Modelling in stationary frame reference of single and two-phase induction machines including the effect of iron loss and magnetising flux saturation

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ABSTRACT

This paper is aimed to determine a general model for simulating the single and two-phase induction machines operation. The objective of this model is to predict motor performance parameters such as torque, and motor speed. The model includes representation of both main and auxiliary winding in stationary reference frame and also the effect of core losses and the saturation of the main flux paths. A notable feature of the model is the technique used for representing the different level of saturation in the both axis of magnetisation. The developed model is suitable for simulation and modelling the steady-state and transient operation of the single and two-phase induction machines more accurate.

Keywords: modelling, single-phase, two-phase induction machine, iron loss, saturation, vector control

1 INTRODUCTION

Different modern control techniques are applied to reduce the impact of parameter variation on the drive performance. Iron loss and flux saturation are among the main causes of the parameter variation for a single or two-phase induction motor. Different from the three-phase induction machine with symmetrical effects, these effects are different for the q and d axis, due to the unsymmetry of the machine. This paper is devoted to the modelling of such machines with inclusion of the iron loss and saturation effects that can be fully compensated, because they are recognised by the control system. The emphasis is focused on a method of detuning these effects in single or two-phase induction machines drives. There are proposed two control systems: rotor flux and stator flux oriented control two-phase induction motor drive.

2 MATHEMATICAL MODEL

The developed model is based on several assumptions, generally acceptable as stated in [1]:

- Only the fundamental space-harmonic component of the air-gap flux distribution is considered.
- Using the assumption that there is a unique distinct magnetisation curve along each of the two orthogonal axes of the machine includes magnetic-flux saturation effects. No superposition is used. The developed model uses the following inductances: steady-state, differential and dynamic (crosscoupling) magnetising inductances as in [2] and [3].
- A non-linear resistor that is associated with the total stator flux linkage models core loss.

Since the stator windings of the unsymmetrical two-phase induction machine are not identical, the only reference frame with constant parameters is the stationary one. In d-q co-ordinates, the machine model is described as follows accordingly to Fig. 1 (the list of symbols is given in Annex I):

Stator voltage equations

$$v_{qS} = R_M i_{qS} + L_{lM}\, p(i_{qS} - i_{qfe}) + p\lambda_{mq} \quad (1)$$

$$v_{dS} = R_A i_{dS} + L_{lA}\, p(i_{dS} - i_{dfe}) + p\lambda_{md} \quad (2)$$

Rotor voltage equations

$$0 = R_r i_{qR} + L_{lr}\, p i_{qR} - \frac{1}{k}\omega_r \lambda_{dR} + p\lambda_{mq} \quad (3)$$

$$0 = k^2 R_r i_{dR} + k^2 L_{lr}\, p i_{dR} + k\omega_r \lambda_{dR} + p\lambda_{md} \quad (4)$$

Flux equations:

$$\lambda_{qS} = L_{lM}(i_{qS} - i_{qfe}) + \lambda_{qm} \quad (5)$$

$$\lambda_{dS} = L_{lA}(i_{dS} - i_{dfe}) + \lambda_{dm} \quad (6)$$

$$\lambda_{qR} = L_{lR} i_{qR} + \lambda_{qm} \quad (7)$$

$$\lambda_{dR} = k^2 L_{lR} i_{dR} + \lambda_{dm} \quad (8)$$

$$\lambda_{qm} = (i_{qS} - i_{qfe} + i_{qR})L_{mq} = L_{mq}(i_m)i_{mq} \quad (9)$$

$$\lambda_{dm} = (i_{dS} - i_{dfe} + i_{dR})L_{md} = L_{md}(i_m)i_{md} \quad (10)$$

Currents equations:

$$i_{qS} + i_{qR} = i_{qfe} + i_{qm} \quad (11)$$

$$i_{dS} + i_{dR} = i_{dfe} + i_{dm} \quad (12)$$

$$i_m = \sqrt{i^2{}_{qm} + i^2{}_{dm}} \quad (13)$$

As the machine is unsymmetrical, the time variation of the magnetisation flux in d-q axis is:

$$\frac{d\lambda_{dm}}{dt} = \left(\frac{d\lambda_{dm}}{di_m}\cdot\frac{i^2{}_{dm}}{i^2{}_m} + \frac{\lambda_{dm}}{i_m}\cdot\frac{i^2{}_{qm}}{i^2{}_m}\right)\frac{di_{dm}}{dt} + \left(\frac{d\lambda_{dm}}{di_m} - \frac{\lambda_{dm}}{i_m}\right)\frac{i_{dm} i_{qm}}{i^2{}_m}\cdot\frac{di_{qm}}{dt} \quad (14)$$

$$\frac{d\lambda_{qm}}{dt} = \left(\frac{d\lambda_{qm}}{di_m} \cdot \frac{i^2_{qm}}{i^2_m} + \frac{\lambda_{qm}}{i_m} \cdot \frac{i^2_{dm}}{i^2_m} \right) \frac{di_{qm}}{dt} + \left(\frac{d\lambda_{qm}}{di_m} - \frac{\lambda_{qm}}{i_m} \right) \frac{i_{dm}\, i_{qm}}{i^2_m} \cdot \frac{di_{dm}}{dt} \tag{15}$$

Now, if there are introduced the notations:

$\frac{d\lambda_{dm}}{di_m} = L_{dmd}$ dynamic inductance in axis d

$\frac{d\lambda_{qm}}{di_m} = L_{qmd}$ dynamic inductance in axis q

$\frac{\lambda_{dm}}{i_m} = L_{dms}$ static inductance in axis d

$\frac{\lambda_{qm}}{i_m} = L_{qms}$ static inductance in axis q

The induced voltage in the magnetising branch become:

$$u_{md} = \frac{d\lambda_{md}}{dt} = L_{md} \frac{di_{md}}{dt} + L_{dq} \frac{di_{mq}}{dt} \tag{16}$$

$$u_{mq} = \frac{d\lambda_{mq}}{dt} = L_{mq} \frac{di_{mq}}{dt} + L_{qd} \frac{di_{md}}{dt} \tag{17}$$

where the inductance terms are described by the relations:

$$L_{md} = L_{dms} + (L_{dmd} - L_{dms}) \cos^2 \rho_m \tag{18}$$

$$L_{dq} = \frac{(L_{dmd} - L_{dms}) \sin^2 \rho_m}{2} \tag{19}$$

$$L_{mq} = L_{qms} + (L_{qmd} - L_{qms}) \cos^2 \rho_m \tag{20}$$

$$L_{qd} = \frac{(L_{qmd} - L_{qms}) \sin^2 \rho_m}{2} \tag{21}$$

$$\rho_m = \arctan \left(\frac{i_{mq}}{i_{md}} \right) \tag{22}$$

The electromagnetic torque is:

$$T_e = \frac{P}{2} (k L_{mqs} i_{dR} (i_{qS} + i_{qR} - i_{qfe}) - \frac{1}{k} L_{mds} i_{qR} (i_{dS} + i_{dR} - i_{dfe}) = \frac{P}{2} (k \lambda_{qR} i_{dR} - \frac{1}{k} \lambda_{dR} i_{qR}) \tag{23}$$

$$T_e - T_L = J_m p \omega_r + B_m \omega_r \tag{24}$$

The machine parameters can be determined experimentally through classical methods, or theoretically by FEM computing. The value of the resistance used to model iron loss is determined from standard no-load test, at first with sinusoidal supply of rated frequency, and then with a PWM supply of rated frequency. The same test is used for determining the discrete points on the magnetising curve of the machine. The detailed processes of determining these parameters are presented in [2] and [4].

Under transient conditions, the Eqs. (1) ÷ (24) describe the performance of the single or two-phase induction machine. The state-variable forms of the voltage equations will be:

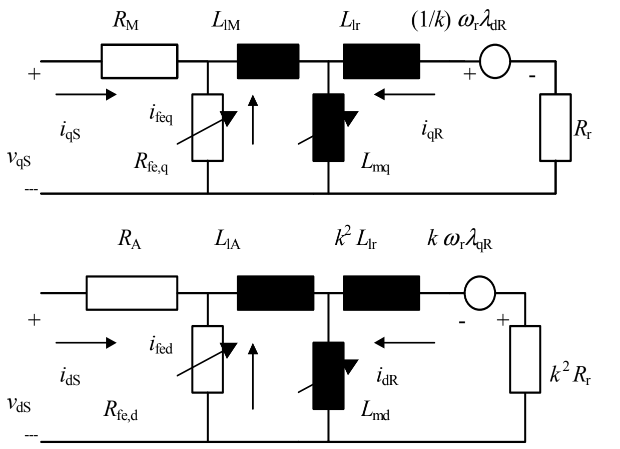

Fig. 1. The d-q axis single and two-phase induction machine equivalent circuit with iron loss and main flux saturation included

$$\mathbf{u} = \mathbf{A} p \mathbf{x} + \mathbf{B} \mathbf{x} \qquad \mathbf{x}^T = [i_{qS}, i_{dS}, i_{qR}, i_{dR}, i_{qfe}, i_{dfe}] \qquad \mathbf{u}^T = [u_{qS}, u_{dS}, 0, 0, 00] \tag{25-26}$$

where:

$$\mathbf{A} = \begin{bmatrix} L_{lM}+L_{mq} & L_{qd} & L_{mq} & L_{qd} & -(L_{lM}+L_{mq}) & -L_{qd} \\ L_{dq} & L_{lA}+L_{md} & L_{dq} & L_{md} & -L_{dq} & -(L_{lA}+L_{md}) \\ L_{mq} & L_{qd} & L_{lR}+L_{mq} & L_{qd} & -L_{mq} & -L_{qd} \\ L_{dq} & L_{md} & L_{dq} & k^2 L_{lR}+L_{md} & -L_{dq} & -L_{md} \\ L_{lM}+L_{mq} & L_{qd} & L_{mq} & L_{qd} & -(L_{lM}+L_{mq}) & -L_{qd} \\ L_{dq} & L_{lA}+L_{md} & L_{dq} & L_{md} & -L_{dq} & -(L_{lA}+L_{md}) \end{bmatrix} \tag{27}$$

$$\mathbf{B} = \begin{bmatrix} R_M & 0 & 0 & 0 & 0 & 0 \\ 0 & R_A & 0 & 0 & 0 & 0 \\ 0 & -\frac{\omega_r}{k} L_{mds} & R_{rot} & -\frac{\omega_r}{k}(k^2 L_{rot}+L_{mds}) & 0 & \frac{\omega_r}{k} L_{mds} \\ k\omega_r L_{mqs} & 0 & k\omega_r(L_{rot}+L_{mqs}) & k^2 R_{rot} & -k\omega_r L_{mqs} & 0 \\ 0 & 0 & 0 & 0 & -R_{qfe} & 0 \\ 0 & 0 & 0 & 0 & 0 & -R_{dfe} \end{bmatrix} \tag{28}$$

The above model is verified by comparing simulated with experimental results.

ROTOR FLUX ORIENTED CONTROLLED TWO-PHASE INDUCTION MACHINE

As the stator windings of the single-phase induction motor are unbalanced, the vector control principles have to be implemented in a special way. The machine parameters differ from axis d to axis q. The waveform of the electromagnetic torque demonstrates the unbalance of the system. Even for equal amplitude, orthogonal stator currents i_{qS} and i_{dS}, the torque contains oscillating term. From Eq.(23), it can be observed that there are present different values for referred magnetising inductance. In [6] there were proposed some relations between the stator currents in order to eliminate the oscillating term from torque expression. However, these relations are valid only in linear conditions (neglecting saturation and iron core losses). Furthermore, the above mentioned model is implemented using a non-referred equivalent circuit, which presume some complicated measurements of magnetising, mutual inductance for stator and rotor.

A new relation for the torque computation, can be determined, by using Eqs. (7)-(10), in Eq. (23):

$$T_e = \left(\frac{P}{2L_{qR} L_{dR}} \right) \left(\begin{array}{l} \frac{1}{k} \lambda_{dR} i'_{qS} L_{mqs} L_{dR} - k\lambda_{qR} i'_{dS} L_{mds} L_{qR} + \\ + \lambda_{qR} \lambda_{dR} \left(kL_{mqs} - \frac{1}{k} L_{mds} \right) \end{array} \right) \quad (29)$$

where:

$$\begin{aligned} i'_{qS} &= i_{qS} - i_{qfe} \\ i'_{dS} &= i_{dS} - i_{dfe} \\ L_{qR} &= L_{lr} + L_{mqs} \\ L_{dR} &= k^2 L_{lr} + L_{mds} \end{aligned} \quad (30)$$

As $L_{mds} = k^2 L_{mqs}$, an equivalent torque expression to that of the symmetric machine, without oscillating term in steady state, can be obtain by imposing the following conditions:

$$i'_{dS} = i'_{dS1} \quad (31)$$

$$i'_{qS} = k^2 \frac{L_{mds} L_{qR}}{L_{mqs} L_{dR}} i'_{qS1} = k^2 i'_{qS1} \quad (32)$$

After some computing manipulations, it results:

$$T_e = \left(\frac{P}{2} \right) \left(\frac{kL_{mds}}{L_{dR}} \right) \left(\lambda_{dR} i'_{qS1} - \lambda_{qR} i'_{dS1} \right) \quad (33)$$

From Eqs. (3), (4) and (7)-(10), one can obtain the dynamic relation that relates the rotor flux to the stator currents:

$$p\lambda_{qR} = -\frac{1}{\tau_{qR}} \lambda_{qR} + \frac{1}{k} \omega_r \lambda_{dR} + \frac{1}{\tau_{qR}} L_{mqs} i'_{qS1} \quad (34)$$

$$p\lambda_{dR} = -k\omega_r \lambda_{qR} - \frac{1}{\tau_{dR}} \lambda_{dR} + \frac{1}{\tau_{dR}} L_{mds} i'_{dS1} \quad (35)$$

where:

$$\tau_{qR} = \frac{L_{qR}}{R_r}; \qquad \tau_{dR} = \frac{L_{dR}}{k^2 R_r}$$

In an arbitrary reference frame, the rotor flux of a single-phase induction machine can now be derived as:

$$p\lambda^a_{qR} = -\frac{1}{\tau_{dR}} \lambda^a_{qR} - \frac{1}{k} (\omega_a - \omega_r) \lambda^a_{dR} + \frac{L_{mds}}{\tau_{dR}} i^a_{qS1} \quad (36)$$

$$p\lambda^a_{dR} = -\frac{1}{\tau_{dR}} \lambda^a_{dR} + k(\omega_a - \omega_r) \lambda^a_{qR} + \frac{L_{mds}}{\tau_{dR}} i^a_{dS1} \quad (37)$$

where ω_a is angular speed of the arbitrary reference frame.

Based on the derived rotor flux model, the rotor field oriented control system can be adapted to a single-phase induction machine. The machine equations in the rotor flux reference frame, can now be obtained by using the constraint condition for rotor flux field orientation control. The resultant equations are as follows:

$$\frac{\omega_{rr} \lambda_r}{k} = \frac{L_{mqs}}{\tau_{qR}} i'_{qS1} \quad (38)$$

$$p\lambda_r + \frac{1}{\tau_{dR}} \lambda_r = \frac{L_{mds}}{\tau_{dR}} i'_{dS1} \quad (39)$$

where λ_r is the rotor flux amplitude, $\omega_{rr} = \omega_e - \omega_r$, and ω_e is the angular speed of the rotor flux viewed from the stator.

The torque expression in rotor flux oriented control system becomes:

$$T_e = \left(\frac{P}{2} \right) \left(\frac{kL_{mds}}{L_{dR}} \right) \lambda_r i'_{qS1} \quad (40)$$

STATOR FLUX ORIENTED CONTROL OF THE TWO-PHASE INDUCTION MACHINE

The torque expression depending on stator flux and current is:

$$T_e = \left(\frac{P}{2} \right) \cdot \left[\begin{array}{l} \lambda_{qS} \lambda_{dS} \left(\frac{k}{L_{mds}} - \frac{1}{kL_{mqs}} \right) + \lambda_{dS} i'_{qS} \left(\frac{L_{qS}}{kL_{mqs}} - \frac{kL_{lM}}{L_{mds}} \right) + \\ + \lambda_{qS} i'_{dS} \left(\frac{L_{lA}}{kL_{mqs}} - \frac{kL_{dS}}{L_{mds}} \right) + i'_{qS} i'_{dS} \left(\frac{kL_{lM} L_{dS}}{L_{mds}} - \frac{L_{qS} L_{lA}}{kL_{mqs}} \right) \end{array} \right] \quad (41)$$

The above relation can be simplified as follows:

$$T_e = \left(\frac{P}{2} \right) \left[\frac{1}{k} \lambda_{dS} i'_{qS} - k\lambda_{qS} i'_{dS} + \left(kL_{lM} - \frac{1}{k} L_{lA} \right) i'_{dS} i'_{qS} \right] \quad (42)$$

Usually, the stator windings have identical configuration, and differ only through number of turns and the diameter of the copper wire. This determines a ratio between the leakage inductance of the main and auxiliary stator windings equal to the square turns ratio (k^2). Conditions for the stator currents become $i'_{qS} = i'_{qS1}$ and $i'_{dS} = k^2 i'_{dS1}$ and Eq. (42) can be re-written as:

$$T_e \cong \left(\frac{P}{2k} \right) \left(\lambda_{dS} i'_{qS1} - \lambda_{qS} i'_{dS1} \right) \quad (43)$$

The dynamic relations function of stator currents and fluxes are described as follows:

$$\begin{aligned} & p\lambda_{qS} + \frac{1}{\tau_{dR}} \lambda_{qS} - \frac{1}{k} \omega_r \lambda_{dS} = k^2 \sigma_{qS} L_{qS} pi'_{qS1} + \\ & - \frac{1}{k} \omega_r \sigma_{dS} L_{dS} i'_{dS1} + \frac{k^2}{\tau_{dR}} L_{qS} i'_{qS1} \end{aligned} \quad (44)$$

$$\begin{aligned} & p\lambda_{dS} + \frac{1}{\tau_{dR}} \lambda_{dS} + k\omega_r \lambda_{qS} = \sigma_{dS} L_{dS} pi'_{dS1} + \\ & + k^3 \omega_r \sigma_{dS} L_{dS} i'_{qS1} + \frac{1}{\tau_{dR}} L_{dS} i'_{dS1} \end{aligned} \quad (45)$$

where

$$\sigma_{d(q)S} = 1 - L^2_{md\,(q)s} / (L_{d(q)r} L_{d(q)S})$$

For an arbitrary reference frame, the corresponding dynamic equations are easily deductible by substituting the ω_r term with $\omega_r - \omega_a$.
Based on the vector model given above, it can be applied the field oriented principles to control the stator flux of the single or two-phase induction machine. The following equations are obtained:

$$\lambda_{qS} = 0 \tag{46}$$

$$\lambda_{dS} = \lambda_s \tag{47}$$

$$i'_{qS1} = \frac{2kT_e}{P\lambda_s} \tag{48}$$

$$p\lambda_s + \frac{1}{\tau_{dR}}\lambda_s = \sigma_{dS} L_{dS}\, p i'_{dS1} + + k\omega_{sr}\sigma_{dS} L_{dS} i'_{qS1} + \frac{1}{\tau_{dR}} L_{dS}\, i'_{dS1} \tag{49}$$

Existence of the saturation effect and core loss in the machine can be included in such manner that the torque and flux production in the machine is governed solely by stator current. It has to be mentioned that in stator flux oriented control operation, the machine presents a steady-state pull-out torque that limits the stabile operating region.

EXPERIMENTAL AND SIMULATED RESULTS

Experimental validation of the proposed model was made on a capacitor run single-phase induction motor, the parameters of which are shown in Annex II.
Fig. 2 illustrates the torque–slip characteristic of the motor with both stator windings energised (no run capacitor was connected). With symbol ‘o’ is denoted the measured points of this curve. A good accuracy of the model can be observed.
Further analysis of the motor is made by simulation of load operation in two cases: normal capacitor run single-phase induction motor, and vector controlled single-phase induction motor, without running capacitor. Due to space limitations, only few simulations results are presented.
Figs 3, 4 are dedicated to run capacitor motor and Figs 5, 6 to vector controlled motor, respectively. Two important improvements are realised through vector control operation: a significant decreasing of the pulsating component torque of the motor, and decayed speed oscillations about the final operating point.

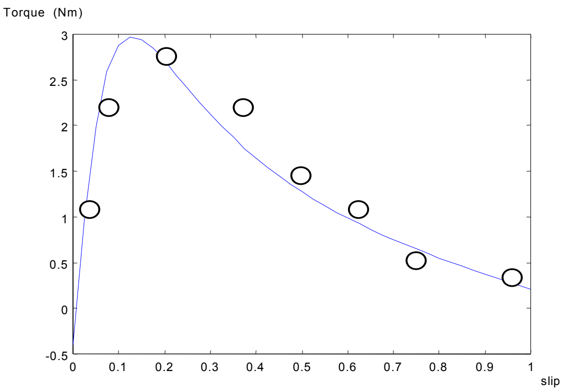

Fig. 2. Torque-slip characteristic (-calculated, o measured)

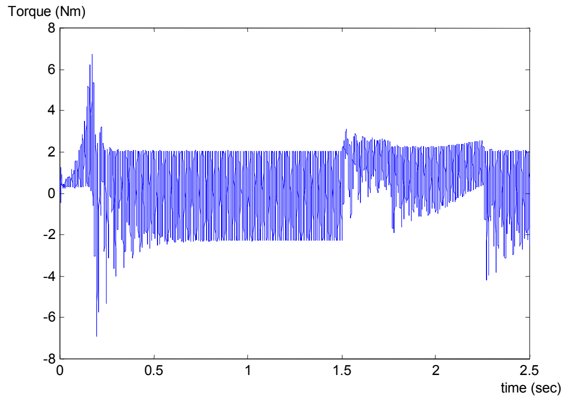

Fig. 3. Simulated torque-time variation for two phase induction motor with run capacitor

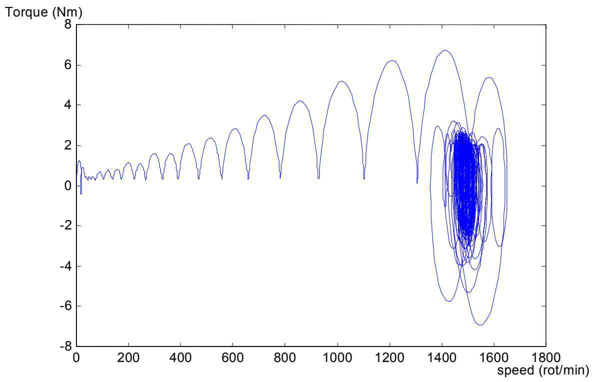

Fig. 4. Simulated torque-speed variation for two-phase induction motor with run capacitor.

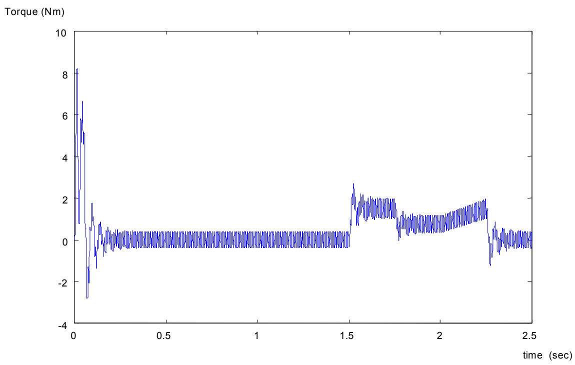

Fig. 5. Simulated torque-time variation for vector controlled two-phase induction motor

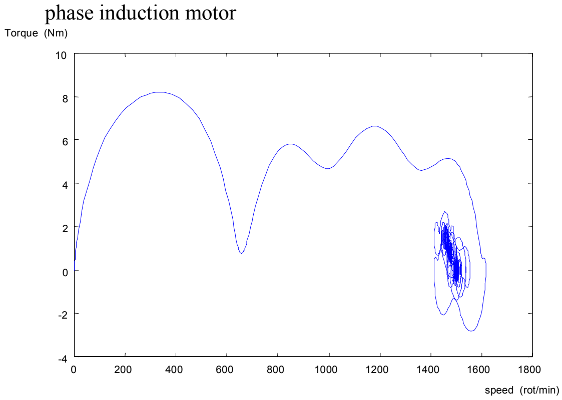

Fig. 6. Simulated torque-speed variation for vector controlled two-phase induction motor

CONCLUSIONS

This paper proposes an equivalent circuit for the single and two-phase induction machines including simultaneously, but as independent phenomena, the iron loss and the main flux saturation. This model permits an improvement in operation of vector-controlled single and two-phase induction machines. The vector control structure suitable for unsymmetrical two-phase induction machine may be chosed rotor or stator flux oriented control. The model can be implemented for simulations by using either experimental determined parameters or FEM calculated parameters.

The stator field oriented system can be readily implemented on the same DSP board with the rotor field control version, as there are used the same parameters. It is expected that the stator flux control should be more suitable for low speed applications, as the rotor flux control system is useful for high-speed applications.

ANNEX I

List of principal symbols

ω_e synchronous speed
ω_r rotor speed
p differential operator
R_M, R_A main, auxiliary stator windings resistance
R_r rotor winding resistance
$R_{feq,d}$….equivalent iron-loss resistance (d and q axis)
L_{lM}, L_{lA} main, auxiliary stator leakage inductance
$L_{mds,mqs}$ magnetising inductance (d and q axis)
L_{lr} rotor leakage inductance
k turns ratio auxiliary/main windings
T_e electromagnetic torque
J_m inertia of motor
$\lambda_{dS,qS}$ stator flux (d and q axis)
$\lambda_{dR,qR}$ rotor flux (d and q axis)
$v_{dS,qS}$ stator voltages (d and q axis)
$i_{dS,qS}$ stator current (d and q axis)
$i_{dR,qR}$ rotor current (d and q axis)
$i_{dfe,qfe}$ iron-loss equivalent stator current (d and q axis)

ANNEX II

Data of the motor under simulation

Rated output power: 750 W
Rated frequency: 50 Hz
Rated speed: 1448 rpm
Rated voltage: 220 V
Number of poles: 4
Inertia: 0.00146 kgm^2
Running capacitor: 10 μF/400 V.
Stator main winding resistance: 5.35 Ω
Stator auxiliary winding resistance: 13.83 Ω
Rotor resistance: 3.95 Ω
Iron-loss resistance (in d axis): 1459 Ω
Iron-loss resistance (in q axis): 1287 Ω
Stator main winding leakage reactance: 12.35 Ω
Stator auxiliary winding reactance: 14.54 Ω
Rotor leakage reactance: 5.25 Ω
Mutual reactance (in d axis): 224.73 Ω
Mutual reactance (in q axis): 104.1 Ω

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