ELECTROMAGNETIC FAULT ANALYSIS FOR HIGH SPECIFIC POWER PERMANENT MAGNET SYNCHRONOUS MACHINE

BY

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THESIS

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This thesis discusses the electromagnetic fault analyses for a high specific power 1 MW permanent magnet synchronous electric machine designed for aerospace applications. As the high specific power of 13.3 kW/kg of the machine is to be achieved by pushing the design parameters such as mechanical speed, electric current, and temperature, the importance of permanent magnet demagnetization and long-term insulation quality is stressed.

Demagnetization will be quantified using finite element methods, where the effects of permanent magnet material, air gap, and rotor back yoke material on demagnetization will be explored respectively. This is expected to provide insights to the machine designers to select appropriate materials and air gap, topics that have not been yet covered in previous work related with the particular design. Ultimately, this discussion will lead to an electro-thermal trade-off problem since permanent magnets are highly sensitive to temperature.

Secondly, this thesis discusses the insulation aging to determine the longevity of the design. Various aging models therefore will be introduced to provide an analytical basis to project the insulation lifetime on real applications. A preliminary aging experimental setup is presented, along with a partial discharge detection setup since partial discharges are suspected to be one of the main electrical aging mechanisms. Ultimately, the data obtained from the experiments must be fitted into the aging models. This thesis will present early experimental results.
To my family - Hyunjong, Min, and Heesook
Above all, I would like to thank my father and brother for their unconditional love and support. I am ever grateful for my mother as well, and sorry that she has not lived to see me graduate. Her memory will be with me always.

I would also like to express my sincere gratitude to my advisor, Dr. Kiruba Haran, for making my studies at the University of Illinois possible, with continuous and patient support. His insightful guidance has led me to explore with passion and form the basis of this research. Also, I would like to thank my colleagues, Xuan Yi and Thanatheepan Balachandran, for their help on demagnetization and the aging/PDIV setup.

Lastly, I want to express my gratitude to NASA and Grainger Center for Electric Machinery and Electromechanics for their financial support.
# TABLE OF CONTENTS

## CHAPTER 1 INTRODUCTION ........................................... 1
  1.1 Background ...................................................... 1
  1.2 Motivation ....................................................... 2

## CHAPTER 2 DEMAGNETIZATION ANALYSIS ............................ 5
  2.1 Theory ............................................................ 5
  2.2 Problem Statement ............................................... 7
  2.3 Methodology ..................................................... 9
  2.4 PM Material and Demagnetization .............................. 9
  2.5 Air Gap and Demagnetization .................................. 15
  2.6 Rotor Yoke Material and Demagnetization ..................... 21
  2.7 Conclusion ...................................................... 23

## CHAPTER 3 INSULATION AGING MODELS ............................ 24
  3.1 Motivation ....................................................... 24
  3.2 Arrhenius Aging Model ......................................... 26
  3.3 Inverse Power Law Aging Model ............................... 27
  3.4 Exponential Aging Model ....................................... 28
  3.5 Multi-Stress Aging Models ..................................... 30
  3.6 Crine’s Aging Model ............................................ 31
  3.7 Conclusion ...................................................... 33

## CHAPTER 4 EXPERIMENTAL SETUPS ................................. 35
  4.1 Accelerated Aging Setup with Impulse Generation .......... 35
  4.2 Preliminary Impulse Aging Setup Results ..................... 39
  4.3 PDIV Diagnostics Setup ....................................... 40
  4.4 PDIV Results ................................................... 44
  4.5 Future Work .................................................... 45

## CHAPTER 5 CONCLUSION ............................................ 46

REFERENCES ......................................................... 48
1.1 Background

This thesis is motivated by the recent trends in development of electric machine designs to achieve higher power density for aerospace applications. According to the International Air Transport Agency (IATA), it is estimated that 2.4 billion passengers and 40 million tons of goods were transported via airplanes in 2010. Demand for aero-transport is expected to keep increasing, marking an estimation of 16 billion passengers and 400 million tons of goods by 2050 [1]. While it is good news that the real cost of air travel has decreased by a factor of 40% for the past four decades, continuing this trend is critical to continue to support the expected demand spike over the next three decades. Another concern is an environmental impact of aviation as airplanes emit particles and gases such as carbon dioxide (CO$_2$), carbon monoxide (CO), and lead directly into the atmosphere [2]. These economic and environmental aspects of aero-transport suggest that development of more efficient and sustainable technology is essential.

As a result, there is a new interest in electrifying the key components in aircraft in order to achieve higher efficiency, less weight, and fewer emissions. However, despite the recent advancement of power electronics and batteries, it is still difficult to surpass the high energy density of conventional jet fuel (40-50 MJ/kg). Therefore, rather than immediately developing a fully electric propulsion technology, it would be more reasonable to propose a concept of hybrid electric aircraft powertrain. Hybrid, turbo-electric concepts would also be able to adopt the benefits from both conventional fuels and batteries.

To enable electric-hybrid aircraft propulsion, the National Aeronautics and Space Administration (NASA) has targeted an achievement of 13 kW/kg specific power for megawatt class electric machines [3]. Considering the state-of-
the-art aircraft propulsion motor has a specific power of 5 kW/kg [4], NASA’s goal is to increase the power density by a factor of 2-3. In response, a permanent magnet synchronous machine (PMSM) design has been proposed in [5] to achieve these aggressive goals by pushing the key machine loadings. The design has been validated via electromagnetic [6], [7], thermal [8], and mechanical analyses [9], [10]. In summary, this design includes an implementation of high pole count, high operating frequency, air-gap litz windings, Halbach magnet arrays, and outer-rotor assembly. These characteristics all aim to decrease the amount of heavy steel from the machine, which leads to an increase in specific power. High speed operation was implemented to compensate for high pole count to maintain the power output. Figure 1.1 shows a cross section of the proposed design while Table 1.1 summarizes the basic specifications of the machine.

1.2 Motivation

While previous work has determined nominal performance of the assembly, important fault condition analyses such as demagnetization risk assessment and stator winding insulation lifetime profile have not yet been determined. These are important factors because permanent magnets (PM) and air-gap stator windings are the key enablers of the proposed PMSM design.
Table 1.1: Basic machine specifications

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Target Power</td>
<td>1 MW</td>
</tr>
<tr>
<td>Mechanical Speed</td>
<td>15000 RPM</td>
</tr>
<tr>
<td>Pole Number</td>
<td>20</td>
</tr>
<tr>
<td>Electric Frequency</td>
<td>2.5 kHz</td>
</tr>
<tr>
<td>Yoke Inner Radius</td>
<td>128.30 mm</td>
</tr>
<tr>
<td>Yoke Thickness</td>
<td>6.33 mm</td>
</tr>
<tr>
<td>Stator Outer Radius</td>
<td>140.46 mm</td>
</tr>
<tr>
<td>Nominal Air Gap</td>
<td>1.14 mm</td>
</tr>
<tr>
<td>Magnet Thickness</td>
<td>12.45 mm</td>
</tr>
<tr>
<td>Rotor Outer Radius</td>
<td>159.13 mm</td>
</tr>
<tr>
<td>Active Length</td>
<td>241.3 mm</td>
</tr>
<tr>
<td>Per-phase RMS Current</td>
<td>93 A</td>
</tr>
<tr>
<td>Yoke Material</td>
<td>Ancorlam 2HR</td>
</tr>
<tr>
<td>Cooling Scheme</td>
<td>Forced Air Cooled</td>
</tr>
</tbody>
</table>

For instance, high specific power of PMSM is largely realized by energy-dense PM materials such as neodymium-iron-boron (NdFeB) and samarium cobalt (SmCo). However, PMs are inherently susceptible to the risks related with irreversible demagnetization which is often a result of thermal loadings and demagnetizing fields [11], [12]. The loss of magnetic strength can negatively impact the machine performance as the air-gap flux density decreases [13], and cause considerable economic losses as well since the demagnetized loss can only be recovered by separating the magnets from machine and re-magnetizing them via a large external B field [12]. Furthermore, demagnetization can be extended to a control problem, especially when the machine is operating in a constant power mode. If the machine is working to keep the power constant but is experiencing PM demagnetization at the same time, electric current input must increase to compensate for the magnetic field loss. This leads to an increase in thermal loadings, which exacerbates the demagnetization that may lead to a catastrophic failure. Thus, extensive attention should be paid to PM demagnetization to avoid this phenomenon in the design or analysis phase before the assembly is actually manufactured. This thesis will present finite element (FE) based results and examine the correlations with various conditions such as different materials, temperature, and air-gap length.

Regarding the stator windings, Renner et al. [7] have already presented an actual development and manufacture of a winding-insulation scheme. While
this gives interesting insights into the immediate usability of the stator windings, it should be noted that an aging test that can assess the longevity has not yet been discussed. As the qualification should be aimed to ensure that the assembly operates properly for a prolonged period, the insulation lifetime estimation should be highlighted. Therefore, this thesis will extend the discussion of the stator winding qualification by introducing insulation lifetime models, accelerated aging, and partial discharge (PD) detection setup. Note that an accelerated aging method should be incorporated for the lifetime testing since it is not feasible to place the stator windings in a normal operating condition for an extended period. Also, PD is an important aging mechanism because air pockets within the winding-insulation system may cause the occurrence of high-voltage discharges which will deteriorate the insulation quality. To mitigate these issues, an accelerated aging test setup and the design of a PD generation/detection method will be discussed.
CHAPTER 2
DEMAGNETIZATION ANALYSIS

2.1 Theory

The magnetization characteristics of PMs are captured in the second quadrant of the material-specific hysteresis loops, shown in Figure 2.1. Available magnetic flux density is characterized by residual magnetization ($B_r$), the y-intercept of the hysteresis loop. Higher $B_r$ indicates that higher flux density is usable in the particular material. Meanwhile, x-intercept of the hysteresis loop is referred as coercivity ($H_c$), which characterizes the material susceptibility to demagnetization. The lower the value of $H_c$ is, the easier it is for the material to get demagnetized by the external currents.

The hysteresis loop is able to retrace its own trajectory once the demagnetizing field is applied as long as the operating point stays within the straight linear region. However, once nonlinear bend, also referred as the knee of the magnetization curve, is reached, the loop does not follow its original path but rather begins to trace out a minor loop, characterized by the recoil line. This phenomenon is well-depicted in Figure 2.2. As a result, loss in residual flux density occurs, which means that PM has lost its magnetic strength permanently and irreversibly. Since its ramification would require long repair time and high monetary costs, it is important to make sure that PMs operate within the linear region, above the knee of the hysteresis curve.

Some techniques, known as memory motors, have been proposed to overcome the demagnetization risks in PMSM by allowing easy remagnetization by using armature current pulses [14]. In these variable-flux designs, AlNiCo, a compound of iron, aluminum, nickel, and cobalt, is often used because of its low coercivity and high residual flux density as shown in Figure 2.1. However, it is noted that the limitations exist in the complex control scheme and AlNiCo magnets generally have less energy density than other PM materials.
Figure 2.1: Magnetization curves for common PM materials

Figure 2.2: Depiction of recoil line
such as NdFeB and SmCo, which may decrease the inherent power-dense benefits of PMSM.

2.2 Problem Statement

As indicated previously, a unique Halbach structure has been implemented in the proposed PMSM design for the magnet arrays. Magnets in conventional PMSMs are typically arranged radially inward or outward to attain a reasonable amount of B-field in the air gap. Consequently, a magnetic rotor yoke with a significant thickness should be placed outside the magnet assembly to guide flux. However, with the Halbach array, the magnetic flux can be concentrated on the inner side. This allows a thinner rotor yoke to be used with a slight compensation in terms of torque.

The proposed Halbach structure is discretized in six magnet pieces per pole. Each magnet segment has a designated magnetic orientation as indicated in the cross-sectional diagram of a single pole shown in Figure 2.3 where each segment is numbered from 1 to 6. The specific orientation of each segment is described in Table 2.1. This means that demagnetization should be assessed separately for each segment along the respective directions. For example, if one particular segment is oriented at 90 degrees like segment 1, the magnitude of the B-field along magnetized directions should be examined to determine if any region in the segment has been demagnetized. In other words, a region is considered demagnetized if the directional B-field is lower than the knee point, where its severity is determined by examining how much B is lower than the threshold. On top of demagnetization, another important metric is the machine performance, which is quantified with the output power of the machine in this study. An examination of the demagnetization prevalence/magnitude and the machine performance is expected to provide an interesting trade-off between reducing the demagnetization risk and attaining a higher performance.

Based on this principle, this thesis quantizes demagnetization and performance under various conditions. The first condition is use of different PM material selections, which would develop a range of electromagnetic characteristics of the machine due to the differences in material BH curves. The next is air gap, provided the machine geometry can be accommodated accord-
Figure 2.3: Cross-sectional view of a single pole and illustration of magnet orientations

...ingly. This introduces an electro-thermal trade-off as larger air gap generally exacerbates demagnetization but lowers the machine temperature because of a larger heat path. The third condition is an existence of magnetism in the rotor back yoke.

Table 2.1: The orientations of each magnet segment in Halbach array

<table>
<thead>
<tr>
<th>Segment Number</th>
<th>Angular Direction (deg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>90</td>
</tr>
<tr>
<td>2</td>
<td>120</td>
</tr>
<tr>
<td>3</td>
<td>150</td>
</tr>
<tr>
<td>4</td>
<td>180</td>
</tr>
<tr>
<td>5</td>
<td>210</td>
</tr>
<tr>
<td>6</td>
<td>240</td>
</tr>
</tbody>
</table>

It should be noted that the rms current input (electric loading) to each coil is fixed to 93 A throughout the analyses. Other geometric dimensions, shown in Table 1.1, have been fixed as well with the exception of air-gap analysis. In the air-gap analysis, air gap is changed while keeping the magnet thickness and the rotor outer radius constant, which means the shell thickness was decreased as much as the air gap increased.
2.3 Methodology

Several analytical methods have been proposed to assess the demagnetiza-
tion in PM machines using the consequences of Maxwell’s equation, often
incorporating the lumped parameter magnetic circuit (LPMC) method [15].
Other studies even succeeded to characterize the Halbach-array PM machine
analytically [16]. Although these analytical models have successfully dis-
played some degree of accuracy compared with FE-based results, there are
several limitations associated with this particular demagnetization analysis.
First, the flux path should be clearly defined to characterize the machine
as a magnetic circuit. As a result, the LPMC method is not suitable for
the machines with slotless structure. Second, partial demagnetization within
each magnet segment is difficult to be considered since multiple elements are
lumped into a single circuit parameter. Third, auxiliary effects such as mag-
netic saturation and leakage paths that are not captured in the analytical
methods and over-simplification of the machine geometry may be significant
enough to worsen the accuracy [12]. Due to these limitations, the FE method
is highly favored in demagnetization analyses and thus implemented in this
thesis. All FE analyses have been conducted with Cedrat’s Flux 2D in this
study.

2.4 PM Material and Demagnetization

NdFeB and SmCo are known as energy-dense PM materials which led to
their popularity in high specific power machines. The major difference be-
tween them is thermal performances, which can be seen in Figures 2.4, 2.5,
2.6, and 2.7. To compare the effects of material selections in terms of PM
demagnetization, two magnet grades from NdFeB and SmCo families were
picked respectively. The selected NdFeB grades are N45UH and N38EH while
Recoma 32S and Recoma 35E are chosen from the SmCo magnet grades.

As the residual flux density of NdFeB is generally higher than that of SmCo
at low temperature, it can be easily deduced that the machine performance
will be better with NdFeB as long as thermal loadings are low. However,
$B_r$ drops quicker for NdFeB as the temperature rises. For example, the
crossover point of $B_r$ for N45UH and Recoma 35E is at around 150 °C. Also,
NdFeB becomes more susceptible to demagnetization at higher-temperature conditions. This is already obvious in Figure 2.4 where the knee at 150 °C occurs at 0.3 T while no significant bending is identified in the SmCo grades up to 200 °C. In this study, the knee was assumed to be at 0 T if a bending point could not be seen in the given material sheets provided by the vendor. It should be noted that this is the worst-case scenario for those materials.

FE simulations provide more comprehensive results. The operating temperature was fixed to 150 °C while keeping the air gap at the nominal value shown in Table 1.1. Figure 2.8 displays the minimum directional flux density profile in each magnet segment, which denotes the severity of demagnetization. Minimum B was -0.23 T for N45UH whereas other grades were at > -0.1 T. It is clear that the center magnet blocks (segment 3, 4, and 5) suffer the most severe demagnetization. This is quite understandable as the flux gets augmented in the side blocks (segment 1, 2, and 6) as the magnet orientations are generally aligned with each other as shown in Figure 2.3. Segments 3 and 4, in contrast, do not benefit from other blocks as much as the others, which means the flux in those segments is mostly self-supported.

Prevalence of demagnetization in N45UH was also more significant than that of the other grades. Out of 118 data points per magnet segment, only 1
Figure 2.5: Example NdFeB (N38EH) demagnetization curve [17]

Figure 2.6: Example SmCo (Recoma 32S) demagnetization curve [18]
Figure 2.7: Example SmCo (Recoma 35E) demagnetization curve [18]

Figure 2.8: PM material analysis - minimum flux density in each PM segment
point in segment 4 was identified as demagnetized region in N38EH, Recoma 32S, and Recoma 35E. Meanwhile, it is again shown in Figure 2.9 that the demagnetization is most prevalent in the center blocks, indicating 4% of segment 3, 23% of segment 4, and 1.7% of segment 5 are demagnetized. This area is highlighted in Figure 2.10 where flux vectors are denoted with arrows. It is evident from here that the flux level is significantly lower in the center segments, which lead to their susceptibility against demagnetization. Also, some flux vectors are not even aligned with the original magnetized directions shown in Table 2.1. This signifies that one should be more careful about the center magnets in the Halbach structure in case of demagnetization.

The machine performance in each case is quantized in terms of the output power. Torque is also commonly used as an important performance metric, but it can be interchangeably used with power in this study since the mechanical speed is fixed to 15000 rpm.

\[
Torque = \frac{Power}{Mechanical\ speed} \tag{2.1}
\]

Despite the demagnetization, N45UH displayed the highest output power.
Figure 2.10: Magnetic flux vectors (N45UH, 150 °C, 1.14 mm air gap; demagnetized area highlighted with a box)

among four magnet grades, as shown in Table 2.2. N45UH is closely followed by Recoma 35E, then Recoma 32S, and N38EH. This order is exactly the same as that of the highest residual flux density of each material at 150 °C. Although some magnetic power is lost due to demagnetization, it can be deduced that residual flux density is still a more dominant factor in terms of the output power at 150 °C.

Table 2.2: The machine performance for each magnet material at 150 °C

<table>
<thead>
<tr>
<th>PM Material</th>
<th>Output Power (MW)</th>
<th>$B_{r,150°C}$ (T)</th>
</tr>
</thead>
<tbody>
<tr>
<td>N45UH</td>
<td>0.961</td>
<td>1.139</td>
</tr>
<tr>
<td>N38EH</td>
<td>0.885</td>
<td>1.042</td>
</tr>
<tr>
<td>Recoma 32S</td>
<td>0.920</td>
<td>1.098</td>
</tr>
<tr>
<td>Recoma 35E</td>
<td>0.950</td>
<td>1.136</td>
</tr>
</tbody>
</table>

The effect of the operating temperature on demagnetization is obvious as all magnet grades including N45UH show a negligible amount of demagnetization at 120 °C (1 point in segment 4). As it can be expected, N45UH outperforms the other materials at 120 °C, which is shown in Table 2.3. It is also noted all output powers in Table 2.3 are higher than the 150 °C
case, majorly due to higher residual flux densities. This concludes that the cooling scheme is highly important to maintain both a demagnetization-free environment and high output power in the machine.

Table 2.3: The machine performance for each magnet material at 120 °C

<table>
<thead>
<tr>
<th>PM Material</th>
<th>Output Power (MW)</th>
<th>$B_{r,120^\circ C}$ (T)</th>
</tr>
</thead>
<tbody>
<tr>
<td>N45UH</td>
<td>0.999</td>
<td>1.188</td>
</tr>
<tr>
<td>N38EH</td>
<td>0.920</td>
<td>1.087</td>
</tr>
<tr>
<td>Recoma 32S</td>
<td>0.930</td>
<td>1.110</td>
</tr>
<tr>
<td>Recoma 35E</td>
<td>0.960</td>
<td>1.148</td>
</tr>
</tbody>
</table>

2.5 Air Gap and Demagnetization

Although the situation is more complicated in practical applications, a general correlation between air gap and flux density in the magnets can be illustrated with a simple 1-D example shown in Figure 2.11. This problem is formulated with an iron piece with a small air gap and a magnet (colored in dark gray), but without any current input. Assuming the permeability of iron is infinite, the operating point of the magnet can be easily determined by observing where the intrinsic material curve of the magnet and the load line of the magnetic circuit coincides. In this setup, the load line can be obtained as a linear line derived from the geometry, and always crosses the origin in the B-H plot due to the absence of an mmf ($N_i$) source. This can be shown with the usage of Ampere’s law under the given conditions, which dictates that

$$H_m L_m + H_g L_g = 0 \tag{2.2}$$

where $H_m$ and $H_g$ are the magnetic field strength in the magnet and air gap, while $L_m$ and $L_g$ are the magnet and air-gap length, respectively. As the flux $\phi$ is continuous in the magnetic circuit, it is also true that

$$\phi = B_g A_g = A_m B_m \tag{2.3}$$

or

$$B_g = \frac{A_m}{A_g} \tag{2.4}$$
Figure 2.11: 1-D example to illustrate the correlation between air gap and flux density in magnet
where \( B_m \) and \( B_g \) are the B-fields in the magnet and air gap, while \( A_m \) and \( A_g \) are the cross-sectional area of the magnet and air gap, respectively. Also, in the air gap, the B-H relationship is

\[
B_g = \mu_0 H_g
\]  

(2.5)

where \( \mu_0 \) is the free-space permeability. Therefore, Eqs. 2.2, 2.4, and 2.5 can be solved to obtain the B-H characteristic in the magnet (\( B_m \) and \( H_m \)).

\[
B_m = -\mu_0 \frac{A_g l_m}{A_m g} H_m
\]  

(2.6)

If the cross-sectional area is constant throughout the path, Eq. 2.6 reduces to

\[
B_m = -\mu_0 \frac{l_m}{g} H_m
\]  

(2.7)

Equation 2.7 indicates that the air gap is inversely proportional to the slope of the load line. This means the operating point of a magnet will be lowered as the air gap increases assuming the temperature remains constant. If this point is lowered significantly enough that the operation resides below the knee of the material curve, the magnet would suffer from demagnetization. This is visually illustrated in Figure 2.12 where how the operating points change as the air gap increases is shown.

However, it should be noted that the 1-D example does not reflect the real PMSM applications comprehensively. In PMSM, there are always the stator windings with current input (therefore mmf) and more importantly, it is hard to define a magnetic circuit in a slotless design in the first place. Nevertheless, the example shown in Figure 2.11 still serves its purpose to illustrate a general relationship between the air-gap length and the load line. Figure 2.12 also indicates the air-gap flux density would decrease as well, which usually leads to a reduction in the performance in electric machines. Therefore, it can be concluded that an increase in air gap causes higher demagnetization risk and lower electromagnetic performance provided there is no change in operating environment.

The results from FE analyses show a similar trend as expected from the 1-D example. These analyses have been conducted with N45UH magnets op-
Figure 2.12: Example BH curve and load lines of the 1-D magnetic circuit

Figure 2.13: Air-gap analysis - minimum flux density in each PM segment
Figure 2.14: Air-gap analysis - percentage of demagnetized area in each PM segment

Figure 2.15: Air-gap analysis - output power vs. air gap plot at 150 °C
erating at 150 °C to present more drastic change in demagnetization magnitude and pervasiveness versus air-gap length in the proposed machine design. First, Figure 2.13 shows that the demagnetization is the most intense in the segment 4 in all cases and it gets worse as the air gap increases. Similarly, more elements are found to be demagnetized with an increase of the air gap as shown in Figure 2.14. Larger air gaps also made the machine performance to suffer as well, like illustrated in Figure 2.15.

Figure 2.16: Air-gap analysis - output power vs. air gap at 150 °C and 120 °C

The possible benefits of having a larger air gap in PMSM are related to thermal aspects. The larger the air gap is, the more heat path that the design can have. Larger air gap thus has a potential to decrease the operating temperature, which will present an interesting trade study between electromagnetic and thermal design constraints. Unfortunately, this is not discussed in this thesis in detail as it is not within the scope of this study. Nevertheless, an optimization scheme that considers both electromagnetic performance and thermal loadings has already been presented in [8]. If larger air gaps are actually able to significantly decrease the temperature, however, it will be greatly beneficial to the demagnetization and machine per-
formance issues. For instance, demagnetization is negligible at the operating temperature of 120 °C in all cases. The output power is also higher at lower temperature, which is shown in Figure 2.16. This suggests that the cooling scheme and heat path are closely related with demagnetization and electromagnetic torque output in PMSMs mostly because PMs are highly sensitive to temperature.

2.6 Rotor Yoke Material and Demagnetization

The baseline design has utilized titanium, which is a non-magnetic material, directly behind the PM array to maintain mechanical integrity. Absence of magnetism in the rotor yoke is not a serious problem as the Halbach structure has been implemented in PMs. However, magnetic material can still be used to further increase the performance. One possible candidate is AerMet. An optimization study has been conducted in [19], where a trade-study of the rotor yoke materials has been explored in terms of the torque density of the proposed machine design. In this study, it has been identified that even though the output torque is 5% higher if AerMet is used, the specific torque was higher for the titanium case as the weight density of titanium is 4.43 g/cm$^3$ while that of AerMet is 7.72 g/cm$^3$.

However, Lee et al. [19] have only discussed the machine performance, not the effect of different rotor yoke material selection on demagnetization. To observe whether an existence of magnetism in the rotor back yoke has any influence on PM demagnetization, another round of FE analyses has been conducted. As in the air-gap analysis, PM material was set to N45UH grade operating at 150 °C. The air gap was also fixed to the nominal value shown in Table 1.1.

The results shown in Figures 2.17 and 2.18 display an interesting phenomenon. While the intensity of demagnetization is greater with the usage of AerMet, the area of demagnetized PMs become larger with titanium. By creating a new magnetic path in the rotor yoke (i.e. using AerMet), certain areas become more demagnetized, but the other areas get more aligned flux along the original magnetized direction. This indicates that another consideration should be involved when choosing a material for the rotor yoke in addition to the maximum output and specific torque.
Figure 2.17: Rotor back yoke analysis - minimum flux density in each PM segment

Figure 2.18: Rotor back yoke analysis - percentage of demagnetized area in each PM segment
2.7 Conclusion

In this chapter, demagnetization characteristics under various conditions have been explored. As indicated in the material datasheets, SmCo magnets have more resilience against demagnetization than the NdFeB grades. N45UH was the most vulnerable although it had the highest performance while N38EH did not experience much demagnetization but had the lowest performance at 150 °C. This result suggests that SmCo is a better choice to achieve a balance between having demagnetization-free PMs and achieving a high performance.

Meanwhile, it was found that larger air gaps led to an increase in demagnetization risks and a decrease in the machine performance. While this may seem only detrimental, having a larger air gap has a potential to decrease the operating temperature since there would be more heat flow. Future work may involve a quantization of thermal benefits from having a larger air gap which will eventually lead to an optimization of temperature and electromagnetic quantities.

The rotor yoke material had some influence to PM demagnetization as well, where the titanium yoke caused a larger area of PMs to be demagnetized while AerMet induced less prevalent demagnetization but with higher intensity. An extension of [19] would thus be interesting, adding some considerations about demagnetization.
CHAPTER 3

INSULATION AGING MODELS

3.1 Motivation

Breakdown of the stator winding insulation system is known to be one of the main causes of ac machine failures [20]. Specifically, it has been reported that the stator insulation failure consists of 40% of total failures in a drive [21]. It is evident that the risk becomes more serious as the machine voltage gets higher due to an increase in dielectric stresses. Moreover, failures in solid insulators, which are typically used in stator windings, mostly lead to non-reversible destructive damages. Special attention should thus be paid to assess, model, and predict the aging behavior of the stator winding insulation.

Generally, the aging factors can be classified in four categories [22].

1 Thermal stress: High temperature operation causes the insulation degradation.

2 Electrical stress: The aging is induced by high voltage gradients. Partial discharges or transition state theory can explain some mechanisms of electrical aging applied to the insulation system. This will be discussed in the following sections in more details.

3 Mechanical stress: The aging is caused by vibration, thermal expansion, and electric compressive force.

4 Environmental stress: Ambient conditions provides various impacts on the aging. These include moisture, ambient pressure, radiation, and oxidation.

Among these factors, the items that are relevant to the proposed machine design can be sorted as the following:
1 Thermal stress: High temperature is definitely a concern due to significant conduction losses caused by high current input (92 A per coil) in the stator windings. There are also other losses such as windage losses. Detailed thermal analysis is presented in [8]. The Arrhenius model will be introduced in the later sections to characterize thermal aging.

2 Electrical stress: This is the main focus of this study. Since the nominal line-to-neutral voltage in the proposed design is 486 V (rms), it needs to be checked whether major electrical stress is present at this voltage level. Experimental examination of partial discharge inception voltage (PDIV), which is the voltage level where PDs start to occur, is thus conducted. Aging models and preliminary results from the PDIV experimental setup will also be introduced accordingly.

3 Mechanical stress: The aging due to mechanical stress is probably present in the proposed machine design due to its aerospace operation with high rotational speed (15000 rpm). However, the mechanical aspects are not discussed in depth in this thesis as they are not within the scope of the study.

4 Environmental stress: Moisture and oxidation may be related with the aging. But radiation is not related; this would be more relevant to the cables in nuclear power plants. A low pressure environment will obviously be present due to the high-altitude (aerospace) operation. This is critical as the ambient pressure is closely related with the breakdown voltage and possibly PDIV; their relationship is widely known as Paschen's law.

One major difficulty in the insulation lifetime test is that typical insulations are designed to last for years. As it would be impractical to conduct an experiment for an extended period of time, an accelerated test is necessary to facilitate the aging in a lab environment. In an accelerated aging test, one or more aging factors are applied to a device under test (DUT), which is the stator windings in this case, with higher intensity than it would experience during the nominal operation. This indicates that appropriate models should be prepared to interpret acquired datasets and extrapolate those to deduce a prediction of actual insulation lifetime.

For this purpose, this thesis first introduces some popular aging models developed by many authors before presenting preliminary results from the
aging setups. These include not only the single-stress models (thermal or electric), but also the multi-stress models that consider both thermal and electric stresses. The discussion of multi-stress models are highly important as the results with simultaneous stresses are different from those with sequential or separate stresses [22].

### 3.2 Arrhenius Aging Model

It is generally accepted that the rate of chemical reactions can be explained in terms of temperature. This is known as the Arrhenius equation [23] which is expressed as

\[
R = Ce^{-\frac{E_a}{k_B T}} \tag{3.1}
\]

where \( R \) is the aging rate, \( C \) is the frequency factor, \( k_B \) is the Boltzmann constant, \( E_a \) is the activation energy, and \( T \) is the temperature. Equation 3.1 presents an obvious correlation between the lifetime and temperature; the higher the temperature is, the greater the rate constant is.

If experimental datasets from accelerated aging tests are available, they can be fitted into Eq. 3.1. Since insulation lifetime is the quantity that is more directly related with the mission objective, Eq. 3.1 can be rewritten using the fact that lifetime is inversely proportional to the aging rate. This is shown as

\[
L = Ae^B T \tag{3.2}
\]

where \( L \) is the lifetime, while \( A \) and \( B \) are the constants that can be determined by conducting data-fitting. It is noted that a straight line would result if the natural log of \( L \) is plotted versus \( T \).

Now, the acceleration factor can be derived from Eq. 3.2 to compare the expected lifetimes of DUT under the reference and accelerated aging conditions as

\[
AF = \frac{L_{\text{accelerated}}}{L_{\text{ref}}} = e^{B\left(\frac{1}{T_{\text{accelerated}}} - \frac{1}{T_{\text{ref}}}\right)} \tag{3.3}
\]

where \( AF \) is defined as the acceleration factor. The subscripts indicate the quantities under the reference and accelerated aging conditions, respectively. Here, the reference may mean the room temperature or the thermal condition during nominal operation, depending on the choice of definition. This
equation can be further simplified if a concept of so-called thermal stress \((\Delta T = 1/T_{ref} - 1/T_{accelerated})\) is introduced. Then,

\[
AF = e^{-B\Delta T}
\]

\[
L_{accelerated} = L_{ref} e^{-B\Delta T}
\]  

(3.4)

Equation 3.4 is often more useful than Eq. 3.3 because \(\Delta T\) has a clearer boundary, from 0 to \(1/T_{ref}\), than \(T_{accelerated}\). Although the activation energy of each material is different, Eqs. 3.1 - 3.4 explain a widely known simple and generalized rule of thumb, which states that many chemical reactions become two times faster per 10 °C increase in temperature.

As indicated in [7], a triple-layer class H (rated at 180 °C) insulation structure has been implemented for the stator windings in the proposed machine design. The target insulation lifetime is set to 20000 hours which is typical specification of a commercial grade machine with a continuous usage at full load [24]. Therefore, thermal aging experiments at an elevated temperature should be conducted to confirm this goal, where the usage of Arrhenius model will assist to predict the lifetime.

### 3.3 Inverse Power Law Aging Model

The first model that correlates the insulation lifetime and the applied electric field is the inverse power law. In this model, the relationship is described with an inverse power function, as

\[
L = cE^{-n}
\]

\[
\log(L) = \log(c) - n\log(E)
\]  

(3.5)

where \(E\) is the applied electric stress (kV/mm), and \(c, n\) are the constants to be determined from experimental data fitting. The assumption of using the inverse power law model is validated if a straight line results in a \(\log(L)\) vs. \(\log(E)\) plot. The frequency dependency can also be added to Eq. 3.5 when an ac source is applied, as

\[
L = cE^{-n}f^{-l}
\]  

(3.6)
where $f$ is the frequency of the source and $l$ is an additional constant to be determined from the frequency-dependent data. The inverse power law is widely used for its simplicity as seen in Eqs. 3.5 and 3.6, and it is acceptably accurate in many applications.

3.4 Exponential Aging Model

As the name suggests, the exponential model describes a relation between the insulation lifetime and the electric field with an exponential function.

$$L = ce^{-nE}$$

$$ln(L) = ln(c) - nE$$

(3.7)

Exponential model also has a simple form and linear dependency between $ln(L)$ and $E$. The exponential model is therefore considered to be valid if a straight line results from a $ln(L)$ vs. $E$ plot. Moreover, the frequency component can be added to the right-hand side of Eq. 3.7 as in Eq. 3.6.

However, in many cases, the data do not fit well to both the inverse power law and exponential models at low $E$ [22]. A concept of threshold electrical stress should thus be introduced to resolve this issue. As a result, modified inverse power law model becomes

$$L = L_0 \left(\frac{E}{E_0}\right)^{-n}$$

(3.8)

where $E_0$ is the threshold electrical stress value and $L_0$ is the lifetime at $E_0$. Meanwhile, modified exponential model is expressed as

$$L = \frac{c_2}{E - E_0} e^{c_1(E - E_0)}$$

(3.9)

where $c_1$, $c_2$ are the constants to be determined from the modified model. The derivation of Eq. 3.9 is shown by Dakin and Studniarz in [25]. They state that the cause of insulation deterioration is primarily the effect of PDs, which are small electric discharges that result from electrical breakdowns of air voids within the insulation. As a result, the insulation layer gets constantly damaged under the condition of PD occurrence, eventually causing total
electrical breakdown. Their aging equation is given as

\[ L = \frac{M_{cr}}{Af(E - E_0)} e^{\frac{E_a - b(E - E_0)}{k_B T}} \]  

(3.10)

where \( M_{cr} \) is the critical amount of insulation decomposition at the failure time \( L \) and \( b \) is a constant to be determined. While the definitions of other variables are already given, it should be noted that \( E_0 \) in Eq. 3.10 is interpreted as the partial discharge onset stress (analogous to PDIV) since PDs are assumed as the primary aging mechanism, as stated in [25]. This is not necessarily true with the other models [26], because PDs are considered as a short-term phenomenon whereas the threshold electric stress is a long-term aging quantity.

Similarity between Eqs. 3.9 and 3.10 becomes more explicit if log-log values are taken. For instance, Eq. 3.9 can be rearranged as

\[ \ln(L(E - E_0)) = c'_2 - c_1(E - E_0) \]  

(3.11)

where \( c'_2 = \ln(c_2) \). Now, Eq. 3.10 can be expressed as

\[ \ln(L(E - E_0)) = \ln\left(\frac{M_{cr}}{Af}\right) + \frac{E_a}{k_B T} - \frac{b}{k_B T}(E - E_0) \]  

(3.12)

These two equations have an obvious relationship that \( c'_2 = \ln(M_{cr}) + \frac{E_a}{k_B T} \) and \( c_1 = \frac{b}{k_B T} \).

The updated models are more accurate at low \( E \) than the original ones with the introduction of \( E_0 \). However, one more point has to be considered. Both models indicate that the lifetime becomes infinite when \( E \) approaches zero in the inverse power law model and \( E_0 \) in the exponential model, respectively. This is often inconsistent with experimental results, suggesting that the models are only valid in certain regions. Equation 3.8 indicates the inverse power law model is valid when \( E \geq E_0 \) while Eq. 3.9 shows the exponential model is valid when \( E > E_0 \). At lower points (\( E < E_0 \)), two models assume that thermal stress is the main cause of the aging and insulation failure.
3.5 Multi-Stress Aging Models

In real applications, the aging stresses mostly stem from multiple stress sources. A combination of electrical and thermal stresses is the most common and almost inevitable to occur, thus suggesting a need for an electro-thermal aging test setup and models to accurately simulate the insulation aging in a lab environment. This is particularly necessary, as it has been already mentioned, because the results from multi-stress and sequential aging tests are considerably different.

Simoni developed multi-stress models based on the Arrhenius and electrical stress models [26]. The model equation is given as

\[ L = k_s L_c \frac{e^{-B \Delta T - nE + b E \Delta T}}{G/G_{rt} + \Delta T/\Delta T_{rt} - 1} \]  

(3.13)

where \( k_s \) is a constant which depends on the short-time characteristics of insulation, \( L_c \) is also a constant under combined stresses, \( G_{rt} \) is the threshold electrical stress at room temperature, and \( \Delta T_{rt} \) is \( \Delta T \) for \( T = T_{rt} \) where \( T_{rt} \) is defined as the threshold temperature without electrical stress. Here, it is noted that the idea of the threshold temperature is introduced as well. The \( k_s \) can be obtained with

\[ k_s = \frac{E_B}{E_{rt}} - 1 = \frac{\Delta T_i}{\Delta T_{rt}} - 1 \]  

(3.14)

where \( E_B \) is the breakdown electrical stress in short-term tests and \( T_i = 1/T_0 \).

Simoni’s model can be summarized as a relationship between three variables; lifetime, electrical, and thermal stress. This can obviously be expressed as an \( x-y-z \) function, where \( x = \Delta T \), \( y = E \) or \( \ln(E/E_{ref}) \), and \( z = \ln(L) \). The relation between two variables can be obtained if the other variable remains constant. For instance, if \( x \) is held constant, the function becomes the relationship between \( E \) and \( L \) at certain temperature. Other relations can be obtained in a similar fashion. If this \( x-y-z \) equation is plotted in a 3-D plane, a surface that describes a relationship between the lifetime and multi-stresses can be clearly visualized. This surface is referred as the life surface in Simoni’s work [26].

Meanwhile, Ramu [27] also developed a multi-stress model that is based
on physical and chemical rate process. This is expressed as

\[ L = K(T)E^{-n(T)}e^{-B\Delta T} \]

where \( E > E_0 \) (3.15)

where \( K(T) = e^{K_1 - K_2\Delta T} \), \( n(T) = n_1 - n_2\Delta T \), and \( K_1, K_2, n_1, n_2 \) are empirical constants. It is noted that Ramu’s model considers the threshold electrical stress in the condition, under which electrical and thermal aging is negligible.

### 3.6 Crine’s Aging Model

Even though Crine’s model is one of the multi-stress models, it was deemed to be valuable enough to be introduced in a separate section as it explicitly considers a different aging mechanism that is not PD. This model incorporates the transition state model to explain the aging.

According to the transition state model, the probability \( (p) \) of transition between the original and the final states of activation process is expressed as

\[ p \approx \frac{k_B T}{h} e^{-\frac{\Delta G_0}{k_B T}} \] (3.16)

where \( h \) is the Planck constant and \( \Delta G_0 \) is the Gibbs activation energy. It is noted that the probability of transition from the final and original states is same as \( p \) in Eq. 3.16 when no electric field is applied. Therefore, the ratio between disrupted and recombined bonds is maintained. With the electric field, however, this balance gets broken as the energy barrier of transition states changes. This is illustrated in Figure 3.1 where the solid plot represents Gibbs free energy \( G \) when there is no stress and the dashed plot indicates \( G \) when some stress \( E \) has been applied. As it can be seen, some \( E \) elevates the potential of the original states while decreasing that of the final states. The ratio consequently changes, which results in an increase in the disrupted bonds.

The bonds that are affected by this phenomenon are mostly van der Waals bonds since they are weaker intermolecular bonds \( (\approx 0.4 \text{ eV}) \) than intramolecular bonds \( (> 4 \text{ eV}) \). The disruptions of these bonds leave some free space in a molecular scale in which free electrons can accelerate. This forms a positive loop where the accelerated electrons break more bonds and consequently
Figure 3.1: Free energy barrier under no stress and some electric stress form a larger free volume ("submicrocavities") [28].

As it has been implied, $p$ in Eq. 3.16 changes under the influence of some electric field. It becomes easier for the bonds to get disrupted than to be recombined, so the probability of disruption becomes

$$p^+ = \frac{k_B T}{h} e^{\frac{\Delta G_0 - e \lambda E}{k_B T}}$$

(3.17)

where $\lambda$ is the submicrocavity size. Meanwhile, the probability of recombination is

$$p^- = \frac{k_B T}{h} e^{\frac{\Delta G_0 + e \lambda E}{k_B T}}$$

(3.18)

The net probability of disruption is therefore

$$p_{net} = p^+ - p^- = \frac{2k_B T}{h} e^{\frac{\Delta G_0}{k_B T}} \sinh\left(\frac{e \lambda E}{k_B T}\right)$$

(3.19)

The time to reach the bond breaking is the reciprocal of Eq. 3.19 that is

$$L \approx \frac{h}{2k_B T} e^{\frac{\Delta G_0 - e \lambda E}{k_B T}} \csch\left(\frac{e \lambda E}{k_B T}\right)$$

(3.20)

At high $E$, Eq. 3.20 can be simplified as

$$L \approx \frac{h}{2k_B T} e^{\frac{\Delta G_0 - e \lambda E}{k_B T}}$$

(3.21)
The transition between the non-exponential lifetime in Eq. 3.20 and the exponential lifetime in Eq. 3.21 occurs at the critical field $E_c$. The maximum scattering length $\lambda_{\text{max}}$ can be obtained from the exponential regime because the slope of $\ln(L)$ vs. $E$ is linear. Also, it should be noted that the scattering length decays rapidly under $E_c$ [28], which means the aging under this point is insignificant.

Crine’s model has been reworked in a more recent paper to account for Maxwell stress [29] as

$$L = \frac{h}{2k_BT} e^{\frac{\Delta\epsilon_0}{\epsilon_B \epsilon'}} \text{csch} \left( \frac{1}{2} \frac{\epsilon_0 \epsilon' \Delta VE^2}{k_BT} \right)$$

(3.22)

where $\epsilon_0$ and $\epsilon'$ are the free-space and the dielectric constant of the polymer insulation, respectively. Equation 3.22 is valid for dc stresses, but the usefulness of this model still extends for ac stresses as the equation can be modified to consider the ac frequency.

$$L = \frac{h}{2f k_BT} e^{\frac{\Delta\epsilon_0}{\epsilon_B \epsilon'}} \text{csch} \left( \frac{1}{2} \frac{\epsilon_0 \epsilon' \Delta VE^2}{k_BT} \right)$$

(3.23)

It has been reported that Crine’s models fit better to experimental data than the preexisting aging models [30].

### 3.7 Conclusion

Some statistical methods can be introduced due to the probabilistic nature of accelerated aging data. Weibull and log-normal distribution are two methods that are often used [22]. The two-parameter Weibull distribution is shown as

$$F(x) = 1 - e^{-\left(\frac{x}{\alpha}\right)^\beta}$$

where $x > 0$

(3.24)

where $F(x)$ is the failure probability, $\alpha$ is the time constant, $\beta$ is the shape parameter, and $x$ is the variable - time or voltage. Meanwhile, the log-normal distribution is

$$f(z) = \frac{1}{\sigma \sqrt{2\pi} z} e^{-\frac{(\ln z - \mu)^2}{2\sigma^2}}$$

(3.25)

33
where \( z = \log(x) \), \( x \) is the breakdown voltage or lifetime, \( \mu \) is the logarithmic mean, and \( \sigma \) is the logarithmic standard deviation. Montanari and Cacciari in [31] utilized the Weibull distribution to explain the behavior of insulation materials subjected to electro-thermal stresses. Along with the aforementioned aging models, statistical techniques can be utilized to further characterize the life models in a probabilistic manner.

In conclusion, many researchers have developed thermal, electrical, and multi-stress aging models. These are considerably mature as well, since most of them were proposed in the 1970s to 1990s. However, the simplicity and accuracy of each model vary since they are often based on different assumptions. Therefore, the aging model should be carefully selected after checking the assumptions and observing which assumption fits best with the particular insulation design. This discussion includes the presence of PDs in the system and whether the operating voltage or temperature resides under or over the threshold points. The ultimate goal would be to successfully extrapolate the data at the elevated to nominal operating conditions and provide a lifetime projection for the insulation system to be used in the proposed machine.
4.1 Accelerated Aging Setup with Impulse Generation

There are several ways to apply electric stresses in an accelerated aging setup. Voltage waveforms can either be square waves, periodic impulses, or ac waves. The choice can affect the aging considerably, as the electrical stresses are closely related with the slew rate; steeper voltage gradients induce higher stress.

To simulate the worst-case scenario, an accelerated aging setup based on periodic impulses is first proposed. This setup is comprised of an impulse generator circuit, a signal generator, a high voltage dc power supply, a logic power supply, a cooler, an oven, and a measurement unit. As shown in Figure 4.1, the impulse generator circuit receives logic signal, high dc voltage, and 12 V logic power from various supplies. The cooler is added to circumvent the thermal limitations (150 °C) of high-power MOSFET implemented in the impulse generator circuit. DUT, which is an in-house manufactured stator armature winding, is located inside the oven to thermally accelerate the aging process. Oscilloscope coupled with high voltage/current probes is connected to DUT to enable real-time voltage/current monitoring during the test and data acquisition. Physical implementation of the setup is shown in Figure 4.2.

The impulse generator circuit must be able to supply target voltage 486 V, which is the expected line to neutral peak voltage in the machine. The circuit incorporated an off-the-shelf evaluation board manufactured by CREE (KIT8020CRD8FF1217P-1). A SiC MOSFET (C2M0025120D) produced by the same supplier was mounted on the board due to its high voltage/current ratings and compatibility. Flyback diode and resistor bank are connected in parallel to avoid sudden voltage spike across DUT, which is an inductive load.
Figure 4.1: Impulse based accelerated aging test setup block diagram

Figure 4.2: Physical implementation of impulse based accelerated aging test
This electric setup is well-described in Figure 4.3, which is equivalent to an LTSpice model created for simulation purposes. Wires and connections were modeled as parasitic RLs (approx. 10 mΩ and 1 µH, measured separately) in the simulations. These were important factors as the DUT resistance and inductance are very low (≈ 15 mΩ and 8 µH).

A voltage waveform from LTSpice simulations is shown in Figure 4.4. It can be seen that the target DUT voltage is achieved, marking a peak voltage that is higher than 500 V. Ringings are obvious as they are caused by the parasitic RLs which created an LC system in the test setup. A current waveform is presented in Figure 4.5, showing a peak current of 13 - 14 A. While this may be concerning, it is noted that the pulse width is only about 8 µs out of 50 µs period.

Since the voltage is applied across the DUT, it is expected that this aging
Figure 4.4: LTSpice voltage waveform across DUT

Figure 4.5: LTSpice current waveform through DUT
setup will stress the turn-to-turn insulation.

4.2 Preliminary Impulse Aging Setup Results

With an established physical setup, actual voltage and current waveforms were collected to validate the impulse generator design. These are shown in Figures 4.6 and 4.7. When compared with Figures 4.4 and 4.5, the measured waveforms display similar levels and patterns. Therefore, it can be concluded that the impulse generator has been successfully established.

DUT will be removed from the experimental setup at each time interval where dissipation factor, ground wall resistance, and partial discharge inception voltage (PDIV), which is the voltage where PDs start to appear, will be measured to quantify the degradation. In order to do so, a high-precision LCR meter, Megger test equipment, and the PDIV setup that will be introduced in the Section 4.3 will be utilized. Figure 4.8 shows the dissipation factor measurement setup using HP 4284A Precision LCR Meter. Iron plates
are used to represent the stator and rotor core. The four-wire measurement method will be used to accurately measure the inductance and dissipation factor of the coil.

4.3 PDIV Diagnostics Setup

PDs are widely known to cause a progressive deterioration of electrical insulation system, ultimately leading to electrical breakdowns. The machine is expected to have a high rms line voltage of 594 V, so each coil will experience 485 V peak line-neutral voltage at nominal conditions.

PD and PDIV are particularly concerning in the proposed machine design because of the aforementioned aerospace operating conditions. For example, air pressure is approximately 144.4 torr at a cruising condition of 11.9 km [32]. The relationship between air pressure and PDIV can be correlated to Pachen’s law, which is a famous characterization of breakdown voltage in a function of gas pressure and gap length. This is understandable because the
PDs are electric discharges in air voids within the insulation system. Some researchers have identified a good agreement between PDIV and the Paschen curve at mid-range air pressure (> 50 torr) [33]. That is, PDIV at 144.4 torr may be lower and that at 1 atm (760 torr), meaning the system becomes more susceptible to PDs at the actual operating condition.

With this consideration, a PDIV diagnostics setup has been designed by a colleague, Thanatheepan Balachandran. The setup diagram is illustrated in Figure 4.9, where the actual implementation is shown in Figure 4.10. The main excitation originates from a 60 Hz wall outlet, which is then controlled by a variac. This primary voltage is stepped up with a 1:40 potential transformer (PT) so that the gap between DUT and an iron base plate can experience high voltage, meaning ground-wall insulation is tested here, unlike the aforementioned impulse aging test setup. Both primary and secondary voltages are monitored with a digital multimeter and an oscilloscope. PDs are detected with a current transformer (CT) connected to the oscilloscope via a high-pass filter (HPF). Any spikes shown in the oscilloscope are expected to indicate the occurrence of PDs in DUT. A vacuum chamber is installed
to test PDIV under low-pressure environment, as mentioned previously.

Coil samples have been prepared by pasting a winding to an arc-shaped iron plate with epoxy resin. The iron plate’s radius is the same as that of the actual stator yoke; epoxy resin is Duralco 128, which is also the same material to be used in the actual machine. Adding a resin layer between DUT and the base plate is necessary for structural issues since the windings need to be held together on the yoke. However, it is also expected that the resin will reduce PD intensity as well, as epoxy would provide more discharge resistance than air. A photo of one sample is shown in Figure 4.11.
Figure 4.11: Coil sample for PDIV testing
4.4 PDIV Results

Before subjecting DUT to a PDIV test, the PD-freeness of the setup without DUT has been confirmed by conducting an open circuit PDIV test. No significant peak was present until 4.2 kV (rms) at this condition, which is well above the nominal voltage. Whenever there is any change in the setup, similar procedure should be followed to confirm the open circuit PD-freeness.

Preliminary experiments have been conducted with a coil sample, where PDIV at 1 atm was around 1 - 1.1 kV (rms). This result is shown in Figure 4.12, contrasted with Figure 4.13 which is the open circuit test result. Here, spikes from CT are obvious with the secondary rms voltage at 1.05 - 1.08 kV while PD-freeness of the setup is clear as no spikes was seen up to 4.16 kV (rms).
This result is optimistic since 1 kV (rms) is still well above the nominal rms line-neutral voltage (343 V). Nevertheless, additional tests would have to be conducted with more coils to ensure the result.

4.5 Future Work

Although the machine will be driven by inverters, pulse width modulated (PWM) signals are expected to be mostly filtered. Since this means the stator windings are likely to experience sinusoidal ac voltage stresses, a new aging setup would be needed to simulate a more realistic situation. This setup will be very similar to the PDIV setup that has already been introduced, except the 60 Hz ac wall outlet power source will be replaced with a high-frequency (2.5 kHz) voltage supply. The potential transformer may have to be replaced as well, to ensure its capability at the high frequency. After these aging experiments are complete, the aging data should be fitted into the most appropriate model among those introduced in the Chapter 3.

Influence of other environmental factors on PDIV would be another interesting topic to explore. As Moonesan et al. indicated in [34], humidity may play an important role in PDIV. An environment chamber can be implemented to test the insulation aging under high-humidity condition. The temperature dependency of PDIV will have to be investigated, since the winding temperature is expected to increase up to 180 °C. In short, more data points at various pressure levels, humidity, and temperature should be attained to deduce PDIV at various operating conditions.
CHAPTER 5
CONCLUSION

The motivation of this thesis was to investigate and confirm electromagnetic integrity of the 1 MW PMSM design presented in [5]. Two major topics were covered, PM demagnetization and long-term insulation quality. This was necessary as the machine is targeted to achieve a high power density of 13.3 kW/kg, which is around 2-4 times higher than the state-of-the-art designs, by pushing the design parameters such as mechanical speed, current, and consequently temperature.

PM demagnetization under different conditions have been explored which allowed the observation of the effects of PM material, air gap, and rotor yoke material on demagnetization and performance. As a result, SmCo PM grade was found to be better in terms of demagnetization resistance, while attaining a reasonable amount of output power. Air gap showed detrimental effects on demagnetization as it became larger, but large gaps do have a potential to decrease the operating temperature, which is certainly a topic to explore in the future. The existence of magnetism in the rotor back yoke surely affected demagnetization profile in PMs as well.

Insulation aging was covered in this thesis to verify the longevity of the insulation system presented in [7]. Various aging models were introduced to explore options and several experimental setups with preliminary results have been presented. There is much work to do regarding this subject, as sufficient amounts of data have not been recorded yet. Some modifications to the setups may be necessary as well, to include the effects of ambient air pressure, humidity, and temperature on PDIV or aging. After enough data are taken, the aging models are expected to provide an extrapolation of lifetime, presenting a projected lifetime of the insulation system.

Comprehensive analyses presented in this thesis have provided suggestions on PM design and long-term insulation quality assessment. The ultimate goal is to realize the design, which is already in progress. Although it would
be interesting to observe PM demagnetization and insulation lifetime in real applications, these may not be possible due to practical limitations such as space constraint and time. Nevertheless, it is expected that these risks can be retired with thorough analyses as presented in this study.
REFERENCES


