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CONFERENCE PROGRAM & CONTENTS

Conference Center, Kitakyushu Science and Research Park

September 30, 2002 (Monday)

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Thermal Analysis of Induction Motors under Time-Dependent Frequencies and Electromechanical Loadings

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Abstract

In this paper we have developed a model for the complete thermal analysis of induction motors accounting for motor losses in converting electrical energy into the mechanical output. Radial and axial cross-sections of the motor have been analysed for a case study example based on a squirrel-cage totally enclosed fan-cooled induction motor. The results of this analysis are used as an input to a control loop allowing to prevent the motor overheating. The model permits calculations for a wide-range of time-dependent loading conditions and frequencies.

1. Introduction

Induction machines constitute a class of electric drives used widely in many *mechatronics* applications due to their relative simplicity and robustness. The basic principle of operation of these *electromechanical machines* is based on the interaction of a rotating field produced in the air gap by the AC in the stator windings and the rotor windings in order to induce a voltage, and, as a result, a current into the rotor conductors [2]. Further, the interaction between the generated field and the rotor current produces torque. A prerequisite of the voltage production in the rotor windings is a relative motion of the rotor conductors and the magnetic field of the stator, the rotational speed of which is dependent of the frequency of the AC supplied and the number of poles.

Our main interest in this paper is in three-phase cage induction machines of totally enclosed fan-cooled (TEFC) design. It is well known that thermal effects can lead to an increased stress in the induction insulation system which could cause electrical shorts in the windings and other undesirable effects leading to a significant decrease in the motor life. Nevertheless, the evaluation of these damaging thermal effects are often conducted in a simplistic manner which does not allow to track accurately thermal history of the device. In this paper we develop a thermal model for the complete thermal analysis of induction motors under a wide range of operational frequencies and loads, and apply the developed model to the TEFC squirrel-cage induction motor.

The basic structure of a squirrel-cage induction motor that we use as a case study in this paper is given in Fig. 1. Key elements of these electromechanical devices include frames, fans, end caps, rotors, end windings, stator coils, stator cores, bearings and shafts.

Fig. 1.

For indicative purposes, we mention that the stator core of the motor depicted in Fig. 1 has the axial length of 90mm, internal and external radii 58mm and 71mm, respectively. The end winding length on the fan side is 33mm (and 39mm otherwise), and their height is 15.25mm. Radial air gap length is 0.25mm. The axial length of bearings is 15mm, while their internal and external radii are 24mm and 52.80mm, respectively. Different materials are used for different parts of the motor, the fact which complicates the construction of the complete model. In particular, the frame and end caps are made of iron, the stator and rotor cores are made of steel, the stator and rotor coils are made of copper and aluminum, respectively, while the stator insulator is plastic.

The major contributors to the temperature rise in such motors are typically related to the power supply regime, as well as to the load parameters, and the frequency of operation. Indeed, while a high voltage can saturate the motor's core, leading to overheating, a high frequency typically leads to increased losses, and decreasing cooling capacity of the motor.

2. Modelling heat transfer and distributions of losses

There are several mechanisms of heat transfers important for our further consideration, conduction, convection, and radiative heat transfer [1]. For example, in the frame, air gap and the end winding space the forced convection mechanism will be dominant, while two other mechanisms contribute to the total distribution of losses. Furthermore, losses, which are the result of energy conversion produced by the motor, serve themselves as heat sources. They are distributed throughout the whole motor, and the calculation of loss distribution represents a challenging and important task because such a distribution is a function of operating conditions of the motor. The motor losses which consume electrical energy but do not contribute to useful mechanical output can be categorized in the following major groups: (a) stator core (iron) losses (15-25% of the total), (b) stator and rotor coils (copper) losses (25-40% and 15-25%, respectively), (c) friction and windage (mechanical) losses (5% of the total), and (d) stray load losses (10-20% of the total).

The equivalent circuit method (e.g., [3]) has been used to calculate the above losses in the motor. For example, for the stator copper losses we have

$$P_1 = mR_1 I_1^2, \tag{1}$$

where R_1 is the stator resistance, m is the phase number of the motor, and I_1 is the phase current of the stator, which can be calculated according to the following formula

$$I_1 = \frac{U_1(Z_3 + Z_1)}{Z_1 Z_3 + Z_1 Z_2 + Z_3 Z_2}, \tag{2}$$

where $Z_i, i = 1,2,3$ are the phase impedance of the stator ($Z_1 = R_1 + jX_1$), the impedance of the magnetized circuit ($Z_2 = R_2/s + jX_2$), and the rotor circuit impedance ($Z_3 = R_3 + jX_3$), respectively, and U_1 is the per-phase supplied voltage. In this setting I_1 and U_1 can vary depending on operational conditions, s is the so-called slip of the motor, $s = (n_s - n)/n_s$ with n_s and n being the rotating speed of the stator produced magnetic field and the rotating speed of the rotor, respectively, $R_i, i = 1,2,3$ are resistances (with indices having the same meanings as explained above), and $X_i, i = 1,2,3$ are reactances defined in terms of the frequency (f) of applied three-phase stator current, e.g. $X_1 = 2\pi f L_1$, where L_1 is the stator leakage inductance. Expressions analogous to (1) are used for the evaluation of iron losses, P_2, and rotor copper losses, P_3 [3].

The friction losses in the air gap and shaft of the motor have been estimated by the power associated with the resisting drag torque of a rotating cylinder in the enclosed free space

$$P_4 = \frac{1}{2} C_M \pi \rho \omega^3 r^4 l, \tag{3}$$

where ρ is the mass density of the fluid, ω is angular velocity, r is the radius, l is the length of the cylinder and C_M is the torque coefficient. In the case of friction losses in the shaft this coefficient is determined by fitting experimentally measured data depending on the value of the tip Reynolds number

$$\mathrm{Re}_r = \rho \omega r^2 / \mu, \tag{4}$$

where μ is the dynamic viscosity. More precisely, the definition of the torque coefficient is made according to the nature of the tangential flow exerted by rotating cylinder, and we use the following formula for its evaluation

$$C_M = a + \frac{b}{(\mathrm{Re}_r)^c}, \tag{5}$$

where the values of a,b, and c are determined depending on the regime of the flow (on the tip Reynolds number).

A similar methodology has been used in evaluating losses in the air gap. Firstly, we have estimated the Cottage Reynolds number

$$\mathrm{Re}_c = \rho \omega r \delta / \mu, \tag{6}$$

where δ is the radial air gap length. Then, we use experimentally fitted formula for evaluating the torque coefficient

$$C_M = d \frac{(\delta / r)^p}{(\mathrm{Re}_c)^q}, \tag{7}$$

where the actual values of d, p, and q are dependent on the range in which (6) lies for specific operational conditions of the motor.

Finally, the losses on bearings have been computed according to the formula

$$P_5 = 2\pi M_f n, \quad M_f = \xi W r = 2R^2 \xi l_b P, \tag{8}$$

where M_f is the frictional torque, ξ is the friction coefficient, r is the radius of the shaft, l_b is the bearing length, n is the shaft rotating speed, W is the radial load applied on the bearing, and P is the pressure determined as the radial load per unit of projected bearing area ($P = W/(2rl_b)$).

In terms of parameter identification for the heat transfer mechanisms, note that the model for convection is dependent on the heat transfer coefficient h, and in the general setting the estimation of h is a difficult task (e.g., [4]). In the air-gap region and in the end winding space we use the analogy with the flow in a channel, so that the heat transfer coefficient can be related to the Nusselt number ($Nu = h\delta / k$), the radial air-gap length δ and the thermal conductivity coefficient k. Comparing the Nusselt number and the modified Taylor number

$$Ta_m = \frac{\rho^2 \omega^2 r_m \delta^3}{\mu^2 F_g}, \tag{9}$$

where ω, as before, is the angular velocity, r_m is the average of the stator and rotor radii, and F_g is a geometric factor dependent on the radial air gap length and r_m, we can estimate h in the air gap giving the angular velocity.

A similar idea is applied to the estimation of h in the end winding space. In evaluating the heat transfer from the frame of the motor into the external environment, the heat transfer coefficient should take into account the dissipation from fins of the frame h_1 and the dissipation among fins h_2. In addition to the geometry and mutual locations of the fins, the evaluation is dependent on the air-flow characteristics provided by the external fan. The approximations used in this paper are

$$h_1 = H(1-0.02(l_h / l_w)), \tag{10}$$

where l_h and l_w denote the height of the fins and the distance between two fins, respectively, and

$$H = A_1(\mathrm{Re})^{A_2}\left[1 - A_3(l_h / l_w)^{A_4}\right]k_a / l_f, \tag{11}$$

where l_f is the axial length of the fins, k_a is the thermal conductivity of the air, the Reynolds number in this case is defined as $\mathrm{Re} = V_a l_f / \kappa$ with V_a being the airflow velocity and κ being the kinematic viscosity of the air, and $A_i, i=1,...,4$ are experimentally fitted constants (dependent on the range of Re). The approximation of h_2 has the form specified by (11), but with different values of coefficients A_i. For other regions (including the shaft and the rotor) the free convection is assumed.

3. Thermal model and losses as functions of time-dependent frequency

The general dynamic model for the thermal analysis of induction motors should account for all three heat transfer mechanisms and the loss distribution as we have discussed in the previous section. The governing equation used in our analysis is the time-dependent nonlinear model

$$\rho C\frac{\partial T}{\partial t} - \nabla(k\nabla T) = Q + h\ (T_{ext} - T) + C_{cons}(T_{am}{}^4 - T^4) \tag{12}$$

where in the first term of the right hand side of (12) we have incorporated the models for distributed losses, as discussed in Section 2. The other two terms take care of convective and radiative effects. The thermal conductivity, density, and the specific heat in (12) vary spatially due to different conductive properties of different parts of the motor.

Boundary conditions for the motor are used in the general form

$$-\nabla(k\nabla T) = q + h\ (T_{ext} - T) + C_{cons}(T_{am}{}^4 - T^4), \tag{13}$$

while the initial conditions use the ambient temperature.

Basic parameters for losses are taken from the data base of the motor which has 2 pole pair, and 3 phases; the resistance of the stator and the rotor, and the magnetizing resistance are 6.509, 4.512, and 2200 [Ω], respectively; the respective values of their inductance are 0.01598, 0.01182, and 0.2972 [H]. Computation of losses has been performed for typical situations where the motor is driven by the dynamically changing frequencies (see Fig. 2, e.g., for t_1 =10s, t_2 =5s, t_3=10s, $f_{\min}$=20Hz, $f_{\max}$=100Hz).

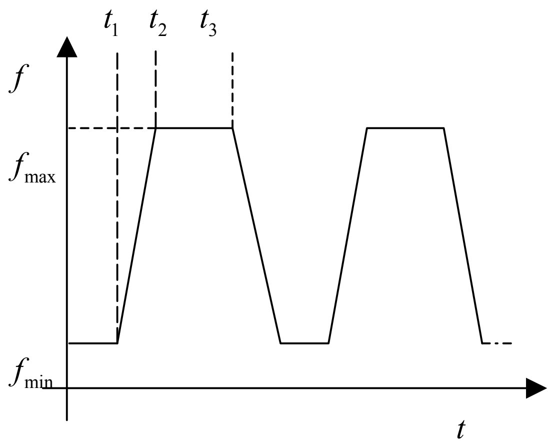

Fig. 2

In the frequency range mentioned above the Reynolds number for the shaft ranges from 2000 to 12000, and for the air gap it ranges from 80 to 400. Based on our discussion from Section 2 (see discussion around (3)—(7)) these estimates allow us to derive approximate formulae for losses as functions of frequency and slip

$$P_4 = \alpha f^{\beta}(1-s)^{\beta}, \tag{14}$$

where for the shaft we have $\alpha = 0.18e^{-6}$ or $\alpha = 0.12e^{-6}$ (first value is on the fan side), and $\beta = 2.5$. For the air gap the formula (14) reads with $\alpha = 0.39e^{-4}$ and $\beta = 2.4$. Taking the lubricant in the bearings with friction coefficient 0.015, we evaluate the losses in bearings, P_5, by using (14) with $\alpha = 0.05$ and $\beta = 1$.

As for the heat transfer mechanism parameter identification, note that for the specified operation conditions we have the Taylor number estimates within the 100-to-1000 range, and therefore, by using the technique discussed in Section 2 (see discussion around (9)), the convection coefficient is estimated at h=224 in the air gap. Further, the convection coefficient in the fin frame area and between the fins (where airflow is laminar for this operating range) can be estimated as

$$h_i = \gamma f^{\beta}(1-s)^{\beta}, \tag{15}$$

where $\gamma = 3.2, \beta = 0.5$ for $i=1$ and $\gamma = 2.2, \beta = 0.5$ for $i=2$ (see (10), (11) and discussion there). After estimations of Ta_m and Nu in the end winding space, we found that the convection in this area can also be represented by (15) but with $\gamma = 11, \beta = 0.482$. The convection in other surface areas is considered to be free with $h = 9$. Finally, radiation absorption coefficient for oxidized forging iron from which the surface of the motor is made is 0.95.

4. Thermal analysis as an important element in the design of induction motors

The model developed in Section 3 provides a comprehensive tool for thermal analysis of induction motors of various geometries, as well as of different physical and mechanical characteristics of the materials used in the design process. The model can be used to calculate thermal regimes of the motors for different operational speeds. Moreover, the developed tool allows us to simulate effectively operation conditions of the motors for a wide range of electromechanical loadings. Simulation results can be used in the design process by comparing critical temperatures with specified thermal thresholds. Not only this puts the whole design process on a more rigorous basis leading to potential cost savings, but it also indicates the ways for the controller design aiming at the prevention of motor overheatings. Since abnormally high temperatures, leading to increased thermal stresses, can contribute substantially to the overall degradation of the motors' insulation system, the thermal analysis becomes an important element in the modern design of induction motors aiming at the increased life-time of the motors with improved functional characteristics.

5. FE analysis of thermal field distribution in the squirrel-cage induction motor

The complete nonlinear model described in the previous sections has been implemented in FEMLAB with 19 subdomains, e.g. the rotor, end windings, fan-side bearings. Heat transfer mechanisms and the distribution of losses are subdomain dependent. For example, in the stator core subdomain, the primary source of losses are iron losses which serve as the major heat source here, while convection and radiation can be neglected in this area. Mesh refinement technique has been applied inside and around the end windings region which is the most temperature sensitive area of the motor. Both radial and axial cross-sections of the motor

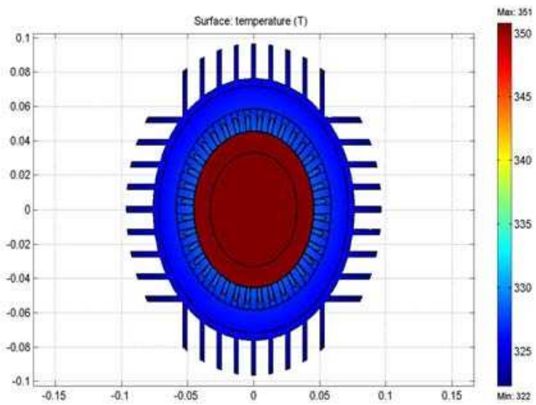

Fig.3.

have been analysed. Typical results of this analysis are presented in Fig. 3 and 4. The results of the thermal analysis can be exported into SIMULINK and a control loop has been constructed to allow for postponing the operation of the motor when the temperature exceeds the motor allowable temperature. Simulations have been performed in real time for up to two hours for several different torques, voltages and frequencies.

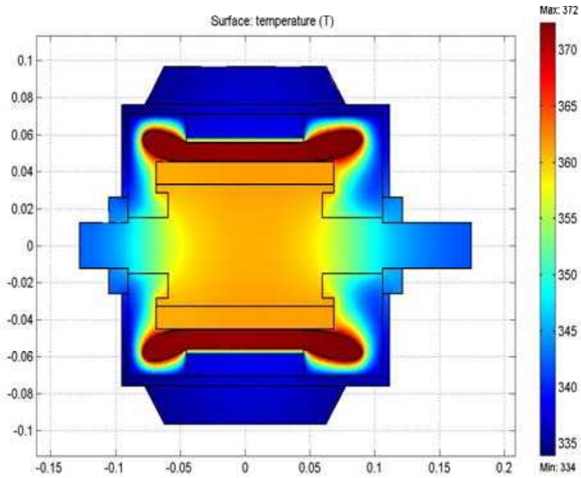

Fig.4.

The steady-state analysis of the induction motors has been conducted to identify the most critical regions in terms of temperature rise. A snapshot of this analysis is given in Table 1, where the results of computations are presented for operational frequency f=50Hz, slip s=0.03, and voltage 212V.

In terms of the experimental validation of the developed model, temperature sensors of the PT1000 type have been fixed inside the motor, in particular in the areas of bearings (on the fan side), end windings (inside and outside), the air gap and the frame (on a fin and between fins). A switch box has been used to control the status of these sensors and temperature sampling has been made every 3.5 seconds. Experimental measurements in the frequency range from 5 to 100Hz, different voltages ranging from 32 to

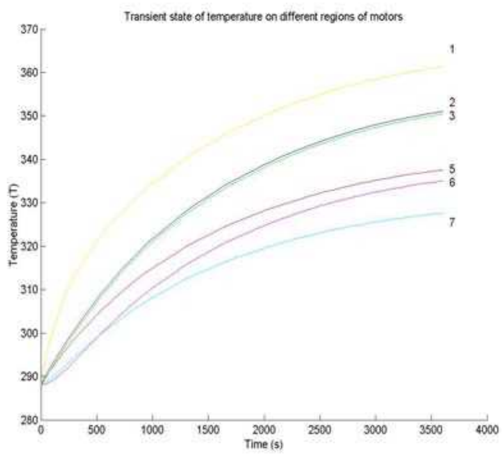

Fig. 5.

308 and different torques have been used to validate

the developed model.

Motor components	Temperature rise
End winding	54.76
Fan side end winding	52.28
Air gap	47.34
Rotor coil	46.67
Rotor core	40.45
Bearing	35.06
Fan side bearing	32.10
Frame	32.89
Fin of frame	30.15

Table 1.

Transient analysis (see Fig. 5) shows that the temperature will go to its steady state in approximately one hour. It is expected that with the implementation of advanced control systems the importance of knowledge on the dynamic distribution of temperature will continue to increase. Finally, we mention that seven plots of the temperature dynamics presented in Fig. 5 correspond to the end windings, the rotor coil, the rotor core, bearings, end caps, and the frame, respectively.

6. Conclusions

In this paper we have proposed a complete model for the thermal analysis of induction motors accounting for losses as functions of frequency change. The model has been applied to calculating temperature distributions of a squirrel-cage induction motor. The results of the calculations point out to the end winding areas as the most thermally sensitive areas of the motor, and these results can be used as an input to a control loop to prevent the overheating of the motor. The model developed can be used for a wide range of operating conditions with time varying frequencies and shaft loadings.

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