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Comparison of Thermal Stresses Developed during Transients on a Damaged Rotor Cage

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Abstract— Structural defects in the rotor cage of large electrical machines significantly impact their expected operational lifetime. This work simulates the stresses developed in a damaged rotor cage during different transient profiles and for different sizes of the imperfection. A combined model featuring electrical, thermal and mechanical stages as well as three different meshes reflecting a progressing narrowing of one of the bars in its junction to the end ring are used for this purpose. The results qualitatively agree with a previously presented fatigue test and show that under severe conditions the capability of aluminum to absorb stress is limited, and a state of plastic deformation is rapidly reached. The effect of a possible mitigation strategy is also studied.

Keywords— *Finite element analysis, induction motors, prognostics and health management, rotors, thermal stresses, transient analysis.*

I. INTRODUCTION

The computation of thermal-caused stresses in the rotor cage, arisen mainly as a consequence of severe startup or stall transients, is an important part in the design process of large motors. The usual procedure consists of simulating the currents in the rotor cage and applying thermal lumped models to calculate, considering adiabatic conditions, the expected temperature rise. These temperature profiles feed a linear elastic solver that yields the stresses caused by the thermal expansion of the material given the structural constraints of the element. The input of the maximum stress in a S-N diagram provides the number of cycles the machine can withstand, which should exceed what is established in the standards [1].

Nevertheless, vibrations [2], pulsating loads [3] and cage imperfections [4] could alter such theoretical performance, decreasing the expected lifetime. Although usually the failure mechanisms arising from these root causes are comprised under the term fatigue, phenomenologically significant differences exist: high cycle fatigue occurs when the stresses on the cage don't surpass the yield limit of the material, and low cycle fatigue, when plastic deformation occurs [2]. This later process is orders of magnitude faster than the former and rather than higher stresses, usually involves the decreasing of the material's

yield limit due to overheating. Hence the interest of motor manufacturers in the use of copper for the construction of the cage, due to its better mechanical characteristics and increased heat capacity [5], especially for large machines in which, since the cage cannot be easily oversized, are particularly prone to these failure modes [6].

In spite of the complexity of the problem and the computational limitations, the study of fatigue in rotor cages has drawn the attention of several authors related to the mining industry, which reflects the significance of the problem under the severe load conditions that motors are operated in such applications. Cabanes et al. in [7] pointed out the importance of thermal processes as a high cycle fatigue methodology to model the bar breakage was unsuccessfully applied. However, Pitis in [8] used a full analytical approach to study the thermo-mechanical stresses that arise in weak points ("hot spots") of the rotor bars, specifically in the filament joining the external and internal rotor cages of a big motor. The full phenomena comprised in the Physics of Failure for low cycle fatigue damage are correctly identified in this work and applied to a 2D section of the motor. In addition, the temperature rise during several transients is compared.

Following [1, 7, 8] and within the Condition-Based Maintenance and Prognostics and Health Management (CBM/PHM) architecture, [9] proposed a combined approach to simulate the effect of hot spots in the rotor cage by computing analytically the electromagnetic behavior of the motor and then reproducing the calculated currents in the cage on a 3D mesh representing half of the rotor. The yielded current distribution (only taking into account the resistances of the rotor elements) was used to obtain the heating and the temperature distribution, whilst thermal effects arising from magnetic phenomena were imposed on the mesh from independent analytical computations. The stator, where no such a detail was needed, was modelled using a thermal network [10, 11]. Finally, stresses developed due to thermal expansion were computed on the cage. Though numerous assumptions were made, the simulation results qualitatively explain the experimental results of a fatigue test.

The aim of this work is thus to extend the results presented in [9] by comparing the thermal and mechanical effects on a damaged rotor cage (featuring a hot spot or narrowing of one of the bars) during several kinds of transients and different sizes of the defect. The original section of the bar is reduced to 12%, 8%

and 4% and startup, plug stopping and stall operation modes are examined, this later considered the most damaging since there is no end space and air gap ventilation [12]. The effect of a possible straightforward mitigation method is also simulated. For this purpose, the rest of the paper is organized as follows, Section II gives an overall description of the combined model used here, already introduced in [9], Section III presents the simulation's details, Section IV shows the results and Section V yields the conclusions.

II. COMBINED MODEL

The computation of the stresses developed in a rotor cage is a problem that involves three fields of physics: electromagnetic, thermal and mechanical analysis. A rigorous attempt to study it, especially needed if the secondary circuit is asymmetric, would yield that most, if not all, phenomena are tightly coupled, making the problem intractable. Therefore, the strategy followed in [9] has been to combine analytical and FE methods using the advantages of Elmer FE software [13] whilst assuming a direct coupling of distributed parameters just for the variation of conductivity with temperature.

As introduced in [9], for the computation of the electromagnetic state of the machine an analytical model is used. Finite element analysis and a thermal network yield the thermal state of the rotor and stator, respectively. Finally, at given time steps, a linear elastic solver calculates the stresses on the cage. The first two stages are loosely coupled by updating the bar resistances in the analytical model according to the output of the thermal one, whilst the later has no influence in the two previous (i. e. the stresses and deformation of the cage do not alter the electromagnetic and thermal characteristics of the rotor.)

A. Analytical Model

Following the magnetic coupled circuits approach, the electromagnetic behavior of the induction motor whose characteristics are presented in the Appendix is reproduced by a set of $m+n$ equations (with m stator and n rotor phases) in the form:

$$[U_s] = [R_s][I_s] + \frac{d[\Psi_s]}{dt} \quad (1)$$

$$[0] = [R_r][I_r] + \frac{d[\Psi_r]}{dt} \quad (2)$$

where the first equation models the stator circuits, fed by external voltage, whereas the second is valid for the rotor ones, short circuited by the end rings. The flux linkages are defined as:

$$[\Psi_s] = [L_{ss}][I_s] + [L_{sr}][I_r] \quad (3)$$

$$[\Psi_r] = [L_{sr}]^T[I_s] + [L_{rr}][I_r] \quad (4)$$

The separate treatment of all the machine's circuits and an accurate computation of the self and mutual inductances L_{ss} , L_{sr} , L_{rr} in the previous equations by utilizing the circular convolution to calculate this magnitude from the conductor's distribution along the airgap allows reproducing the behavior of a non-symmetrical (faulty) induction machine [9]. However, no saturation of the iron is taken into account.

The mesh used in the next point is loaded applying half of the resistive voltage of (2) on the surfaces of the bars at the middle of the rotor. Once the thermal computation is performed, the rotor bar resistances are updated proportionally to the ratio between the current in (2) and the current actually crossing those surfaces on the mesh.

B. Rotor Thermal Model

Contrary to the electromagnetic one, the thermal model treats differently rotor and stator. For the rotor, partial differential equations accounting for currents and thermal flow are solved in 3D using the FE method. Since the heating and current flow are tightly coupled due to the variation of conductivity with temperature, these two phenomena are treated accordingly. However, in order to reduce the computational needs the currents in the rotor are reproduced as DC flow according to the values in the bars computed in the previous stage for each time step. In this sense, with respect to the electromagnetic model, the rotor thermal one acts as a surrogate model for the study of the rotor resistances, where distributed parameters are applied and then, when its solution is reached, the lumped values used in the electromagnetic one are updated accordingly.

The coupling of two FEM modules in Elmer, heat transfer and static current conduction, allows solving in sequence the Maxwell's and heat equations in the rotor mesh for each time step until a common solution is found. For quasistatic approximation, the curl of the Faraday's and the curl's divergence of Ampere's laws are equal to zero and hence the electric scalar potential ϕ can be used to univocally compute the distribution of currents in the mesh [9]:

$$\nabla \cdot \sigma(\theta) \nabla \phi = \frac{\partial \rho}{\partial t} \quad (5)$$

The Joule heating is calculated simply as:

$$h_{joule} = \nabla \phi \cdot \sigma(\theta) \nabla \phi \quad (6)$$

and input into the right-hand side of the heat equation, to be solved for θ :

$$\rho_d c_p \left(\frac{\partial \theta}{\partial t} + (\vec{u} \cdot \nabla \theta) \right) - \nabla \cdot (k \nabla \theta) = \rho_d h \quad (7)$$

Regarding the parameters required, σ accounts for the electric conductivity, which in the aluminum regions is variable with temperature and provides the coupling parameter between the Static Current and Heat Transfer FE modules; whereas in the rotor's iron, σ is a second order constant tensor to take into account the negligible conductivity in the axial direction due to the effect of the laminations, being the values in the other two directions the corresponding to the interbar resistance assumed in [14]. The thermal conductivity k for the iron, a critical parameter, is treated similarly using the values provided by [3]. Furthermore, ρ_d is in this case the density of the material and c_p the heat capacity.

The thermal effects of the magnetic phenomena are calculated analytically and imposed on the mesh. In the iron a new term is added to the heat source of Eq. 7 to reflect the Eddy current losses:

$$h = h_{joule} + h_{losses} \quad (8)$$

and the skin effect in the rotor bars is approximated by modifying the heat capacity of the bars along their height, according to the field frequency obtained from the electromagnetic module [9]. In this manner, the same amount of current causes more heating at the top of the bar than at the bottom. The implementation of this approach on the complex geometry of the end ring is not possible using just an analytical approach and has not been carried out in this work.

C. Stator Thermal Model

The thermal behavior of the stator, cover and air regions inside the motor is simulated by a lumped-parameter thermal network [3, 10, 11]. Rotor and stator are coupled by Neumann boundary conditions on the mesh (indicating a heat flux) and the corresponding heat sources in the network for the shaft, end ring and rotor iron surface.

D. Linear Elastic Model

The mechanical effects on the cage produced by the heating are calculated as a post-processing stage by the Elmer's Linear Elastic module, using the dynamical equation for elastic deformation of solids (9) to obtain the displacements in the mesh $\vec{d}$ and the linear strain ε :

$$\rho_d \frac{\partial^2 \vec{d}}{\partial t^2} - \nabla \cdot \tau = \vec{f} \quad (9)$$

$$\varepsilon = \frac{1}{2} (\nabla \vec{d} + (\nabla \vec{d})^T) \quad (10)$$

$\vec{f}$ is volume force, and τ the stress tensor:

$$\tau^{ij} = C(\theta)^{ijkl} \varepsilon_{kl} - \beta^{ij} (\theta - \theta_0) \quad (11)$$

where $C(\theta)$ is the temperature-dependent elastic modulus tensor, β the thermal expansion tensor, in this particular case both reduced to scalars and θ_0 the reference temperature: 316.9 K for cold startup or stall, 335 K in the case of cold plug stopping and 443 K for warm plug stopping.

III. SIMULATIONS

The simulations intend to reproduce the conditions under which a bar breakage naturally developed during a fatigue test [15]. No rotor quantities (besides speed) were measured then, thus even though a full validation of the results is not possible, at least a qualitative comparison can be carried out. Consequently, three different meshes have been employed in this work, as shown in Fig. 1. Featuring half of the rotor, their construction is symmetric, albeit the hot spot created in the end ring at the junction of one of the bars, whose cross section is varied modifying the centers of the drills and their diameter. This reproduces the machining process carried out in [15] to weaken one of the bars, first to 14 % of its total cross section (Fig. 1 b)) and then up to 7 % (Fig. 1 a)). These sizes of the defect caused in the analysis of the stator's transient currents for the first state,

a small worsening trend of the rotor asymmetry indicators (indicating damaging was progressing) and a fast evolution (in few hundred cycles) for the second, until full breakage was attained. Therefore, in order to explore that range, the geometries used in the simulation have a remaining section of 4% (Fig. 1 c), also utilized in [9]), 8% (Fig. 1 d)) and 12% (Fig. 1 e). Table I depicts the main parameters common to the simulations.

In [15] the cycling consisted of a heavy startup transient, lasting up to 7 seconds, a stationary period of 13 seconds and a plug stopping of around 5.5 seconds, followed by a cooling down interval. Additionally to the startup and plug stopping, in this work, stall transients and the effect of a possible mitigation strategy consisting of installing a fan turned by an independent motor in the end space region of the machine –intended for larger ones– have been simulated. This later case is modelled in this volume by suppressing the variation of the convection coefficient with the speed of the machine. A constant value corresponding to 3,000 rpm for the whole simulation (118.7 W/m² K) has been programmed instead. Table II summarizes all the cases computed.

Fig. 1. Weakening of the bar carried out in [15] with estimated remaining section 7 %, a) and 14 % b) (top). Meshes used to compute the thermal and mechanical state of the rotor showing different remaining section of the bar: 4% c), 8% d) and 12% e) (bottom).

An initial thermal state of the rotor bodies and the thermal network is necessary to begin the time-stepping simulation. Cold conditions for the startup and stall operation assume a uniform distribution of temperature in the motor, almost at equilibrium

with the ambient, in agreement with the available experimental data [9]. However, the study of the plug stopping transient, either in “cold” (first cycle after a prolonged rest period) or warm (after around 10 cycles) states, involve the existence of thermal gradients within the machine at the instant the simulation begins. Thus, initial values have been obtained running a full thermal network (depicting also the rotor) according to the cycling carried out in experimental testing, up to the point when the plug stopping was switched on [15]. Nevertheless, this initialization, along with the iron-bar contact phenomena, remain the biggest causes of inaccuracy for the present approach.

TABLE I. PARAMETERS OF THE SIMULATIONS

Voltage	400 V	σ_{xy} (Fe)	$1.67 \cdot 10^3$ S
Frequency	50 Hz	σ_z (Fe)	0
Inertia, J	0.13 kg·m ²	k_{xy} (Fe)	31 W/m·K
External Temperature	297 K	k_z (Fe)	0.6 W/m·K
Time step, EM model	$1 \cdot 10^{-6}$ s	k (shaft)	31 W/m·K
Time step, FE model	$1 \cdot 10^{-3}$ s	Cp (Fe, shaft)	449 J/kg K
Cp (Al)	897 J/kg K		

TABLE II. CASES STUDIED AS PERCENTAGE OF THE REMAINING BAR CROSS SECTION

Operation Mode	Initial thermal condition		
	Cold	Warm	Cold, Fan
Startup	4, 8, 12 %	-	4 %
Stall	4, 8 %	-	4 %
Plug stopping	4, 8 %	4, 8 %	-

IV. RESULTS

This section presents a comparison among the results of the previous 11 simulations, which on average took in a PC one day per second computed.

A. Cold Startup Cases

Fig. 2 a) shows the average temperature rise along a radial line centered in the narrowest part of the hot spot during the startup transient, for cold conditions. As the section of the bar is reduced from 12% to 8% the maximum temperature rises from 370.7 K to 378.7 K, both peaking at 3.5 s. However, when the fault progresses (remaining cross section equal to 4 %) the increase of resistance limits this value to 374.8 K and maintains it for around one second (3.5–4.5 s). The shape of the heating curve is similar for the first two cases, whereas for the later irregularities appear. The averaged maximum temperature rise is 57.8 K and the effect of enhancing the convection in the end space, negligible.

Fig. 2 b) shows the curves regarding the maximum Von Mises stress on the surface of the hot spot. Dashed lines indicate the computed yield stress limit of aluminum for the temperatures (and cases) presented in Fig. 2 a). For similar temperature increases, the wider shape of the saddle reduces the Von Mises stress values during the entire transient below the yield limit (σ_y) in the case of 12% remaining bar section. The 4% and 8% the curves, though, approach plastic deformation conditions and, in the former case, clearly surpass it between 2–4 s. The effect of cooling down the end ring during the entire transient (*fan*) is greater for this magnitude, but on average the difference is small: 0.23 MPa and does not decrease the peak value.

An analysis of the transient stator currents, a method of diagnosing rotating electrical machines using externally measurable quantities, yields, for the Wigner-Ville based indicator γ_w (Eq. 5 in [15]) that the energy of the main current component’s low sideband harmonic, LSH-50, reaches a value of 54.4 dB for the 12 % reduction, 50.9 dB for the 8 % case and 49.1 dB for the 4% one, well within the values considered healthy state (below 40 dB). During the fatigue test, the measurements yielded for a size of 14 %, 56 dB and for 7 %, 47 dB [15].

B. Stall and Plug Stopping Cases

The temperature evolution in the hot spot for the other two kinds of transients is shown in Fig. 3 a) grouped in three sets: cold stall transients, simulated from an initial temperature of 316.9 K, cold plug stopping, having an initial temperature of 335 K and warm plug stopping, starting at 443 K. All were carried out in both 4% and 8% sizes of the defect as well as for improved convection conditions in the end ring (4% fan), but in this case only for stall.

The heating of the hot spot in all these cases follows a first order system’s step evolution, especially clear for the stall transient, for which the currents in the bars are roughly constant. Higher values of this magnitude during the plug stopping transient cause a more intense heating in the first instants, in spite of the better heat transient coefficient, and a stabilization afterwards showing the effect of the decreasing currents as the rotor decelerates. The higher voltage between the ends of the bars for these severe cases has a clear impact when the size of the defect is bigger.

Fig. 3 b) presents the Von Mises stress obtained at the surface of the hot spot for the cases whose temperature is shown in Fig. 3 a), as well as the yield limit of aluminum at the temperature of the least severe of them (the values for the rest of the transients are lower). It can be appreciated how the temperature rise rapidly exhausts the elastic properties of this metal. With the exception of the cold stall case (size of the defect 4%) and its equivalent with enhanced cooling of the end ring, all surpass the yield limit in the first half second after connection. Nevertheless, the reduction of currents during the plug stopping maintains the maximum stresses between 35 and 40 MPa, (contributing probably also the relative higher temperature of the end ring), behavior that cannot be appreciated during the stall transients, where the damage increases with time.

Fig. 2. Temperature evolution in the hot spot during the startup transients (a) and stresses developed (b).

Fig. 3. Temperature evolution in the hot spot during the stall and plug stopping transients (a) and stresses developed (b).

It is worth to mention the differences between the cold start 4% case in Fig. 2 and stall 4% one in Fig. 3. Higher temperatures and stresses appear in the first one, depleting the elastic properties of aluminum after just 1.5 s, whereas in Fig. 3 b) it is clearly shown that for stall this happens at 3.5 s, when the Von Mises stress in the saddle surpasses the elastic limit of the material. This is caused by the skin effect, since during stall conditions the heating is more intense at the top of the bars, but the hot spot is situated at the bottom of one of them, thus effectively improving the cooling in that case. During the cold startup, however, due to the reduction of frequency in the rotor currents, the bar warms up more evenly and thus the hot spot is facing higher temperatures from that side. Therefore, this different behavior is a particular effect of the geometry and not a generalizable result.

Finally, as for the previous point, the effect of improving convection in the end space region affects the Von Mises stress values in a more clear way compared to temperature, but for both magnitudes is not significant.

C. Increasing the Convection Coefficient in the End Region

The effect of increasing the convection coefficient on the end ring surfaces, by means, for instance, of a fan independently moved by another motor is almost unappreciable in the previous diagrams. However, Fig. 4 depicts the difference in temperature for the last computed state of the rotor on the mesh featuring the reduction of the cross section of one of its bars to 4%. Temperature of stall operation is subtracted here from temperature of stall operation with higher convection.

Contrary to what it could be expected, the values in the hot spot increase when the end ring is cooled down, mainly because the resistance of the later decreases. A rise of 2.71 K is appreciated at the end of the long stall transient. Overall, the temperature of the rotor is also slightly higher. The thermal network yields an increase of 31.4 K in the end space region and the 0.23 K in the motor's cover. The variation for the rest of the nodes is negligible.

Fig. 4. Temperature difference at the end of the transient ($t=7$ s) caused by increasing convection on the end ring surfaces during stall condition, 4 % remaining bar section. The hot spot temperature rises due to the reduction of resistance in the end ring.

In addition, Fig. 5 shows the difference in the bar resistance between the stall transient with increased convection and without it, as processed by the EM analytical model. During the first 100 ms the thermal model does not update this value to avoid the electromagnetic transient's oscillations [9]. As the computation progresses the apparent resistance of the bar decreases, which corresponds to a higher circulating current.

Fig. 5. Difference of resistance during the transient for the bar facing the hot spot as seen from the analytical model.

Finally, Fig. 6 compares the power transferred through the air regions (end space and airgap) during the cold startup and cold stall operation modes, with (*fan*) and without increased convection in the end space, for the size of the defect corresponding to a 4 % of reduction in the section of the bar. The cold stall case yields straight lines, as the components in the machine heat up, since all the heat transfer coefficients and power sources are approximately constant. The power flowing

through the airgap for the startup ones also follows the behavior of the heat sources, stabilizing as the currents in the rotor decrease. However, the end space presents a different pattern when the speed of the rotor modifies not only its convection coefficient, but also the overall power transferred from the motor to the ambient, due to the action of the external fan. Thus, the curves corresponding to the cold startup and cold stall –both with increased convection, *fan*– coincide for the first instants of the transient, and then begin to differ as more heat is evacuated through the cover in the startup case.

Fig. 6. Power transferred from the rotor through the end space region (ES) and the rotor iron surface during different startup and stall transients. The effect of the increased convection coefficient in the end region's volume is appreciable on this magnitude.

D. Discussion of the results

Regardless of the numerous assumptions made, the simulated results presented in the two initial points of this section qualitatively agree with the experimental data acquired during a fatigue test in [15]. For this machine and operating conditions, the reduction of the cross section of one bar must be below 15 % to trigger a low cycle fatigue process. Damage probably begins to happen in the plug stopping for cross sections below this value, what would explain the slow evolution observed in the experiment when the bar's area was reduced to 14 % of its actual size (Fig. 1 b)). Around 8 % a second stage is reached, as the maximum thermal effects take place, which corresponds to the hot spot's section when the bar breakage naturally developed in the test (Fig. 1 a)). Finally, as the size of the defect further reduces, the amount of current through it decreases and accordingly the temperature of the hot spot.

These high temperatures rapidly exhaust the capability of the aluminum to deform elastically, regardless of the initial conditions for the specially severe case of plug stopping, and plastic distortion appears, which leads, as the cage cools down, to development of cracks and low cycle fatigue. Due to its limited heat capacity, any cooling enhancement involving air as a fluid has minimal impact when such relatively big powers, as the ones developed during a direct-on-line transient, are

involved. On the contrary, thermal gradients may increase which could lead to higher stresses.

Furthermore, the analysis of the simulated stator currents provide a general quantification parameter for the performance of the combined model, at least in what concerns to the electrical and thermal part. The results are roughly within 10% of error when compared to the ones experimentally measured in [15] for the same state of the motor. Nevertheless, the electromagnetic part is manifestly limited: (2) must be improved and, in addition, saturation taken into account. Replacing that part with a 2D Finite Element model would enhance the overall accuracy but at the cost of increasing the computational time an estimated one order of magnitude. Thermal improvements in the computation are also possible for the heat transfer in the drills. However, the biggest unknown remains the interbar resistance, a value that is not even constant during the lifetime of a machine.

Finally, concerning a full experimental validation of the combined model, stall tests also measuring temperatures in the rotor could be arranged. This would provide a necessary feedback regarding the output of the electromagnetic and thermal models. Nonetheless, a complete confirmation of the results in actual operating conditions, with the rotor spinning and acquiring actual stresses, remains challenging.

V. CONCLUSIONS

The capacity of storing heat and preventing a rise of temperature that produces stresses above the yield limit of the material is depleted if hot spots exists in the cage of an induction motor. In such cases, even very short, severe transients can cause plastic deformation and hence, low cycle fatigue, which sharply reduces the useful life of the rotor. An electromagnetic-thermal-mechanical combined model, weakly coupling most of the magnitudes involved, is able to provide a correct description of the implied processes. However, for quantitative validation, further improvements, especially in the electromagnetic part, and experimental validations are mandatory.

VI. APPENDIX

Motor characteristics: Star connected, rated voltage (U_n): 400 V, rated power (P_n): 1.5 kW, 1 pole pairs, stator rated current (I_n): 3.25 A, rated speed (n_n): 2860 r/min.

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