3</td>
<td>907.2</td>
<td>893.1</td>
</tr>
</tbody>
</table>

Comparison between no cutting and cutting in stator and rotor cases in Table 4.8 shows that the required rotor voltage to achieve the same shaft power increase from 552 V to 559.5 V causing the rotor current to increase from 495.2 A to 501.9 A. Meanwhile, the terminal current has changed less significantly.

As a result of the changes in the rotor current and terminal current, while the stator resistive losses and end winding losses almost stayed stationary, the rotor resistive losses increased up to 8 kW. Combining this increase with the changes in the core losses, the total losses increased by 16.4 kW from 892.3 kW to 908.7 kW.
5. Discussion and Conclusions

This thesis has focused on the experimental characterization, material modeling, and incorporation of the models in the simulation of large-diameter synchronous machines. This chapter provides a summary and discussions of the proposed methods and obtained results. Finally, suggestions for future work are provided and the thesis is concluded.

5.1 Discussions of the Methods and Results

5.1.1 Summary of the Findings

Effect of Uniaxial Stress on Punched Electrical Steel Sheets
Magnetic measurements were conducted on M400-50A grade NO electrical steel sheets using a modified SST to investigate the combined effect of punching and uniaxial stress. Four sample groups with the same external geometry (0.50 mm × 24 mm × 280 mm), assembled from 1–4 equally wide strips, were measured at different magnetization levels and frequencies under uniaxial stress ranging from -30 MPa to 80 MPa. The measurement results and detailed qualitative and quantitative analyses are presented in Publication I.

The results in Publication I showed that the effect of stress on the samples varies depending on the level of degradation observed in the samples following the punching process. Specifically, when the samples are subjected to compression, the losses increase less as the degradation caused by punching increases. On the other hand, when the samples are subjected to tension, the losses decrease until certain threshold levels, but then increase again beyond these thresholds. The threshold levels at which the transition occurs become higher as the degradation of the samples increases due to punching.

In practice, the ferromagnetic materials used in energy conversion de-
Discussion and Conclusions

VICES experience both cutting degradation and the effect of mechanical stress during the operation of the machine. Therefore, it is important to consider the coupling of these two phenomena for the most accurate modeling, which initially requires accurate modeling of each phenomenon individually. Within the scope of this thesis, the cutting phenomenon is modeled comprehensively. However, the findings of Publication I would provide a foundation for further research to fully couple the two phenomena.

**Modeling of Punched Electrical Steel Sheets**

The magnetization and iron losses of M400-50A grade punched NO electrical steel sheets were modeled based on the experimental results for the stress-free case in Publication I. SV reluctivity was modeled with a continuous local material modeling approach to account for cutting deterioration through an exponential deterioration profile. Similarly, iron losses were modeled based on Jordan’s two-components formula (Jordan, 1924) by expressing the coefficients as functions of the distance from the cutting edge through an exponential profile. The models were formulated in a simple mathematical form to make it easier to incorporate them into FE simulations. The accuracy of the models was tested by comparing the modeled magnetization and iron losses to experimental measurements, which is demonstrated in Publication IV.

**Characterization and Modeling of Laser-Cut Thick Steel Laminations**

Magnetic measurements were conducted on 3 mm–12 mm thick S275JR grade structural steel laminations using a ring-core measurement system to investigate the effect of laser cutting. Five groups of toroidal samples with the same external geometry, assembled from 1–5 equally wide concentric rings, were measured at different magnetization levels and frequencies. The characterization of the material properties and iron losses was then achieved by a combined experimental and numerical methodology presented in Publication II.

The proposed methodology in Publication II involves 2-D axisymmetric FE modeling of the lamination cross-section with the inclusion of a continuous local material model to account for cutting deterioration, using a quadratic deterioration profile. Using the measured magnetomotive force as the source of the problem, the eddy-current loss along the lamination thickness was simulated using a hysteretic constitutive law based on the scalar JA model. The parameters of the hysteresis model were identified iteratively by comparing the simulation results with the measurements. The simulations yielded highly accurate results with an average relative error of less than 2.9% for the total loss values and excellent matching for the B-H loops. The loss segregation based on the simulations showed that the eddy-current loss becomes dominant over the hysteresis loss even at
the low frequencies (i.e., 5 Hz–10 Hz) with a significant skin effect. Furthermore, the results indicated that the inclusion of the edge effects for the thick laminations is needed, which makes 2-D modeling of eddy-currents loss necessary for accurate modeling.

The proposed numerical methodology in Publication II achieved accurate modeling and segregation of the eddy-current loss in the thick laminations. However, coupling these 2-D models along the lamination with the field solution of energy conversion devices can be challenging and computationally inefficient. To address this issue, a 2-D analytical model for computing the eddy-current loss was developed in the Cartesian coordinate system and presented in Publication III. The model was developed based on the analytical solution of the 2-D field problem. Initially, a simple 2-D time-domain analytical model was proposed assuming a magnetic field strength with a quadratic spatial dependency. Then, a simple 2-D frequency-domain formula was derived for the special case of sinusoidal flux density with a skin-effect correction factor using a phenomenological approach. Highly accurate results were obtained compared to FE reference results with an average relative error of less than 5.1% in the nonlinear region.

**Incorporation of Cutting Deterioration into Electromagnetic Finite-Element Simulation**

A numerical integration methodology was developed to incorporate cutting deterioration into electromagnetic FE simulation based on the recomputation of the quadrature weights and coordinates for a modeled deterioration in a computationally efficient manner. The proposed methodology and its applications and results are comprehensively presented in Publication IV.

In Publication IV, the time-stepping analysis demonstrated the computational efficiency of the proposed method compared to existing approaches, which involved increasing the mesh density by reducing the element sizes or boosting the quadrature order for the same mesh size. The proposed method was found to be 10-13 times faster than the former approach and 10.4% more efficient than the latter. In addition to this difference in the computational time, there are two main drawbacks of boosting the Gaussian quadrature order and number of integration points. Firstly, in the case of a hysteretic material law based on the hysteron decomposition, it results in a much poorer performance compared to the use of the proposed method in terms of both computational time and memory allocation. The second drawback is the uncertainty in the selection of the required quadrature order. In the simulated cases of this study, a similar level of accuracy with the proposed method was reached by boosting the quadrature order up to 8, but for any other study, this selection might be ambiguous. Nonetheless, with the proposed method, the minimum required quadrature order based on the triangular element order can always be employed.
**Discussion and Conclusions**

**Effect of Cutting Deterioration on Electrical Machine Simulation**

The developed material and loss models for the thin electrical steel sheets and thick steel laminations were incorporated into electromagnetic FE machine simulation based on the proposed numerical integration methodology. With the incorporated models, the machine was simulated for different cases with a time-stepping analysis. The results revealed that when subjected to a voltage supply, the inclusion of the cutting effect increased the required rotor voltage to attain the same shaft power and power factor. Consequently, this led to an increase in the rotor current, resulting in higher rotor resistive losses. However, the stator resistive losses remained relatively stable due to a less significant change in the terminal current. Combining the increase in copper losses with the increase in core losses, the total losses increased by 16.4 kW. As a consequence, better cooling capabilities would be necessary to handle this increase in total losses.

**5.1.2 Significance of the Work**

The experimental characterization of the effect of uniaxial stress on magnetization and iron losses of M400-50A grade punched electrical steel sheets was achieved through detailed qualitative and quantitative analyses based on a developed measurement procedure with a modified SST. To the best of the author’s knowledge, this is the first study to take into account the combined effect of stress and punching in material characterization.

The laser-cut 3 mm–12 mm thick S275JR grade structural steel laminations were characterized based on a combined experimental and numerical methodology. The numerical part involved solving a 2-D axisymmetric FE problem along the lamination cross-section with a scalar JA model-based hysteretic constitutive law based on $T\Omega$ formulation. Although similar 2-D axisymmetric FE models, such as the ones used by Rasilo et al. (2019) and Bottauscio et al. (2000), have been used in the past, the implementation of the JA hysteresis model was considered for the first time. Additionally, the modeling included a continuous local material model proposed by Elfgen et al. (2016) to account for the cutting deterioration, which has not been thoroughly studied for thick laminations previously.

The characterization of the thick laminations revealed that accurate modeling of eddy-current loss for thick laminations requires 2-D approaches. In the literature, most analytical solutions are based on the modification of Bertotti’s 1-D eddy current loss model (Bertotti, 1988), but 2-D analytical solutions have not been studied. In light of this, a simple 2-D time-domain analytical solution to the 2-D field equation was proposed, and a simple 2-D frequency-domain formula for the special case of sinusoidal flux density was derived. A skin effect correction factor was also developed using a phenomenological approach. These proposed approaches were then successfully validated against the FE reference solutions. To the best of the
Discussion and Conclusions

In author’s knowledge, a skin effect correction for the nonlinear region of the material law has also not been developed in a similar manner previously.

A new method for incorporating cutting deterioration into FE machine simulations was developed and compared to the existing approaches. Previous studies have accounted for the cutting deterioration in the numerical integration by either increasing the number of integration points (Sundaria et al., 2018) or increasing the FE mesh density (Mohammadi et al., 2022), without taking the spatial dependency of deterioration profiles into account. With this study, the spatial dependency was addressed for the first time in the numerical integration. Furthermore, this study is also significant for its detailed comparative analysis of different incorporation approaches. The findings of the study have proved the accuracy of the proposed method as well as its computational efficiency compared to the existing methods. While this study focused on incorporating exponential deterioration, the method can be adapted to accommodate any type of deterioration profile. Additionally, the presented systematic approach for selecting the required element size to reach a specific predefined level of accuracy is expected to provide a perspective for future studies.

Machine simulations were performed with the incorporation of the developed models based on the proposed methodology. The effect of cutting was thoroughly investigated under various scenarios, and the results were analyzed in a granular manner, focusing on operating points and loss components. While previous studies by Vandenbossche et al. (2013), Bali et al. (2014), Elfgen et al. (2017), Sundaria et al. (2019), and Mohammadi et al. (2022), have studied the effect of cutting on losses and operating characteristics of machines, their primary focus has been on smaller machines with lower ratings, particularly studying the effect on the stator part. Large-diameter electrical machines with the inclusion of cutting effects on both stator and rotor parts have not been studied.

5.2 Suggestions for Future Work

5.2.1 Modeling Combined Effect of Stress and Cutting for Electrical Steel Sheets

In this study, experimental characterization of the combined effect of stress and punching was studied for M400-50A grade NO electrical steel sheets. Afterward, within the scope of this thesis, the focus concerning the electrical steel sheets was given to modeling the effect of punching based on the experimental characterization and incorporation of the developed model into electromagnetic FE machine simulation. Therefore, modeling the combined effect of stress and punching was not studied.
In practice, electrical steel sheets in the machine parts are subject to both mechanical stress due to the operation of the machine and plastic stress caused by the cutting process. These two phenomena, among others, exist together in a coupled way. To achieve the best accuracy in the machine simulations, the modeling of the combined effect on the material level first, and then on the machine level is inevitable. To achieve this, the models developed in this thesis can be coupled with the models developed for the effect of stress on electrical steel sheets based on the presented experimental results. For instance, energy-based invariant models for magnetization (Aydin et al., 2017) and iron losses (Aydin et al., 2018) provide simple forms that can be used for coupling.

5.2.2 Eddy-Current Modeling in Finite-Element Machine Simulation

In this study, eddy-current loss for thick steel laminations was first modeled in a 2-D axisymmetric FE model by coupling them with hysteresis loss. Then based on the loss segregation from the FE model, a 2-D analytical model for eddy-current loss was developed to provide a simpler form for implementation into FE simulations. Both 2-D axisymmetric FE and 2-D analytical models take into account the edge effects, i.e., the return path of the eddy currents at the lamination edges, which become more significant when the dimensions of the lamination width \( w \) and lamination thickness \( d \) are comparable \( w \approx d \). When the lamination width \( w \) is significantly larger than lamination thickness \( d \) (i.e., \( w \gg d \)), the 2-D eddy-current model reduces to the 1-D eddy-current model. During the incorporation of the models into the machine simulation, the 1-D eddy current model was used, and the effect of \( w \) was ignored to simplify the process.

The implementation of \( w \) poses challenges in defining the lamination width in complex geometries and accounting for non-unique paths of magnetic flux lines. To address these challenges, a comprehensive study on implementing edge effects using either 2-D/2-D coupling or 3-D modeling can be performed. This would provide a more accurate representation of the eddy-current loss and enhance the simulation results.

5.2.3 Effect of Speed Fluctuations on Losses in Electrical Machines

In this study, during the FE simulations of the machine, the focus was directed towards investigating the effect of cutting on the iron losses and the operating points of the machine. Apart from the additional losses caused by cutting, speed fluctuations also play a significant role in increasing the overall losses in the machine. These fluctuations arise due to the presence of heavy torque ripple, and their occurrence cannot be anticipated during the design process or factory testing. They increase the losses in the
machine as a result of induced currents in the damper and stator windings. Consequently, this rise in losses leads to the generation of excessive heat that surpasses the predicted levels established during the design stage. Therefore, conducting a comprehensive analysis of the effects of speed fluctuation would provide valuable insights into mitigating the associated issues and optimizing the machine’s performance.

5.3 Conclusions

In conclusion, this dissertation presents a comprehensive methodology for realistic and computationally efficient simulations of electrical machines considering the effect of cutting on iron losses and magnetization. This was accomplished through a series of phases, involving experimental characterization, modeling, incorporation of the developed models, and finite-element simulation of the machine with the incorporated models.

The models developed for non-oriented electrical steel sheets and thick steel laminations proved their accuracy in the comparisons against the wide range of experimental results. The methodology proposed for incorporating the developed models provided a systematic and theoretically accurate approach in a computationally efficient manner. Finally, the machine simulation performed with the incorporated models showed the applicability of the developed models and provided an overview of the effect of cutting on the operating points and losses of large-diameter synchronous machines.

The tools and methodologies developed in this thesis are expected to improve the accuracy of machine simulations and facilitate the machine design procedure. This improvement would lead to the development of more efficient machines with reduced losses, which would contribute to the global effort to decrease energy consumption and greenhouse gas emissions.


References


References


References


References


Lubliner, J. (2008), Plasticity theory, Courier Corporation.


Errata

Publication II

The unit for mass density in Table 1 is erroneous. The correct mass density is 7750 kg/m$^3$.

Publication IV

The values in the x-axes of Figure 2(a) are erroneous. $k_{hy}(r)$ and $k_{dy}(r)$ values should be scaled by $2 \times 10^{-2}$ and $4 \times 10^{-4}$, respectively.

In Figure 5, the values in the legend are erroneous. $\tau/L = 1/10^{-1}$, $\tau/L = 1/(2.5 \times 10^{-1})$, $\tau/L = 1/(5 \times 10^{-1})$, and $\tau/L = 1/10^{-2}$ values should change to $\tau/L = 1/10^1$, $\tau/L = 1/(2.5 \times 10^1)$, $\tau/L = 1/(5 \times 10^1)$, $\tau/L = 1/10^2$, respectively. This change should take place also in the text, when these cases are referred to.

In Figure 5(b), in the rightmost graph, the expression "Increasing decay rate" should change to "Decreasing decay constant".
Throughout the article, "as decay rate $\tau/L$ increases" expression should change to "as decay constant $\tau/L$ decreases". Similarly, "as decay rate $\tau/L$ decreases" expression should change to "as decay constant $\tau/L$ increases".

Throughout the article "decay rate" expression should change to "decay constant".
Electrical steel sheets used in the electrical machine parts are cut into appropriate shapes by several techniques during the manufacturing processes. These cutting processes degrade the magnetic properties of the materials and increase iron losses. When these materials are used in electrical machines, alongside other loss mechanisms, they increase the energy losses further and reduce the overall efficiency of electrical machines.

Considering the substantial financial investment in energy consumption and the global commitment to minimizing energy losses, the precise estimation of additional losses incurred due to the cutting processes becomes vital. Accurate prediction of these losses not only contributes to the optimization of electrical machines but also holds the potential for substantial global energy savings.

This thesis aims to present an end-to-end methodology, involving experimental characterization of the effect of the cutting process, material modeling including cutting deterioration, incorporation of the material models into numerical machine simulation software, and the full simulation of the machine with the incorporated models, for realistic and computationally efficient simulations of a large-diameter synchronous machine.