Reliability-Oriented Design of Low-Voltage Electrical Machines Based On Accelerated Thermal Aging Tests

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Abstract — In transport applications, electrical machines are required to be highly reliable, in order to fulfill the intended mission profile over the expected lifetime. This task has been conventionally addressed through the adoption of safety-factors that might lead to an over-engineered electrical machine. Only recently, few works have started championing a paradigm shift towards physics of failure methodologies, which allow to achieve an appropriate trade-off between optimal performance and demanded reliability figures. Thus, this paper proposes a reliability-oriented approach for low-voltage electrical machines and outlines its critical implementation steps. Accelerated thermal aging tests are preliminarily performed on custom-built specimens to assess the aging trend of the turn-to-turn insulation system. The thermal endurance graph at several percentile values is then determined and a lifetime model suitable for variable duty motors is developed. Finally, this model is used to predict the turn-to-turn insulation cycles-to-failure of an electrical machine installed on full-electric vehicle.

Keywords — Accelerated Thermal Aging Tests, Dielectric Breakdown, Electrical Machines Insulation, Physics of Failure, Design of Experiment, Safety-Critical Applications.

I. INTRODUCTION

Throughout the decades, the electrical machine (EM) design has been gradually refined, and nowadays, EM designers can rely on various engineering tools, which enable accurate and optimal designs. Indeed, the performance-oriented design method already allows to develop EMs that can reach excellent operating performance values, in terms of both efficiency and power density [1, 2]. In transport applications, these are essential features, because a compact and lightweight design is desirable. However, the safety-critical nature of mobile applications also imposes strict reliability figures [3, 4].

From a lifetime prospective, the winding insulation system often represents a bottleneck, since uncontrolled over-temperatures might trigger a significant degradation of the insulation, compromising its dielectric properties [5]. In fact, studies revealed that approximately the 30% of the EM failures are associated to winding faults [6, 7]. Focusing on the insulation system, the reliability constraints are usually met through an over-engineering strategy, which consists in using safety-factors, whose values come from rules of thumb and/or previous experience. Nevertheless, such practice can result in a lower slot fill factor that negatively affects both the EM

weight and its thermal management (i.e. dissipation of the heat produced inside the slot) [8]. In addition, by over-sizing the insulating material, the insulation system might greatly outperform the requested lifetime. Therefore, a design contrast between operational performance and reliability exists and it is source of conflicting design choices.

To pursue a proper balance between performance and reliability, a paradigm shift from performance- to reliability-oriented EM designs should be targeted [9, 10]. The flowchart of the typical reliability-oriented design is shown in Fig. 1, where reliability is considered from the early design stage (i.e. reliability as design objective) and it is evaluated through lifetime models based on physics of failure (PoF) methodologies. The latter provide a precise knowledge of the insulation degradation under specific stresses and the insulation system can be exactly defined in accordance with the reliability level required by the application at hand (i.e. no safety-factors are needed). The reliability-oriented design will then grant a better exploitation of insulating materials securing satisfactory reliability figures while meeting the performance requirements, without undermining the EM's weight, volume and cost [11].

Fig. 1. Reliability-oriented EM design approach.

In this paper, the procedure for a reliability-oriented EM design is outlined and the primary steps for its implementation are addressed. The turn-to-turn insulation of low-voltage random-wound EMs is examined via an accelerated thermal aging test campaign. For the investigated enamelled wire, a multi-percentile Arrhenius graph (i.e. thermal endurance graph) is drawn and employed to determine the parameters of

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a lifetime model built according to Arrhenius-Miner law. The model, which is suitable for variable duty EMs, is consequently tuned on the demanded reliability level (i.e. percentile of time-to-failure) and it is finally used to estimate both loss-of-life and cycles-to-failure of the turn-to-turn insulation belonging to a low-voltage random-wound EM meant for a full-electric minivan.

II. CONSIDERATIONS AND STUDY ASSESSMENT

Low-voltage EMs (i.e. rated below 700 Vrms) are normally equipped with random-wound windings, where the turn-to-turn insulation is composed by a thin layer of enamel made of organic material (i.e. Type I insulation). Due to its characteristics, the turn-to-turn insulation is commonly recognized as the weakest point in terms of expected failure [5], and for this reason, the preformed investigation purely focuses on such insulation system.

In general, the analysis of the turn-to-turn insulation lifetime is carried out through accelerated tests, where aging factors, as thermal, electrical, mechanical and environmental (i.e. moisture, pressure, etc.), are individually applied at greater level than that usually encountered in service conditions [12]. Then, the lifetime at normal operations is extrapolated from the data collected in abnormal circumstances, using a lifetime probability distribution (e.g. Weibull distribution). However, in real applications, the aging factors (i.e. degrading stresses) act concurrently on the turn-to-turn insulation and a comprehensive lifetime prediction can only be given by multi-stress models [13, 14], which are relatively complex and time consuming to build. Additionally, it results challenging to precisely quantify the combined aging caused by the concomitant application of more aging factors, since the superimposition principle cannot be applied [15]. This issue is typically overcome by assuming a prevailing aging mechanism that predominantly affects the insulation lifetime, whilst other stresses feature a negligible impact on the lifetime consumption (i.e. secondary aging factors) [15, 16]. Under such assumption, a single-stress lifetime model can be employed to predict the turn-to-turn insulation lifetime.

In this work, the thermal stress (i.e. temperature) is assumed as prevailing aging factor. Thus, the presented lifetime evaluation is based on a single-stress lifetime model, which can be only applied on partial discharges (PD) free, low-voltage EMs, where the insulation aging is mainly due to the temperature.

A. PD Risk Evaluation

In low-voltage random-wound EMs, the PD activity might be source of severe faults, because Type I insulating materials are not PD resistant [17]. In order to evaluate the risk of PD inception, the whole electric drive needs to be considered, because the combination among power converter, connecting cable and EM might lead to a voltage enhancement at the EM terminals [18, 19]. According to the IEC standard [17], a PD free turn-to-turn insulation is gained when the peak voltage across two adjacent turns is lower than the minimum PD inception voltage ($PDIV_{min-t-t}$), which is given by (1).

$$PDIV_{min-t-t} = 0.7 \cdot K(t_r) \cdot V_{DC} \cdot OF \cdot EF \quad (1)$$

In (1), EF is the enhancement factor, OF is the overshooting factor, V_{DC} is the DC link voltage and $K(t_r)$ is a coefficient function of the voltage rise time t_r . The values of the factors in (1) can be selected from [17], based on both

electric drive and magnet wire attributes. Considering an electric drive with the specifications listed below:

- 1) 270 V DC link voltage;
- 2) two-level power converter mounting silicon IGBT with 200 ns rise time and unipolar waveform;
- 3) connection cable between EM and power converter shorter than 10 m;

the $PDIV_{min-t-t}$ results equal to 330 Vrms (i.e. $V_{DC} = 270$, $K(t_r) = 1$, $EF = 1.4$ and $OF = 1.25$), whereas the measured turn-to-turn PD inception voltage ranges from 480 Vrms to 500 Vrms, for the studied magnet wire. The $PDIV$ measurements are taken with a transverse electromagnetic (TEM) antenna and the Techimp PD-Base® post-processing software. Relying on the obtained findings, the PD risk is reasonably confined when the examined magnet wire is used in an electric drive that fulfils the specifications discussed above (i.e. PD free EM).

B. Motorettes and Magnet Wire

Accounting that the EM is free from PD, it is plausible to suppose the thermal stress as the main aging factor. Therefore, the thermal stress influence is evaluated via accelerated thermal aging tests at constant temperature. These tests are carried out on motorettes (i.e. specimens), which are available well earlier than the first set of EM prototypes. A single motorette has 6 slots and a tooth-wound, double-layer winding with the 2 layers (i.e. 2 active sides of different coils) located radially within the slot, as depicted in Fig. 2. The motorette winding layout is detailed in Fig. 3, where a total of 6 coils per motorette are illustrated and every coil features 2 parallel strands, for assessing the turn-to-turn dielectric properties during the diagnostic session [20].

Fig. 2. Motorette employed during the accelerated aging tests.

Fig. 3. Motorette winding layout.

The winding is made of a Class 200, Grade 2, 0.4 mm diameter dual-coat magnet wire, i.e. the insulating enamel layer consists of a modified polyester base-coat plus a polyamide-imide over-coat. Dual-coat magnet wires are commonly employed in high performance EMs, because they reveal better heat-shock and abrasion resistance than

monolithic coating (i.e. single-coat magnet wire). In fact, the polyamide-imide over-coat grants a good abrasion resistance, whereas the modified polyester base-coat assigns high flexibility [21].

III. ACCELERATED AGING TESTS CAMPAIGN

Using the dual-coat magnet wire, a set of 15 motorettes is wound following the winding arrangement of Fig. 3. A Nomex® type slot-liner of 0.13 mm thickness is placed inside the slots to avoid the direct contact between enamel and motorette core. This expedient strengthens the phase-to-ground insulation and prevents the enamel scratching when the coils are inserted in the slots.

For a given aging temperature, the accelerated thermal test is performed on a set of 5 motorettes, counting 30 coils in total (i.e. 6 coils per motorette). Although a minimum number of 10 samples is recommended by technical standard [22], a higher number is adopted, in order to account for any discrepancies in the physical make-up of the random-wound winding and to enhance the statistical validity of the study.

The accelerated aging test procedure is sketched in Fig. 4, where the corresponding flowchart is reported. At groups of 5, the samples are fitted in a ventilated oven and they are aged at three different temperatures above the magnet wire thermal class (i.e. 200 °C). In particular, the aging temperatures of 230 °C, 250 °C and 270 °C are chosen and the samples are exposed to several thermal cycles, whose duration depends on the temperature level, as listed in Table I. Once the thermal exposure is completed, the samples are naturally cooled down to room temperature and the turn-to-turn insulation is evaluated (i.e. diagnostic session).

Fig. 4. Flowchart of the accelerated thermal aging tests

The diagnostic session consists in a pass/fail test, namely AC hipot test [23, 24], which is conducted through the dissipation factor tester (i.e. Megger® Delta 4000). During this test, a 500 Vrms sinusoidal voltage at 50 Hz is applied for 1 minute across the strands belonging to the same coil [22], e.g. between the terminals “#1A1_a” and “#1A1_b” of coil “#1A1” in Fig. 3. Here, the diagnostic voltage amplitude is set equal to the magnet wire’s *PDIV* value (i.e. 500 Vrms) to derive a more conservative lifetime estimation. Since the dielectric breakdown is selected as end-of-life criterion, the coil is established ‘dead’ when it does not withstand the diagnostic voltage (i.e. insulation breakdown occurs) and its end-of-life is estimated at half of the last thermal cycle.

Therefore, the time-to-failure is determined as the sum of the total thermal exposure hours minus the duration of half thermal cycle. After completing the diagnostic session, all the ‘still-alive’ coils are ready for a new thermal cycle and the accelerated test at a specific temperature ends when all samples fail the AC hipot test.

TABLE I. DURATION OF THE THERMAL EXPOSURE CYCLES [22]

Aging Temperature [°C]	Cycle Exposure Time [h]
230	384
250	96
270	24

IV. TESTS CAMPAIGN RESULTS AND MULTI-PERCENTILE ARRHENIUS GRAPH

The recorded time-to-failures are statistically post-processed via a two-parameter Weibull distribution, whose cumulative distribution function (CDF) $F(t_i)$ is provided in (2), where α is the scale parameter (i.e. the 63.2% percentile of the lifetime), β is the shape parameter (i.e. inverse of the data scatter) and t_i is the generic time-to-failure.

$$F(t_i) = 1 - e^{-(t_i/\alpha)^\beta} \quad (2)$$

These parameters are determined using a graphical method based on the linear regression approach. As first step, the Weibull CDF is linearized, as detailed in (3).

$$\ln \left[\ln \left(\frac{1}{1-F(t_i)} \right) \right] = \beta \cdot \ln(t_i) - \beta \cdot \ln(\alpha) \quad (3)$$

Introducing the auxiliary variables x and y , respectively defined in (4) and (5),

$$x = \ln(t_i) \quad (4)$$

$$y = \ln \left[\ln \left(\frac{1}{1-F(t_i)} \right) \right] \quad (5)$$

the Weibull CDF is rewritten as in (6), which is the equation of a straight line with $\beta \cdot \ln(\alpha)$ as y-axis intercept and β as slope.

$$y = \beta \cdot x - \beta \cdot \ln(\alpha) \quad (6)$$

For each collected time-to-failure t_i , the Weibull CDF is estimated by the median rank estimator, according to the Benard's approximation (7) [12], where i is the i^{th} insulation breakdown and N is the samples' population.

$$\hat{F}(t_i) = \frac{i-0.3}{N+0.4} \quad (7)$$

The points having as x-axis coordinate the time-to-failure t_i (experimentally gathered) and the corresponding estimation of the Weibull CDF $\hat{F}(t_i)$ as y-axis coordinate are located on the probability plotting paper. Afterwards, the best possible straight line fitting these points is drawn relying on the linear regression (e.g. least squares method). Thus, the Weibull probability plot, which allows to evaluate the probability of failure at a generic time instant, is obtained and the associated α and β parameters are found. In Figs. 5, 6 and 7, the Weibull probability plots with 95% confidence interval are shown, for the aging temperatures of 230 °C, 250 °C and 270 °C respectively. The resulting scale and shape parameters are listed in Table II, along with the time-to-failure at B_{50} (i.e. 50% percentile) and B_{10} (i.e. 10% percentile). The lifetime percentile values B_p express the time when a predetermined percentage of samples will ‘die’, e.g. B_{50} indicates the time-to-failure related to the 50% failure probability.

Fig. 5. Weibull probability plot of the time-to-failures at 230 °C.

Fig. 6. Weibull probability plot of the time-to-failures at 250 °C.

Fig. 7. Weibull probability plot of the time-to-failures at 270 °C.

TABLE II. WEIBULL PARAMETERS AND FAILURE TIME PERCENTILES

Temperature [°C]	α [h]	β [-]	Mean [h]	B50 [h]	B10 [h]
230	4809	3.54	4330	4337	2550
250	1307	3.08	1168	1160	629
270	337	6.98	316	320	244

Knowing the Weibull probability distribution, the time-to-failures at several percentiles are calculated for each aging temperature and the Weibull CDFs are plotted, as illustrated in Fig. 8. Then, the Weibull CDFs are used to extrapolate the multi-percentile Arrhenius graph, which delivers the lifetime in normal operating conditions for different percentiles (i.e. reliability levels). Assuming a certain percentile (e.g. 50% percentile), the correspondent thermal endurance line (e.g. Arrhenius graph at 50% percentile) is sketched by interpolating the three time-to-failures of the CDFs at the selected percentile value. By repeating this procedure for various percentiles, the multi-percentile Arrhenius graph is accomplished, as reported in Fig. 9, where only 10% and 50% percentiles are considered,

for the sake of drawing clarity. Despite the selected percentile in Fig. 9, any other percentile could have been employed, since it depends on the reliability level demanded by the application at hand (i.e. design requirement). It is worthy to underline that a lower percentile leads to a more stringent reliability constraint.

Fig. 8. Weibull CDFs of the three aging temperatures.

Fig. 9. Experimental multi-percentile Arrhenius graph.

V. MULTI-PERCENTILE LIFETIME MODEL FOR VARIABLE DUTY EMS

Based on the outcome of the statistical analysis, a single-stress thermal lifetime model is built to predict the life of the EM's turn-to-turn insulation under variable duty operations. The multi-percentile Arrhenius graph of Fig. 9 is mathematically described by the Arrhenius's equation (8).

$$L_{B\rho}(\theta) = L_{0-B\rho}(\theta_{0-B\rho}) \cdot e^{-R_{B\rho} \left[\frac{1}{\theta_{0-B\rho}} - \frac{1}{\theta} \right]} \quad (8)$$

In (8), $L_{B\rho}(\theta)$ is the life at the generic constant temperature θ for the percentile $B\rho$, $L_{0-B\rho}$ is the life at the reference temperature $\theta_{0-B\rho}$ for the same percentile $B\rho$, while $R_{B\rho}$ is a characteristic parameter of the insulating material. Assuming the thermal class as reference temperature, i.e. temperature that guarantees 20,000 hours of continuous operation, the Arrhenius's equation (8) is rearranged as in (9), which represents the lifetime model for continuous duty EMs (i.e. operating at constant temperature).

$$L_{B\rho}(\theta) = 20,000 \cdot e^{-R_{B\rho} \left[\frac{1}{\theta_{0-B\rho}} - \frac{1}{\theta} \right]} \quad (9)$$

Considering 10% and 50% percentiles, the parameter $R_{B\rho}$ and the thermal class $\theta_{0-B\rho}$ are graphically obtained from Fig. 9 and their values are summarized in Table III.

TABLE III. PARAMETERS OF THE ARRHENIUS'S EQUATION

Percentile	Parameters	Value
10%	θ_{0-B10}	468.9 K
	R_{B10}	16028 K
50%	θ_{0-B50}	482.4 K
	R_{B50}	17793 K

For variable duty EMs (i.e. operating at variable temperature), the lifetime model is developed by combining the Arrhenius' law with the cumulative damage law (i.e. Miner's law) [25]. Taking into account a generic time-variable temperature profile $\theta(t)$, whose time duration is indicated with Δt_{cycle} (i.e. cycle's period), the insulation loss-of-life occurring during a single cycle (i.e. single temperature profile) at a given percentile $B\rho$ is calculated as in (10).

$$LF_{cycle-B\rho} = \int_0^{\Delta t_{cycle}} \frac{1}{L_{B\rho}[\theta_i(t)]} dt \quad (10)$$

In (10), $LF_{cycle-B\rho}$ is the loss-of-life per cycle, dt is the infinitesimal interval of the temperature profile during which the temperature is supposed to be constant, while $L_{B\rho}[\theta_i(t)]$ is the insulation life at the constant temperature $\theta_i(t)$ that is determined by (9) [26]. Relying on the cumulative damage law [25], the number of cycles-to-failure $K_{B\rho}$ relative to predetermined percentile is computed according to (11).

$$K_{B\rho} = \frac{1}{LF_{cycle-B\rho}} \quad (11)$$

Therefore, the turn-to-turn insulation will experience a breakdown when the cumulative loss-of-life reaches the unity value, hence its total life L_{tot} is expressed by (12).

$$L_{tot} = K_{B\rho} \cdot \Delta t_{cycle} \quad (12)$$

Using the multi-percentile Arrhenius graph (i.e. Fig. 9), the developed lifetime model (10) is tailored/tuned on a particular percentile (e.g. 50%, 10%, etc.), which is contingent on the required reliability figure. This model is then utilized as life prediction tool in the reliability-oriented EM design (i.e. Fig. 1). Indeed, knowing the EM duty cycle, the associated losses are evaluated based on the EM preliminary design and they are fed to the EM thermal model (e.g. lumped parameter thermal network) [27-29]. The latter provides the winding temperature profile that is given as input to the lifetime model. By comparing the lifetime prediction (i.e. time-to-failure or cycles-to-failure) to the reliability specification (i.e. design constraint), it is checked whether or not the EM preliminary design fulfils the desired reliability level. In the affirmative case, the EM design can be finalized and the EM prototyped, on the contrary, the preliminary design should be revised. The applicability and practical utilization of the implemented lifetime model is addressed in the next section.

VI. AUTOMOTIVE CASE-STUDY

A full-electric minivan is considered as case-study, where the mechanical power at its wheels is provided by two identical EMs, while a gearbox is placed between every EM and the wheelbase (i.e. one EM per vehicle axle). Such architecture ensures the proper level of redundancy and availability for the entire system [30, 31]. The EM is a 24 slots / 20 poles, interior permanent magnet synchronous machine (PMSM) [32] and its geometry, resulting from the preliminary design, is depicted in Fig. 10. The PMSM is connected to the power electronics converter through a 3 m long cable and the electric drive parameters are listed in Table IV. Based on the specifications of Table IV, the electric drive meets the PD-free

requirements introduced in section II.A. Thus, under the assumption of thermal predominant aging stress, the presented lifetime model is employed to estimate both the loss-of-life and the cycles-to-failure of the EM's turn-to-turn insulation.

Fig. 10. Preliminary design geometry of the EM.

TABLE IV. ELECTRIC DRIVE PARAMETERS

Parameter	Data
Slot number	24
Pole number	20
Rated Speed	4500 rpm
Rated Torque	150 Nm
Stack Length	130 mm
Stator Outer Diameter	250 mm
Copper fill factor	0.5
DC link voltage	270 V
Switching frequency	10 kHz
Connection cable length	3 m
Cooling method	Forced liquid cooled

The EM's temperature profile depends on the vehicle drive cycle, as well as, the EM's characteristics, such as thermal loading and cooling method. A standard extra urban driving cycle (EUDC) is picked and it is periodically performed over one hour time window. Relying on the corresponding EM losses, the hot-spot winding temperature is yielded by the EM thermal model, as shown in Fig. 11 (red continuous line). This temperature profile refers to 25 °C ambient temperature and 40 °C inlet cooling liquid temperature. Looking at Fig. 11, the temperature exceeds the insulation thermal class (i.e. 200 °C) for short time periods leading to a considerable loss-of-life. The Arrhenius-Miner lifetime model (10) is adopted to predict the loss-of-life per hour and the associated trends are reported in Fig. 11, for both 10% (black dashed line) and 50% (black continuous line) percentiles. For the sake of completeness, the hourly loss-of-life values are also reported in Table V, along with the predicted cycles-to-failure calculated as in (11), where Δt_{cycle} is set equal to 1 hour.

As an outcome of the lifetime assessment, it is possible to claim that if 13,700 EMs are operated with the EUDC during 1 hour, then 10% of them would fail because of a turn-to-turn insulation breakdown caused by thermal aging. Despite the fact of accounting for just the thermal aging factor, the simulated EUDC is highly conservative from the lifetime consumption point of view. Indeed, the study is carried out under the assumption that throughout its whole lifetime, the electric vehicle is driven on extra urban roads only. Referring to the flowchart of the reliability-oriented EM design (i.e. Fig. 1), if the predicted cycles-to-failure comply with the reliability requirement, the EM can be prototyped, otherwise changes at EM preliminary design needs to be implemented. Nevertheless, it is worthy to emphasize that the gathered results are in line with the lifetime expectancy of a traction

EM for automotive, which is generally set to 10,000 hours or 300,000 km.

Fig. 11. EM's hot-spot winding temperature profile (continuous red line) over one hour EUDC and hourly loss-of-life for both 10% (black dashed line) and 50% (black continuous line) percentiles.

TABLE V. LOSS-OF-LIFE AND CYCLES-TO-FAILURE AT 10% AND 50% PERCENTILES

Percentile	Parameter	Value
10%	LF_{B10}	$7.28 \cdot 10^{-5}$ [p.u.]
	K_{B10}	$1.37 \cdot 10^4$ [cycles]
50%	LF_{B50}	$2.95 \cdot 10^{-5}$ [p.u.]
	K_{B50}	$3.39 \cdot 10^4$ [cycles]

VII. CONCLUSIONS

The paper proposed a reliability-oriented EM design approach aiming at overcoming the adoption of safety-factors to achieve the needed reliability figures. Such a paradigm shift addresses the necessity of complying with the stringent power density and reliability constraints demanded by modern EMs for transportation applications. The suggested approach deals with the turn-to-turn insulation of low-voltage random-wound EMs (i.e. weakest insulation point) and specifically targets the thermal aging. In fact, accelerated thermal aging tests were carried out for building single-stress thermal lifetime model for variable duty EMs, which offers the possibility of being tuned at specific percentile of time-to-failure. Both applicability and feasibility of the described methodology were proven by discussing an automotive case-study.

Although the experimental tests were performed on a particular enamelled wire and the findings can be directly used in the design of EMs employing the same insulation, a general procedure is outlined for tailoring the lifetime model on a different magnet wire topology. The discussed single-stress lifetime model represents just the first step towards the PoF methodology applied at EM. The research end-game consists in developing multi-stress lifetime models for a more advanced and comprehensive lifetime forecast.

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