Experimental Characterization of an Integrated Starter/Generator

by

David D. Wentzloff

B.S., Electrical Engineering (1999)
University of Michigan, Ann Arbor

Submitted to the Department of Electrical Engineering and Computer Science
in partial fulfillment of the requirements for the degree of

Master of Science in Electrical Engineering

at the

MASSACHUSETTS INSTITUTE OF TECHNOLOGY

August 2002

© 2002 Massachusetts Institute of Technology. All rights reserved

Signature of Author .................................................................

Department of Electrical Engineering
June 2002

Certified by .................................................................

Jeffrey H. Lang
Professor of Electrical Engineering, Associate Director of LEES
Thesis Supervisor

Certified by ............................................................................

Thomas Keim
Assistant Director of LEES
Thesis Supervisor

Accepted by .................................................................

Arthur C. Smith
Chairman, Department Committee on Graduate Students
Experimental Characterization of an Integrated Starter/Generator

by

David D. Wentzloff

Submitted to the Department of Electrical Engineering and Computer Science
August, 2002, in partial fulfillment of the
requirements for the degree of
Master of Science

Abstract

This thesis presents the characterization of an integrated starter/generator (ISG) based on an interior permanent-magnet (IPM) synchronous machine. The ISG is designed to operate in a hybrid vehicle; mounted to a traditional gasoline engine and coupled directly to the crankshaft. An experimental comparison is made between the design and the working prototype including a detailed description of the machine and the materials used. Theoretical operation and lumped-parameter models (LPMs) that predict its performance are summarized. Results from finite-element analysis (FEA) are also presented. An automated platform has been built for the purpose of testing the machine and is described in detail. This platform is used to measure mechanical losses, back-EMF, inductance, temperature, and performance while motoring and generating. The methods used for each experiment are described and the results are compared with LPM and FEA predictions.

This thesis will show that LPM predictions for q-axis inductance and PM flux linkage match well with FEA and measured results. However, the LPM predictions for d-axis inductance must be revised to include leakage from fringing fields around PM cavities at the ends of the stack, cross-coupling of q-axis flux, and details regarding the saturation of the PM bridges. The impact of these errors on the motoring torque is small because at high current, measurements, LPM, and FEA match fairly well. The impact of these errors on generating output power is substantial, and the ISG is not expected to meet the specifications for generating.

Thesis Supervisor: Jeffrey H. Lang
Title: Professor of Electrical Engineering, Associate Director of LEES

Thesis Supervisor: Thomas Keim
Title: Assistant Director of LEES
Acknowledgements

I would first and foremost like to thank my parents for their love and guidance, without which I would not be where I am today, and my wife for her enduring support and patience.

I would also like to thank my advisors, Prof. Jeff Lang and Dr. Tom Keim, for their technical guidance, Dr. Ed Lovelace for his continued involvement in this thesis, Mr. Jackson Wai and Prof. Tom Jahns at the University of Wisconsin for the development of the control software, Mr. Frank O’Sullivan for developing LabVIEW drivers, Mr. Wayne Ryan for machining blocks used in the drive stand, Mr. Pat McLeer for contributions beyond manufacturing the prototype machines, Prof. James Kirtley for technical guidance, Dr. Franco Leonardi and Dr. John Miller at Ford Motor Company for various technical contributions, and my colleges in the LEES laboratory for various “not-so-technical” contributions.

I would like to acknowledge the companies that made financial contributions to the thesis. They are the member companies of the Consortium, Ford Motor Company for funding the manufacturing of prototype machines, Kollmorgen Corporation for donating a load motor and drive, and Danfoss Corporation for donating an industrial drive.


<table>
<thead>
<tr>
<th>Chapter 1</th>
<th>Introduction</th>
<th>11</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>Background</td>
<td>11</td>
</tr>
<tr>
<td>1.2</td>
<td>ISG Specifications</td>
<td>12</td>
</tr>
<tr>
<td>1.3</td>
<td>Thesis Objectives and Contributions</td>
<td>12</td>
</tr>
<tr>
<td>1.4</td>
<td>Thesis Organization</td>
<td>13</td>
</tr>
<tr>
<td>1.5</td>
<td>Summary of Conclusions</td>
<td>13</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Chapter 2</th>
<th>Fabrication</th>
<th>15</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.1</td>
<td>Description of the ISG</td>
<td>15</td>
</tr>
<tr>
<td>2.2</td>
<td>Differences from Theoretical Design</td>
<td>17</td>
</tr>
<tr>
<td>2.3</td>
<td>Summary</td>
<td>20</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Chapter 3</th>
<th>Theory</th>
<th>23</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.1</td>
<td>Definitions</td>
<td>23</td>
</tr>
<tr>
<td>3.2</td>
<td>Lumped-parameter Models</td>
<td>26</td>
</tr>
<tr>
<td>3.3</td>
<td>LPM Predictions</td>
<td>34</td>
</tr>
<tr>
<td>3.4</td>
<td>Summary</td>
<td>34</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Chapter 4</th>
<th>Physical Experiments</th>
<th>37</th>
</tr>
</thead>
<tbody>
<tr>
<td>4.1</td>
<td>Experimental Setup</td>
<td>37</td>
</tr>
<tr>
<td>4.2</td>
<td>Mechanical Losses in the Drive Stand</td>
<td>43</td>
</tr>
<tr>
<td>4.3</td>
<td>DC Stator Resistance</td>
<td>45</td>
</tr>
<tr>
<td>4.4</td>
<td>Permanent Magnet Flux linkage</td>
<td>45</td>
</tr>
<tr>
<td>4.5</td>
<td>Inductances</td>
<td>48</td>
</tr>
<tr>
<td>4.6</td>
<td>Torque vs. Angle – Stationary</td>
<td>53</td>
</tr>
<tr>
<td>4.7</td>
<td>Motoring</td>
<td>55</td>
</tr>
<tr>
<td>4.8</td>
<td>Generating</td>
<td>58</td>
</tr>
<tr>
<td>4.9</td>
<td>Thermal Model</td>
<td>61</td>
</tr>
<tr>
<td>4.10</td>
<td>Summary</td>
<td>63</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Chapter 5</th>
<th>Numerical Experiments</th>
<th>65</th>
</tr>
</thead>
<tbody>
<tr>
<td>5.1</td>
<td>MagNet Model</td>
<td>65</td>
</tr>
<tr>
<td>5.2</td>
<td>Permanent Magnet Flux linkage</td>
<td>67</td>
</tr>
<tr>
<td>5.3</td>
<td>Inductances</td>
<td>67</td>
</tr>
</tbody>
</table>
List of Figures

Figure 1.1: Generating power requirements ................................................................. 13
Figure 2.1: ISG rotor stack and hub assembly ............................................................ 15
Figure 2.2: Cross section of the ISG laminations ....................................................... 16
Figure 2.3: ISG stator stack assembly ........................................................................ 16
Figure 2.4: Magnetic circuit for measuring permanent magnet flux density .............. 18
Figure 2.5: Flux lines for the designed stator windings (left) and a basket wound stator (right) ........................................................................................................ 19
Figure 2.6: Stator winding diagram ............................................................................ 21
Figure 3.1: D- and q-axis definitions for a PM synchronous machine ....................... 23
Figure 3.2: Simple magnetic circuit ......................................................................... 26
Figure 3.3: Short-pitched coil angle .......................................................................... 27
Figure 3.4: Stator tooth dimensions .......................................................................... 28
Figure 3.5: Zigzag leakage flux path ......................................................................... 29
Figure 3.6: Sectional view of rotor showing fringing flux paths ................................ 30
Figure 3.7: LPM magnetic circuit for calculating the q-axis inductance .................... 30
Figure 3.8: B-H curve for core material in LPM ......................................................... 31
Figure 3.9: Rotor saturation sections ......................................................................... 32
Figure 3.10: LPM magnetic circuit for calculating the d-axis inductance ................... 32
Figure 3.11: LPM magnetic circuit for calculating the PM flux linkage ................... 33
Figure 3.12: LPM inductance predictions .................................................................. 35
Figure 4.1: Drive stand CAD drawing ......................................................................... 37
Figure 4.2: Dimensions of the T-slot base plate ......................................................... 38
Figure 4.3: Ball bearing ............................................................................................. 38
Figure 4.4: Diagram of the flexible shaft couplings showing how spring fits to the hubs ........................................................................................................... 39
Figure 4.5: Danfoss industrial drive .......................................................................... 39
Figure 4.6: Resistor load bank (left) and capacitor bank (right) ................................. 40
Figure 4.7: Kollmorgen wiring diagram ..................................................................... 41
Figure 4.8: Losses in the shaft calculated from spin down data .................................. 44
Figure 4.9: Line to neutral phase voltage waveform at 4000 RPM ......................... 47
Figure 4.10: Harmonic content of back-EMF waveform at 4000 RPM .................. 47
Figure 4.11: Back-EMF data points before averaging (5 points plotted at each speed) ............................................................................................................ 48
Figure 4.12: Experimental setup for measuring inductance ..................................... 48
Figure 4.13: V and I measurements recorded during q-axis inductance experiment ............................................................................................................. 50
Figure 4.14: Measured q-axis flux linkage used to calculate $L_q$ .............................. 51
Figure 4.15: Measured d-axis flux linkage used to calculate $L_d$ .............................. 51
List of Figures

Figure 4.16: Measured $d$- and $q$-axis inductances ............................................................. 53
Figure 4.17: Experimental setup for measuring stationary torque versus angle .................. 53
Figure 4.18: Torque versus control angle experimental results at 150 $A_{\text{peak}}$ .................. 54
Figure 4.19: Maximum torque per Amp trajectory curve fit to data from three speeds ....... 57
Figure 4.20: Sample power flow at 50 RPM showing shaft power and losses ................. 57
Figure 4.21: Maximum torque at 300 $A_{\text{peak}}$ ................................................................. 58
Figure 4.22: Experimental setup for generating test ............................................................. 58
Figure 4.23: Generating power and efficiency from 500 – 6000 RPM .............................. 60
Figure 4.24: Current trajectory for maximum output power and varying speed ............... 61
Figure 4.25: Simplified thermal model of the ISG in the MIT drive stand ....................... 62
Figure 4.26: Temperature rise per kW of the stator windings ............................................ 62
Figure 5.1: MagNet model: no rotor shift (left) and 5 mechanical degree rotor shift (right) ..... 65
Figure 5.2: $B$-$H$ curve for the core material in the MagNet model ................................... 66
Figure 5.3: Simulated PM flux linkage and the resulting fundamental amplitude, $\lambda_{PM}$ .... 67
Figure 5.4: MagNet inductance predictions ....................................................................... 68
Figure 5.5: Inductance ripple predicted by MagNet ............................................................ 69
Figure 5.6: Torque ripple at rated current ($460 A_{\text{peak}}$) .................................................... 70
Figure 5.7: Cross-coupling effects on inductance predictions ............................................ 71
Figure 5.8: Contour map of $L_d$ ......................................................................................... 72
Figure 5.9: Contour map of $L_q$ ......................................................................................... 73
Figure 6.1: Comparison of fundamental peak back-EMF phase voltage ......................... 76
Figure 6.2: Comparison of inductance measurements without cross-coupling ............... 77
Figure 6.3: Comparison of FEA and measured stationary torque at 150 $A_{\text{peak}}$ ........... 78
Figure 6.4: Comparison of measured motoring torque and predictions at 125 RPM ....... 79
Figure 6.5: Comparison of torque curves at 300 $A_{\text{peak}}$ ................................................. 80
Figure 6.6: Maximum shaft power with Danfoss inverter limitations .............................. 81
Figure 6.7: Theoretical maximum power without inverter limitations ............................. 82
Figure 6.8: Comparison of shaft power, predicted after adjusting for filter delay in currents .... 83
List of Tables

Table 2.1: ISG design parameter values .................................................................................... 17
Table 2.2: Comparison of specified and measured parameters................................................. 17
Table 3.1: LPM predictions ........................................................................................................... 34
Table 4.1: List of components used in the drive stand.............................................................. 40
Table 4.2: List of test and measurement equipment and their respective communication ports ... 42
Table 4.3: Spin down test procedure.......................................................................................... 43
Table 4.4: DC resistance test procedure.................................................................................... 45
Table 4.5: Summary of measured phase resistances................................................................ 45
Table 4.6: Back-EMF measurement test procedure................................................................. 46
Table 4.7: Inductance measurement test procedure ................................................................. 50
Table 4.8: Low current q-axis inductance test procedure ....................................................... 52
Table 4.9: Torque versus angle measurement test procedure ................................................ 54
Table 4.10: Motoring measurement test procedure..................................................................... 56
Table 4.11: Generating measurement test procedure .............................................................. 59
Table 4.12: Thermal experiment test procedure ....................................................................... 63
Table 5.1: Parameters used in MagNet model .......................................................................... 66
Table 6.1: Comparison of PM flux linkages.............................................................................. 75
Chapter 1 Introduction

In recent years, the number of electric loads in the average automobile has dramatically increased. Some of these loads are currently being researched at MIT, including an electronic-hydraulic power steering module and an electrically actuated engine valve [1]. Work being done by others includes electrically actuated suspension, brakes, steering, and air-conditioning [2]. These high power loads will place an increasing demand on the amount of power supplied by the generator. In addition to higher power consumption, the specifications on emissions have become more strict. These demands are forcing automobile manufacturers to seek more efficient, lighter weight components.

One solution to the future problems of power supply and component weight that is being investigated by MIT and others is an integrated starter/generator (ISG). One such ISG proposed by Dr. Edward Lovelace at MIT [3] replaces the existing starter, alternator, and flywheel with an interior permanent-magnet (IPM) synchronous machine that mounts directly to the crankshaft at the back of the engine. The result is an overall reduction in size, weight, and engine compartment complexity. The direct coupling to the crankshaft makes start/stop operation more feasible, attributing to an increase in fuel efficiency [2]. This ISG is designed to meet the anticipated power requirements of a high-end automobile in production between 2005 and 2010, as projected by members of the MIT/Industry Consortium on Advanced Electrical/Electronic Components and Systems. A prototype of Dr. Lovelace’s design was built by McCleer Power Inc., with funding provided by Ford Motor Company. The focus of this thesis is to experimentally characterize this prototype ISG and compare these characterization results with predictions from lumped-parameter models used for its design and separate finite element analyses.

1.1 Background

Before selecting the IPM machine as the design for the ISG, a tradeoff study compared four types of electric machines and mechanical configurations to determine the best candidate for further study [3]. The machine types analyzed were the induction machine (IM), IPM machine, surface PM machine, and switched reluctance machine. The mechanical configurations considered were a direct drive from the crankshaft and offset coupled (conventional generator location) with three different speed ratios: 3:1, 5:1, and dual speed: 3:1 while starting and 15:1 while generating.
The tradeoff study evaluated attributes such as the production cost of the machine and of the converter. The methodology of the study was to develop cost-optimized designs for each machine in different mechanical configurations and evaluate the performance characteristics of each design. Based on the results of the study, the IM and IPM machine had the lower overall costs. Between these two machines, the IPM machine was not as widely used and analyzed as the IM, and the IPM machine was marginally cheaper than the IM. Therefore, the IPM was selected for further investigation.

The design theory of the IPM machine was studied in great detail in [3]. A lumped-parameter model (LPM) of the IPM machine was adopted for analysis. This method of analysis was preferred over finite element analysis (FEA) because of the excessive computation time associated with FEA when sweeping a number of parameters over a wide design space. The LPM was developed for predicting parameters that defined the electrical performance of an IPM machine. When an LPM was used to predict the performance, a Monte Carlo synthesis covering multiple variables was searched for an optimal design in a relatively short period of time. The result of this work was a cost-optimized IPM machine design for an ISG. From there, FEA analysis was performed to refine the predictions of output power and torque, and a prototype machine was built.

1.2 ISG Specifications

The specifications for the ISG were set by members of the MIT/Industry Consortium on Advanced Automotive Electrical/Electronic Components and Systems during a conference in 1996. At that time, the specifications were intended to meet the future requirements of a high-end vehicle in production between 2005 and 2010. The specifications include:

- Motoring torque of 150 Nm from 0 – 100 RPM sustained for a maximum of 15 seconds
- Generating power requirements shown in Figure 1.1: 4 kW at 600 RPM to 6 kW at 6000 RPM
- Generating efficiency requirement of 75% at 3250 W and 1500 RPM

1.3 Thesis Objectives and Contributions

The work discussed in this thesis is a continuation of the investigation into a high-power ISG began by Dr. Edward Lovelace [3]. The objectives of this work are to assemble a fully automated dynamometer drive stand to test the prototype ISG, experimentally measure parameters such as permanent magnet flux linkage and inductance, experimentally measure the performance of the ISG as a motor and generator, and compare the experimental results with predictions from LPM and FEA analysis. Part of this work relies on control software developed by Prof. Thomas Jahns and Mr. Jackson Wai at the University of Wisconsin – Madison [4]. This software was tested on a similar drive stand there, having a second prototype ISG identical to the one characterized in this thesis.
Chapter 1: Introduction

1.4 Thesis Organization

Chapter 2 discusses the mechanical parameters of the prototype machine and how they differ from the optimized design discussed in [3]. Chapter 3 discusses the definitions used throughout this thesis, and the lumped-parameter models used to predict the parameters of the ISG. Chapter 4 discusses the experimental setup and dynamometer assembly. The experiments run to characterize the ISG are discussed in detail, and results are presented. Chapter 5 discusses the finite element analysis model used, and the simulations run to predict the performance of the ISG. Cross-coupling of the $d$- and $q$-axis inductances is also introduced in this chapter. Chapter 6 compares the experimental results to predictions from the LPM and FEA analyses. Discrepancies in the data are also explained. Chapter 7 summarizes the results and suggests future work for refining the LPMs and characterizing the ISG.

1.5 Summary of Conclusions

This thesis will show that LPM predictions for $q$-axis inductance and PM flux linkage match well with FEA and measured results. However, the LPM predictions for $d$-axis inductance must be revised to include leakage from fringing fields around PM cavities at the ends of the stack, cross-coupling of $q$-axis flux, and details regarding the saturation of the PM bridges. The impact of these errors on the motoring torque is small because at high current, measurements, LPM, and FEA match fairly well. The impact of these errors on generating output power is substantial, and the ISG is not expected to meet the specifications for generating.

Figure 1.1: Generating power requirements
McCleer Power Inc. was contracted by Ford Motor Company to build the prototype machines. Three working prototype machines have been constructed. One resides at MIT, one resides at the University of Wisconsin-Madison, and one resides at McCleer Power. This chapter discusses the method that McCleer Power used to assemble the ISG prototypes. A summary of the machine measurements and specifications is provided, as well as a discussion of the impact of the differences between the prototype machine and the original design.

2.1 Description of the ISG

Figure 2.1: ISG rotor stack and hub assembly

2.1.1 Rotor and Stator Assembly

Figure 2.1 is a CAD drawing of the rotor. The rotor consists of a stack of magnetically soft laminations compressed onto an aluminum hub with permanent magnets (PMs) embedded in cavities pre-cut in the rotor. The first step in constructing the rotor was machining the pieces. The steel used for the laminations is ANSI grade M-19, Temple Steel designation 26N174. This designation is interpreted as 26 gauge thickness (0.47 mm), non-oriented, and less than 1.74 Watts per pound at 1.5 T and 60 Hz. The laminations were laser cut with a lateral tolerance of ±0.05 mm. Figure 2.2 is a cross-section of a lamination showing the U-shaped magnet cavities and holes to allow for bolts. The active length of the rotor stack is specified to be 60 mm. The stack was assembled by adding and compressing laminations one at a time until the total length of
the stack exceeds 60 mm. Bolts were threaded through the boltholes to secure the compressed stack to the hub. The total number of laminations is unknown, however McCleer Power has reported that historically this process of assembling laminations yields a 97% packing factor.

The next two steps in the construction are to insert the PM material and magnetize the magnets. For the first rotor built, McCleer Power inserted unmagnetized permanent magnet pieces and potted around them to secure them in place. Then the rotor and stator were mounted in a test stand and the stator was used to generate the field that would magnetize the magnets. McCleer Power found that this required more current than the stator was capable of supporting. Therefore, this rotor was discarded and the remaining three rotors were assembled by first magnetizing the PM material and then inserting it into the rotor and potting.

Figure 2.2: Cross section of the ISG laminations

Figure 2.3: ISG stator stack assembly

Figure 2.3 shows a CAD drawing of the stator stack. The stator laminations were laser cut from the same M-19 material as the rotor. The stator stack was also assembled by the same
process as the rotor: by adding and compressing one lamination at a time until the length of the stack exceeded the specified 60 mm. The stator has 72 un-skewed and partially closed slots, and was wound using a single layer basket wind. This winding pattern is described in more detail in Section 2.2.3. Each conductor is made up of 13 strands of insulated 19.5 AWG wires. Once the winding was complete, the entire stator was dipped in epoxy. The epoxy was then removed from the inside surface of the stator. The cross-section of the rotor and stator lamination is shown in Figure 2.2.

2.1.2 Machine Parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated phase current</td>
<td>327 (A_{RMS})</td>
</tr>
<tr>
<td>Rated phase voltage</td>
<td>19.3 (V_{RMS})</td>
</tr>
<tr>
<td>Turns per phase</td>
<td>24</td>
</tr>
<tr>
<td>Total slots</td>
<td>72</td>
</tr>
<tr>
<td>Pole pairs</td>
<td>6</td>
</tr>
<tr>
<td>Moment of inertia</td>
<td>63.3 g(\cdot)m(^2)</td>
</tr>
</tbody>
</table>

Table 2.1: ISG design parameter values

Table 2.1 is a summary of various design values describing the ISG [3]. These values apply to the optimum machine design. Table 2.2 is a summary of the dimensions of the ISG that have been measured. PM permeability and remanent flux density was measured from a sample magnet piece from the same batch of magnets used in the rotor. The remanent flux density is calculated from a measurement of flux taken from the magnetic circuit shown in Figure 2.4.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Specification</th>
<th>Measured</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>PM remanent flux density</td>
<td>0.28</td>
<td>(\geq 0.28)</td>
<td>T</td>
</tr>
<tr>
<td>Active length</td>
<td>60</td>
<td>58.43 (\pm 0.23)</td>
<td>mm</td>
</tr>
<tr>
<td>Rotor OD</td>
<td>218</td>
<td>216.789 (\pm 0.025)</td>
<td>mm</td>
</tr>
<tr>
<td>Stator ID</td>
<td>219.27</td>
<td>218.008 (\pm 0.025)</td>
<td>mm</td>
</tr>
<tr>
<td>Stator OD</td>
<td>272</td>
<td>280 (\pm 0.1)</td>
<td>mm</td>
</tr>
<tr>
<td>Airgap</td>
<td>0.635</td>
<td>0.65 (\pm 0.05)</td>
<td>mm</td>
</tr>
<tr>
<td>Total length</td>
<td>90</td>
<td>90 (\pm 2)</td>
<td>mm</td>
</tr>
</tbody>
</table>

Table 2.2: Comparison of specified and measured parameters

2.2 Differences from Theoretical Design

There are some key differences between the theoretical design and the prototype machine. This section discusses only the physical differences between the prototype and the design and does not address any of the inaccuracies between the predicted performance of the machine and the measured results. These results are compared in Chapter 6.

2.2.1 Airgap

The lumped-parameter models assume a smooth and constant airgap in the machine. Although this may be a good approximation of the actual airgap, tolerances should be assigned to the assumed airgap to account for various inaccuracies. The tolerances of the measurement
devices used to align the rotor inside the stator may present a planar or angular offset. In addition, the tolerance of the laser cut laminations and alignment of the stack may present a non-uniform rotor or stator diameter.

![Magnetic Circuit Diagram](image)

Figure 2.4: Magnetic circuit for measuring permanent magnet flux density

The airgap was measured by two methods. The first measurement was taken by placing calipers on the rotor’s outer diameter and on the stator’s inner diameter. The edges of the laminations create a rough surface on both the rotor and stator. When the diameters of the rotor and stator were measured, the measurement device could have caught a lamination that jutted out farther than the others. This would result in a smaller measurement of the airgap. The second measurement of the airgap was taken from individual laminations. McCleer Power had laminations left over from the assembly. Two sets of these were measured to arrive at an average airgap of 0.65 mm. To account for all variations, a tolerance of ±0.05 mm is assigned to the airgap. Correspondingly, simulations that predict machine parameters using the airgap predict a target range instead of a single target value to reflect this tolerance.

2.2.2 Permanent Magnets

The ISG design specifies that the magnet material should be bonded ferrite. This material is chosen because of its low cost, high temperature range, and good corrosion resistance relative to rare earth magnets. Ferrite magnets typically cannot achieve the remanent flux density $B_r$ and coercive force $H_c$ that rare earth magnets can. However, the requirements set by the optimization exercises are low enough to be met using ferrite magnets. Another characteristic of bonded ferrite magnets is a positive temperature coefficient for coercive force. In other words, as the temperature of the magnet increases, so would the amount of flux required to permanently demagnetize it. For permanent magnet machines, demagnetization is an important concern. It was determined through FEA that it would require over 500 A of negative d-axis current for the onset of demagnetization to occur. The flux would concentrate at the ends of the magnets so only these regions would demagnetize first. As the current increased beyond 500 A the demagnetization would penetrate further into the center of the magnets [3].

Because of availability, the prototype machine is not built with bonded ferrite magnets. Instead, bonded neodymium-boron-iron (NdFeB) magnets are used. These rare earth magnets are said to be superior to ferrite. They are capable of much higher remanent flux density and coercive force, and their remanent flux density to weight ratio is roughly 20% greater than that of ferrite. The design of the ISG does not take advantage of these benefits because the specifications can be met with ferrite magnets. Additionally, the cost of NdFeB magnets is 6-8 times more than that of a typical ferrite magnet. Finally, the upper operating temperature limit of
bonded NdFeB magnets is 100-180 degrees C depending on the grade, compared to 200-300 degrees C for ferrite magnets [8].

Unlike bonded ferrite, NdFeB magnets have a negative temperature coefficient for coercive force. This means the magnets are more susceptible to demagnetization as their temperature increases. Despite a negative temperature coefficient, the overall coercive force of NdFeB magnets is typically greater than ferrite, making NdFeB magnets superior in demagnetization resilience. Hybrid magnets with a mixture of NdFeB and ferrite will have a blend of characteristics from the two classes of magnets, which can be tailored for a specific application.

There are additional considerations that led to selecting bonded magnets over less expensive sintered blocks. Both sintered ferrite and sintered NdFeB magnets are not practical for shaping into smooth arcs to fill the magnet cavities and are limited in the directions they can be magnetized. They are often brittle and hard. On the other hand, bonded materials can be injection molded to fit complex geometries, and magnetized in any direction. The bonded NdFeB magnets in the prototype were molded and magnetized prior to being inserted into the rotor. Sintered NdFeB magnets are additionally limited by their corrosion behavior. In humid applications, a protective coating is highly recommended. Coatings that have been used successfully include E-coat (a liquid dip epoxy coating), dry electrostatic spray epoxy, nickel plating, and combinations of these coatings. Changes in composition and processing over the past several years have resulted in significant improvements in corrosion resistance and high temperature performance (180 degrees C). The bonding material used in the bonded NdFeB magnets served the additional purpose of protecting the material from corrosion, thus additional protection was not required [8].

2.2.3 Stator Winding Pattern

![Flux lines for the designed stator windings (left) and a basket wound stator (right)](image)

The stator winding pattern is specified to be distributed windings with each phase belt fully occupying one slot and half filling the adjacent slots. This winding pattern is shown in Figure 2.5.
on the left, where the phase A coils have been superimposed on the stator slots to indicate where they are wound. Because of the nature of this winding pattern, the phase A axis lies on a slot. The orientation in the figure shows the rotor q-axis aligned with the phase A axis. Note that the PM cavities align with stator teeth, making it harder for the q-axis flux to cross the airgap. The prototype machine is wound using a “basket wind” pattern with each phase belt fully filling two adjacent slots. This winding pattern is shown in Figure 2.5 on the right, where the phase A coils have been superimposed on the stator slots. With this winding pattern, when the rotor q-axis is aligned with the phase A axis, the PM cavities align with stator slots, and q-axis flux can easily cross the airgap. The machine is affected by tooth ripple for both winding patterns. The amount that the prototype machine is affected is analyzed in Chapter 5.

The stator is designed to have 24 series turns in each phase. Since the ISG is a 12-pole machine, this results in two turns per pole. However, each phase of the prototype machine stator is wound as two separate circuits connected in parallel. These two circuits are indicated in Figure 2.6 by the numbers 1 and 2 following the phase letters. In order for this winding pattern to produce the same flux in the rotor for a given total phase current, the number of turns per pole \( N \) is doubled to four. For each phase, the current \( I \) in each circuit is half that of the total phase current because the two circuits are connected in parallel. The flux produced by each circuit is determined by the \( NI \) product. Because the number of turns per pole is doubled and current per pole halved, the flux generated by each pole is identical to that of a single circuit with two turns per pole.

### 2.2.4 Stator Back Iron

The stator outer diameter is specified to be 272 mm, however the prototype stator is 280 mm. This has no significant effect on the performance of the machine. The stator was built larger in order to make the machine fit in an existing test fixture at Ford Motor Company. This test fixture is copied in the drive stand at MIT.

### 2.3 Summary

This chapter discussed the method for assembling the prototype machines and the materials used. A summary of the machine parameters and dimensions was provided including specified values and actual measurements. Major differences between theoretical design and the prototype machine were presented including the airgap, permanent magnet material, and stator winding pattern. This chapter also discussed the advantages and disadvantages of ferrite and neodymium-boron-iron permanent magnet materials.
<table>
<thead>
<tr>
<th>Slot number</th>
<th>4 turns per pole - conductors stranded with 13 #19.5 AWG wires</th>
<th>6 slots per pole</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2</td>
<td>3</td>
</tr>
</tbody>
</table>

**Figure 2.6: Stator winding diagram**

- A1+
- A2N
- B1+
- B2+
- C1N
- C2N
- A1'
- A2'
- B1'
- C2'
- A1''
- A2''
- B1''
- C2''

1 to A1'
2 to A2'
3 to B1'
4 to C1+
5 to C2N
6 to A1
7 to A2
8 to B1
9 to C2
10 to A1+
11 to A2+
12 to B1N
13 to C2+
This chapter discusses the definitions used throughout this thesis, and the LPMs (lumped-parameter models) that describe the ISG (integrated starter/generator) [3]. These LPMs are used to predict the inductances and PM (permanent magnet) flux linkage in the ISG. In Chapter 6, predictions of the torque and power are made, and compared with measured results and FEA (finite element analysis) predictions.

3.1 Definitions

![Diagram of D-q-o transformation for a PM synchronous machine](image)

Figure 3.1: D- and q-axis definitions for a PM synchronous machine

The $dq0$ transformation, also referred to as the Blondel-Park transformation, is a proven theory for synchronous machine analysis [5]. This transformation takes advantage of the synchronous rotation of the fundamental stator $mmf$ excitation and the rotor. To the rotor, the $mmf$ excitation appears constant with respect to time. To elaborate, consider looking at the rotating magnetic flux in a machine from two different perspectives. The first perspective is of one standing on the stator. From this perspective the rotor and flux excitation appear to be spinning while the stator appears stationary. Now consider the perspective of one standing on the rotor. From this perspective the stator appears to be spinning, but the rotor and flux excitation...
appear stationary. The \(dq0\) transformation is a method of transforming parameters such as voltage, current, and flux from the stator frame where they vary in time to the rotor frame where they are constants and easier to work with.

A two-pole, three-phase, PM synchronous machine is shown in Figure 3.1. In the figure, a two-axis reference frame has been superimposed onto the rotor. The rotor direct axis (\(d\)-axis) is aligned with the PM field axis. The rotor quadrature axis (\(q\)-axis) is in quadrature with the \(d\)-axis and leading it by 90 electrical degrees. The electrical angle is \(p\) times the mechanical angle where \(p\) is the number of pole pairs. This coordinate frame rotates synchronously with the rotor. The positive direction of currents in the stator phases are indicated by a dot for current out of the page and cross for current into the page. The stator phase axes are defined using the right-hand rule for circulating currents. The phase axes do not rotate, however the currents in the phases produce a current vector that does rotate synchronously with the rotor. The angle \(\theta\) is defined as the electrical angle between the rotor direct-axis and the stator phase \(A\) axis.

The \(dq0\) transformation is given in (3.1). The letter \(S\) refers to the parameter being transformed to the \(dq0\) frame, which can be current, flux, or voltage. The subscripts \(a, b,\) and \(c\) refer to the three-phase quantities and the subscripts \(d, q,\) and \(0\) refer to the corresponding \(d\)-axis, \(q\)-axis, and zero-sequence quantities. The zero-sequence term \(S_0\) is always zero for a balanced three-phase excitation.

\[
\begin{bmatrix}
S_d \\
S_q \\
S_0
\end{bmatrix} = \frac{2}{3} \begin{bmatrix}
\cos(\theta) & \cos(\theta - 120^\circ) & \cos(\theta + 120^\circ) \\
-\sin(\theta) & -\sin(\theta - 120^\circ) & -\sin(\theta + 120^\circ) \\
0.5 & 0.5 & 0.5
\end{bmatrix} \begin{bmatrix}
S_a \\
S_b \\
S_c
\end{bmatrix}
\]  

(3.1)

Equation (3.2) illustrates the transformation of a balanced three-phase set of currents to the \(dq0\) frame. The electrical angle \(\theta\) has been replaced by \(\omega t\), indicating the machine is rotating at the electrical frequency \(\omega t\). The three-phase current oscillates at the same electrical frequency, however a phase shift \(\gamma\) has been introduced as a constant angular offset between the rotor \(d\)-axis and the phase \(A\) current vector, otherwise known as the control or torque angle. The resulting \(d\)- and \(q\)-axis currents are functions of the control angle and current magnitude, which are constant when operating in periodic steady state.

\[
\begin{bmatrix}
\cos(\omega t) & \cos(\omega t - 120^\circ) & \cos(\omega t + 120^\circ) \\
-\sin(\omega t) & -\sin(\omega t - 120^\circ) & -\sin(\omega t + 120^\circ) \\
0.5 & 0.5 & 0.5
\end{bmatrix} \begin{bmatrix}
I \cos(\omega t + \gamma) \\
I \cos(\omega t + \gamma - 120^\circ) \\
I \cos(\omega t + \gamma + 120^\circ)
\end{bmatrix} = \begin{bmatrix}
I \cos(\gamma) \\
I \sin(\gamma) \\
0
\end{bmatrix}
\]  

(3.2)

Electrical power flowing into the ISG is defined to be positive. Mechanical power flowing out of the ISG is also defined to be positive. These definitions are generally referred to as a motoring convention. Following these definitions, positive stator currents flow into the positive voltage terminals and torque produced in the direction of rotation is positive.

The inductance of the ISG measured from the phase terminals is a function of the rotor position. The rotor of the ISG is similar to the rotor shown in Figure 3.1 in that as its position is varied, the reluctance and thus the inductance measured at the stator terminals changes. This
characteristic of inductance that is dependent on rotor position is known as saliency. The inductance matrix of the ISG is given in (3.3).

\[
L = \begin{bmatrix}
L_{aa} & L_{ab} & L_{ac} \\
L_{ab} & L_{bb} & L_{bc} \\
L_{ac} & L_{bc} & L_{cc}
\end{bmatrix}
\] (3.3)

A simplified model for these inductances in a variable reluctance machine is given by

\[
\begin{align*}
L_{aa} &= L_{a00} + L_t + L_{g2} \cos(2\theta) \\
L_{ab} &= L_{a00} + L_t + L_{g2} \cos(2\theta + 120^\circ) \\
L_{bc} &= L_{a00} + L_t + L_{g2} \cos(2\theta - 120^\circ) \\
L_{cc} &= L_{a00} + L_t + L_{g2} \cos(2\theta - 120^\circ)
\end{align*}
\] (3.4)

where \( \theta \) is the rotor angle in electrical degrees, \( L_{a00} \) is the airgap inductance, \( L_t \) is the total leakage inductance, and \( L_{g2} \) is the inductance term resulting from the rotor saliency [5]. The \( d \)- and \( q \)-axis inductances are written directly from the terms in (3.4) as

\[
\begin{align*}
L_d &= L_t + \frac{3}{2}(L_{a00} + L_{g2}) \\
L_q &= L_t + \frac{3}{2}(L_{a00} - L_{g2})
\end{align*}
\] (3.5)

The three-phase flux linkages for the ISG are calculated by

\[
\begin{bmatrix}
\lambda_a \\
\lambda_b \\
\lambda_c
\end{bmatrix} =
L \cdot
\begin{bmatrix}
I_a \\
I_b \\
I_c
\end{bmatrix} +
\lambda_{PM}
\begin{bmatrix}
\cos(\theta) \\
\cos(\theta - 120^\circ) \\
\cos(\theta + 120^\circ)
\end{bmatrix}
\] (3.6)

where \( L \) is defined in (3.3), \( I_{a,b,c} \) are the phase currents, \( \theta \) is the rotor angle in electrical degrees, and \( \lambda_{PM} \) is the amplitude of the fundamental PM flux linkage. The \( d \)- and \( q \)-axis flux linkages are calculated by

\[
\begin{bmatrix}
\lambda_d \\
\lambda_q
\end{bmatrix} =
\begin{bmatrix}
L_d I_d + \lambda_{PM} \\
L_q I_q
\end{bmatrix}
\] (3.7)

The \( d \)- and \( q \)-axis voltages are calculated by

\[
\begin{bmatrix}
v_d \\
v_q
\end{bmatrix} =
\begin{bmatrix}
R_a I_d - \omega_L I_q \\
R_a I_q + \omega_L (L_d I_d + \lambda_{PM})
\end{bmatrix}
\] (3.8)

where \( R_a \) is the resistance of one phase and \( \omega_L \) is the electrical frequency in radians per second. The torque from electric origin is predicted from the \( dq \) inductances, PM flux linkage, and \( dq \) currents by

\[
\tau_e = \frac{3}{2} p(\lambda_{PM} + (L_d - L_q)I_d)I_q
\] (3.9)

where \( p \) is the number of pole pairs. Torque is positive when motoring. The power is predicted by
\[ P_e = \frac{1}{2} (v_d I_d + v_q I_q) \]  

and is also positive when motoring.

### 3.2 Lumped-parameter Models

The purpose for developing LPMs of the ISG is for use in a Monte Carlo synthesis that sweeps a large design space, searching for a cost-optimized ISG design. A complete analysis of the LPMs is provided by Dr. Lovelace [3]. This section summarizes the development of the linear \( d \)-axis LPM and the saturable \( q \)-axis LPM for calculating inductances and PM flux linked by the stator.

![Figure 3.2: Simple magnetic circuit](image)

The magnetic circuit shown in Figure 3.2 demonstrates how the LPM is used to calculate parameters such as flux linkage and inductance. The source \( F_{mmf} \) represents a magneto-motive force that originates from either the stator windings or a PM. The reluctance element \( R_{eq} \) represents the equivalent reluctance of the path taken by the magnetic flux \( \phi \). These three parameters are related by \( \phi = F_{mmf} / R_{eq} \). Flux linkage is calculated from the flux and then inductance calculated from the flux linkage. The LPMs define multiple \( mmf \) sources and reluctances that model the ISG along the \( d \) - and \( q \)-axis. These elements form complex magnetic circuits that are solved for the inductances and PM flux linkage.

#### 3.2.1 Round Rotor Inductance

The round-rotor, or airgap, inductance \( L_{ag} \) is calculated assuming infinite permeability in the stator and rotor cores and no PMs embedded in the rotor. The relevance of such a simplified inductance calculation will become apparent in Section 3.2.3 for calculating the \( d \)-axis inductance. The airgap surface along the inside of the stator is not smooth because of the slot openings. To accommodate for the slot features, the airgap is modified by

\[ g' = g - \frac{w_o + w_f}{w_o + w_f - g_kcs} \]  

(3.11)

where \( g \) is the actual airgap (stator inner radius \( r_n \), minus the rotor outer radius \( r_o \)), \( w_o \) and \( w_f \) are defined in Figure 3.4, and \( k_{cs} \) is the Carter coefficient defined in (3.12). Equation (3.11) effectively makes the airgap larger. The inductance is solved assuming a smooth stator surface and the modified airgap \( g' \).

\[ k_{cs} = \frac{w_o}{2g} \tan^{-1} \left( \frac{w_o}{2g} \right) - \log \left( \frac{\sqrt{w_o^2 + 4g^2}}{2g} \right) \]  

(3.12)
The round-rotor inductance is written as

\[ L_{ag} = \frac{6 \mu_0 N_a^2 k_p^2 l r_o}{p^2 g} \]  

(3.13)

where \( N_a \) is the series turns per phase, \( k_p \) is the fundamental pitch factor, \( l \) is the active stack length, \( r_o \) is the rotor outer radius, and \( p \) is the number of pole pairs.

The pitch factor \( k_p \) in (3.13) accounts for the short-pitched stator windings. The LPM as described in [3] also has a breadth factor and skew factor built in but the prototype ISG studied here has concentrated windings and no skew, therefore not requiring these factors. A short-pitched coil and the definition of \( \theta_{sp} \) are illustrated in Figure 3.3 [10]. The pitch factor for the \( n^{th} \) harmonic is calculated by

\[ k_{pm} = \sin \left( \frac{n \theta_{sp}}{2} \right) \quad n = 1, 5, 7, \ldots \]  

(3.14)

![Figure 3.3: Short-pitched coil angle](image)

3.2.2 Leakage Inductance

The \( d \)- and \( q \)-axis inductances \( L_d \) and \( L_q \) are each divided into two terms: a magnetizing inductance \( L_{dm} \) and \( L_{qm} \) for each respective axis, and a leakage inductance \( L_l \), which is the same for both. Therefore,

\[ L_d = L_{dm} + L_q \quad L_q = L_{qm} + L_l \]  

(3.15)

The magnetizing inductances are defined in sections to follow. This section summarizes the leakage terms defined by \( L_l \) that are included in the LPM.

The total leakage inductance \( L_l \) is the sum of slot leakage inductance \( L_{slot} \), end-turn leakage inductance \( L_{end} \), \( 5^{th} \) and \( 7^{th} \) harmonics \( L_{nbelt} \) of the round rotor inductance, and zigzag leakage inductances \( L_{fzg} \) and \( L_{bzg} \). Therefore,

\[ L_l = L_{slot} + L_{end} + L_{5belt} + L_{7belt} + L_{fzg} + L_{bzg} \]  

(3.16)
The slot leakage inductance models the flux in the stator core that does not cross the airgap. This inductance is calculated from an approximation of the permeance of a single slot $P_s$ [11]. $P_s$ is a function of the dimensions of the slots shown in Figure 3.4. The total slot leakage inductance is given by

$$L_{slot} = \frac{12N_a^2 P_s}{n_s} \tag{3.17}$$

where $N_a$ is the series turns per phase and $n_s$ is the total number of slots in the stator.

The end-turn leakage inductance models the flux paths that are generated by the overhang of the windings, or end-turns, on the stator. These flux paths are generally through air and highly dependent on the winding pattern of the stator. The end-turns on the prototype machine are flattened towards the stator core in order to reduce the overall length of the ISG. This may have increased the end-turn leakage since more of the flux could pass through high permeability core material. End-turn inductance is not simulated by the 2D finite element analysis (FEA) later discussed in Chapter 5, and is not easily measured. Therefore, an accurate prediction here is required. The LPMs used the equation for $L_{end}$ given by

$$L_{end} = 3 \frac{140}{4} \frac{\mu r_{si} N_a^2}{p^2} \left( \frac{\theta_{sp}}{\pi} - 0.3 \right) \tag{3.18}$$

where $r_{si}$ is the stator inner radius, $N_a$ is the series turns per phase, $\theta_{sp}$ is the short-pitching angle defined in Figure 3.3, and $p$ is the number of pole pairs. This equation is also found in [13]. To determine the possible range of $L_{end}$, two other equations from separate sources are also compared. From [12],

$$L_{end} = \frac{0.1006 N_a^2 l_o k_{pf} C}{2\pi \cdot 25.4 \cdot 10^5 p} \tag{3.19}$$

where $N_a$ is the number of turns per phase, $l_o$ is the total end-turn length per phase in mm, $C$ is the end-turn leakage factor (0.24 for the prototype), $k_{pf}$ is the pitch factor, and $p$ is the number of pole pairs. From [11],
Chapter 3: Theory

\[ L_{\text{end}} = 9.6 \mu \text{o} \frac{N_a^2}{2p} k_{pl}^2 \left( \frac{\theta_{sp} (r_{si} + d_o + d_s/2)}{p} + \frac{3w_{st}}{2} \right) \]  

(3.20)

where \( N_a \) is the number of turns per phase, \( k_{pl} \) is the pitch factor, \( p \) is the number of pole pairs, \( \theta_{sp} \) is defined in Figure 3.3, \( r_{si} \) is the stator inner radius, and \( d_o, d_s, \) and \( w_{st} \) are defined in Figure 3.4. These equations predicted a range of 1.9 - 4.9 \( \mu \)H; the LPM prediction is 3.1 \( \mu \)H. To approximate for the flattening of the end-turns, 5 \( \mu \)H is the assumed value for \( L_{\text{end}} \) that is added to the FEA results.

The harmonics of the round rotor inductance come from the harmonics in the flux excitation. These harmonics generally do not contribute to net torque production. For this reason, they are modeled as leakage terms.

\[ L_{\text{zng}} = L_{ag} \left( \frac{k_{pm}}{nk_{pl}} \right)^2 \quad n = 5, 7, \ldots \]  

(3.21)

The zigzag leakage inductances model the forward and backward traveling waves of flux that passed from one stator tooth to another by crossing the airgap in a zigzag pattern shown in Figure 3.5 [11]. The inductances are calculated by defining a series of factors that depended on parameters such as the tooth pitch and dimensions outlined in Figure 3.4. The factors are multiplied together to form the forward \( k_{afrz} \) and backward \( k_{afrb} \) zigzag factors. The inductances are then calculated from

\[ L_{\text{fzg}} = L_{ag} \left( \frac{k_{afrz}}{nk_{pl}} \right)^2 \quad L_{\text{bfrz}} = L_{ag} \left( \frac{k_{afrb}}{nk_{pl}} \right)^2 \]  

(3.22)

An additional leakage term was discovered to affect only the \( d \)-axis inductance, and is not incorporated into the LPMs or calculated by FEA. This is \( L_{\text{fringe}} \), which models the fringing flux around the PM cavities in the \( r-z \) plane, shown in Figure 3.6, and is only added to the \( d \)-axis inductance term. This reluctance path is analyzed in [14], and is calculated to add 8% to \( L_d \) [7]. This inductance is approximated to be a constant 9 \( \mu \)H, which is added to the FEA results for \( L_{ofr} \).
3.2.3 Q-axis Inductance

Figure 3.6: Sectional view of rotor showing fringing flux paths

The q-axis inductance $L_q$ is modeled by the sum of a magnetizing inductance $L_{qm}$ and a leakage inductance $L_d$ defined by (3.16). This section outlines the equations and models used to calculate $L_{qm}$ for flux excited along the q-axis only. For these calculations, it is assumed that there is no d-axis current excitation. If the core material were assumed to be infinitely permeable and not allowed to saturate, $L_{qm}$ would be equal to the round rotor inductance defined by (3.13). However, due to flux barriers that the PM cavities introduce, q-axis flux in the rotor is concentrated into the U-shaped paths between the PMs. In addition, flux in the stator...
concentrates in the teeth. The resulting flux densities in these locations are high enough to saturate the M-19 steel laminations. For this reason, a more complex model of the reluctance paths is required than that for the round rotor inductance.

The first step in developing a model for $L_{qm}$ is to define the non-linear reluctance elements in the rotor and stator. Each reluctance element takes the form

$$R_i = \frac{l_i}{\mu(B_i)A_i}$$  \hspace{1cm} (3.23)

where $l_i$ is the length of the reluctance path, $\mu(B_i)$ is the permeability of the steel at the flux density $B_i$, and $A_i$ is the cross-sectional area of the reluctance path. Figure 3.8 is the DC magnetization, or $B$-$H$, curve for M-19 steel used to solve for $\mu(B_i)$. The shaded regions in Figure 3.9 indicate the sections where the rotor steel is expected to saturate due to $q$-axis excitation. These sections are modeled by the reluctance elements $R_{ry1}$, $R_{ry2}$, and $R_{ry3}$ in Figure 3.7. Similarly, the reluctance of the stator back iron and teeth is modeled by $R_{yn}$ and $R_{tin}$ respectively. The airgap is also sectioned and modeled by linear reluctance elements $R_{gn}$. For each of these elements an average length $l_i$ and area $A_i$ is defined. The entire model of the magnetic circuit is analyzed in Matlab, which iteratively solves for the $B_i$ and thus $\mu_i$ terms for each of the non-linear reluctance elements. Once complete, the $q$-axis flux per unit excitation is known and $L_{qm}$ is directly calculated from this result.

![Figure 3.8: B-H curve for core material in LPM](image-url)
3.2.4 D-axis Inductance

Similar to $L_q$, the $d$-axis inductance $L_d$ is divided into a magnetizing inductance $L_{dm}$ and a leakage inductance $L_d$ defined in 3.2.2. $L_{dm}$ is calculated assuming that the core is infinitely permeable, the PMs have a constant remanent flux density, and the PM bridges are fully saturated and carry constant flux. For $d$-axis excitation alone, the flux in the rotor must pass through or around the PM cavities. These large magnetic barriers prevent the flux density from reaching a level that saturates the core. For this reason, an infinitely permeable core with no saturation limit is assumed. However, the PM bridges are assumed to be fully saturated by the PM flux alone. Increasing the negative $d$-axis current aids the saturation of these bridges. Because all of the reluctance elements are linear and the bridges are assumed to be fully saturated, this model predicts a constant value of $L_{dm}$ for all negative $d$-axis currents.
FEA and measured results both indicate that \( L_d \) is higher than the LPM predicts for low \( d \)-axis current and then decreases as \( I_d \) increases. This is explained by the PM bridges not fully saturating from PM flux alone. In addition, the fringing fields around the PM cavities at the ends of the rotor stack cannot be neglected.

\( L_{dm} \) is assumed to be composed of two parameters: the through and circulating inductances, calculated as ratios to the round-rotor inductance defined in 3.2.1 [9]. The magnetic circuit in Figure 3.10 is used to calculate the reluctance paths along the \( d \)-axis. All of the reluctance elements are assumed to be linear. \( R_{g1} \) and \( R_{g2} \) represent sectioned reluctances of the airgap. \( R_{m1} \) and \( R_{m2} \) represent the reluctances of the magnet cavities, assuming the bridges are fully saturated. Matlab is used to solve this circuit and calculate \( L_{dm} \).

A limitation of this LPM model is the assumption of an infinitely permeable core. While it is true that for \( d \)-axis excitation alone the core does not saturate, \( q \)-axis excitation is shown to heavily saturate the core. This presents a cross-coupling effect on \( L_{dm} \). As \( q \)-axis flux begins saturating the rotor sections shown in Figure 3.9, the reluctance of the \( d \)-axis path increases. This reduces \( L_{dm} \), and makes it a function of \( q \)-axis current. This cross-coupling is not modeled by the LPM but is present in FEA results and evident in inductances extracted from measured torque.

### 3.2.5 PM Flux linkage

![Figure 3.11: LPM magnetic circuit for calculating the PM flux linkage](image)

The final component used to predict torque is the fundamental PM flux linkage \( \lambda_{PM} \). This is modeled using the circuit shown in Figure 3.11, which is very similar to that for the \( d \)-axis inductance. The airgap and PM cavity reluctances are identical to those used for calculating \( L_{dm} \). The constant flux sources \( \phi_{m1} \) and \( \phi_{m2} \) are proportional to the PM remanent flux density. The saturated bridges are represented by constant flux sources \( \phi_{b1} \) and \( \phi_{b2} \). These sources are proportional to the estimated saturation flux density of the M-19 core material. This circuit is
solved using Matlab. The flux crossing the airgap into the stator is determined. From this, the
PM flux linkage is calculated.

3.3 LPM Predictions

The LPM predictions in this section are calculated based on the construction of the prototype
ISG, and are therefore different than the predictions in [3] which are based on the theoretical
design. A summary of the predictions is given in Table 3.1. Figure 3.12 is a plot of the predicted
$d$- and $q$-axis inductances, showing the effect of saturation in the core along the $q$-axis. A
comparison of these results with FEA and experimental results is made in Chapter 6.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>PM flux linkage</td>
<td>8.97 mWb-turns</td>
</tr>
<tr>
<td>Phase resistance</td>
<td>10.3 mΩ</td>
</tr>
<tr>
<td>Leakage inductance</td>
<td>4.1 μH</td>
</tr>
<tr>
<td>$D$-axis inductance</td>
<td>69.1 μH</td>
</tr>
</tbody>
</table>

Table 3.1: LPM predictions

3.4 Summary

This chapter defined the conventions that are used throughout this thesis and the LPMs. The
method used by the LPMs for calculating $d$- and $q$-axis inductances and PM flux linkage was
outlined. Results of the LPM calculations for the prototype machine were presented. These
parameters could then be used in (3.24) for predicting the torque produced by the ISG.

$$
\tau_e = \frac{1}{2} p (\lambda_{pm} + (L_d - L_q) I_d) I_q
$$

(3.24)
Figure 3.12: LPM inductance predictions
Chapter 4 Physical Experiments

This chapter discusses the drive stand used to test the ISG (integrated starter/generator) and the results of these tests. For each experiment, the setup and procedure is explained and the results are presented. Comparisons between the measured results, LPM (lumped-parameter model) predictions, and FEA (finite element analysis) predictions are made in Chapter 6.

4.1 Experimental Setup

![Drive stand CAD drawing](image)

Figure 4.1: Drive stand CAD drawing

4.1.1 Hardware

The CAD drawing in Figure 4.1 shows the layout of the drive stand. A T-slot base plate formed the base on which the prototype ISG and load motor are mounted. This base plate was custom made for this application by Inter-Lakes Bases in Michigan. The dimensions of the base plate are given in Figure 4.2. Before machining, the base plate weighed roughly 1500 lbs. The four T-slots are 6 inches apart and run the length of the base plate. They are designed to fit the T-slot nuts available at that time from McMaster-Carr: part number 94750A585 for 1/2"-13 threads, 94750A586 for 5/8"-11 threads. The base plate is moved by screwing four eyebolts into T-nuts at the corners of the base plate and then lifting by chains with a 1-ton crane. It rests on a rubber mat.
on the laboratory floor. The floor is uneven and the mat smoothes out the surface, as well as prevents the drive stand from moving due to vibrations.

Figure 4.2: Dimensions of the T-slot base plate

The ISG rotor is suspended by two grease-packed bearings similar to the one shown in Figure 4.3. The stationary part of each bearing is housed in a ball-in-socket joint that allows the bearing to swivel vertically and horizontally. This range is limited, but enough to accommodate a small angular misalignment. These bearings operate at the top of their speed range, thus during high-speed operation they begin to heat up. Losses due to friction in the bearings are incorporated into the measurements.

Figure 4.3: Ball bearing

The ISG stator is mounted in an aluminum housing designed by Ford Motor Company. This housing has a narrow cut at its top so that the housing can be compressed around the stator to secure it in place. The housing also has water-cooling jackets surrounding the opening for the stator, however water cooling was not used during the testing.

The rotor couples to a torquemeter through a flexible shaft coupling shown disassembled in Figure 4.4. The piece that couples the rotor and torquemeter shafts is a spring; part number 4 in the figure. The S-shaped spring weaves around the gear teeth on the two hubs, both part number 3 in the figure. Grease is packed around the hubs and spring and then enclosed by a sealed casing. An identical coupling is used between the torquemeter to the load motor. A shaft adapter is used to properly downsize the shaft on the load motor (60 mm diameter) to fit the hub of the coupling (1.5 in. diameter).

The advantage of a flexible coupling is a tolerance for planar and angular offsets. One drawback to the spring is the amount of play that exists between the position of the ISG rotor and load motor shafts. This is only an issue for static torque measurements when shaft position is set by the load motor on one side of the couplings and measured by an encoder mounted on the ISG side of the couplings.
Figure 4.4: Diagram of the flexible shaft couplings showing how spring fits to the hubs

The load motor is a 20 HP Kollmorgen permanent magnet synchronous machine, which was donated by Kollmorgen Corporation specifically for this project. The load motor is controlled by a VectorStar inverter and power supply also donated by Kollmorgen. The setup is wired according to a schematic in the manual. Figure 4.7 presents a copy of that schematic.

Figure 4.5: Danfoss industrial drive

The inverter for the ISG is a donated Danfoss industrial drive, which has been modified by Danfoss Corporation for this application. A picture of the inverter is shown in Figure 4.5. A schematic of the modified inverter is shown in Appendix D. The switching is performed by IGBTs. In an automotive environment, MOSFETs would be a much better choice, but the purpose of this thesis is to study the machine, not the inverter, so any working inverter would suit. One modification to the Danfoss drive is the replacement of the standard controller with a prototyping board that pins-out the IGBT gate signals and brake signal to fiber optic inputs. This modification retains the shoot-through and overcurrent protection in the inverter. The gate inputs
allow a dSPACE controller running a custom control algorithm to operate the inverter. The brake signal controls another IGBT that switches an external resistor bank onto the DC bus to load the inverter. A second modification to the inverter is the disconnecting of the internal 300 V DC bus, allowing the inverter to be powered from an external DC source. The DC source is a Sorensen 10 kW, 60 V, 166 A DC power supply. This power supply is capable of sourcing 7 kW at 42 V, more than enough to operate the ISG over the range of specifications. An additional capacitor bank is also connected to the DC bus, which is more realistic of the type of load the ISG will see in an automotive application. An abstract drawing of this capacitor bank and the external resistor bank is shown in Figure 4.6. A summary of the components used in the drive stand with their manufacturers and part numbers is provided in Table 4.1.

![Diagram of resistor load bank and capacitor bank](image)

<table>
<thead>
<tr>
<th>Part</th>
<th>Manufacturer</th>
<th>Part number</th>
</tr>
</thead>
<tbody>
<tr>
<td>Encoder</td>
<td>Agilent</td>
<td>HEDL-6540 J10</td>
</tr>
<tr>
<td>Current sensor</td>
<td>LEM</td>
<td>LT 500-S</td>
</tr>
<tr>
<td>Real-time controller</td>
<td>dSPACE</td>
<td>DC 1103</td>
</tr>
<tr>
<td>Bearings</td>
<td>SealMaster</td>
<td>NP-24T 1-1/2</td>
</tr>
<tr>
<td>Flexible shaft couplings</td>
<td>Falk</td>
<td>1040T20</td>
</tr>
<tr>
<td>Torquemeter</td>
<td>Himmelstein &amp; Co.</td>
<td>90-02T-(4-3)</td>
</tr>
<tr>
<td>Torquemeter display</td>
<td>Himmelstein &amp; Co.</td>
<td>66032</td>
</tr>
<tr>
<td>Load motor</td>
<td>Kollmorgen</td>
<td>V3144-BE24-004</td>
</tr>
<tr>
<td>Load motor controller</td>
<td>Kollmorgen</td>
<td>VSA28-0012-3144</td>
</tr>
<tr>
<td>Load motor power supply</td>
<td>Kollmorgen</td>
<td>PA8500</td>
</tr>
<tr>
<td>DC power supply</td>
<td>Sorensen</td>
<td>DHP60-166 M9D</td>
</tr>
<tr>
<td>ISG controller</td>
<td>Danfoss</td>
<td>VLT-5052</td>
</tr>
<tr>
<td>Scanning thermometer</td>
<td>Cole-Parmer</td>
<td>Digi-Sense 92000-00</td>
</tr>
<tr>
<td>T-slot base plate</td>
<td>Inter-Lakes Bases</td>
<td>Custom</td>
</tr>
<tr>
<td>Shaft adapter</td>
<td>Stafford</td>
<td>Custom</td>
</tr>
</tbody>
</table>

Table 4.1: List of components used in the drive stand
Figure 4.7: Kollmorgen wiring diagram
4.1.2 Automation Software

This drive stand is designed so that hands-free automated testing can be performed. Through the use of automation, each experiment is precisely repeatable and many data points can be sampled in a relatively short period of time. LabVIEW, a product of National Instruments, is the software used for data acquisition and controlling the equipment. This is done using a set of drivers designed in LabVIEW for communicating with each device over a serial or GPIB port. These drivers act as subroutines that perform the low-level execution for interfacing with an instrument. Table 4.2 is a list of the test and measurement equipment and their respective communication port and address. Each driver executes a specific task by sending the proper instructions to the equipment it interfaces with. For each instrument, there exists an instruction set and protocol for establishing communication.

<table>
<thead>
<tr>
<th>Instrument</th>
<th>Com. Port</th>
<th>Address</th>
</tr>
</thead>
<tbody>
<tr>
<td>Oscilloscope</td>
<td>GPIB</td>
<td>7</td>
</tr>
<tr>
<td>Load motor</td>
<td>Serial</td>
<td>COM 1</td>
</tr>
<tr>
<td>dSPACE</td>
<td>Internal</td>
<td>C library</td>
</tr>
<tr>
<td>Torquemeter</td>
<td>Serial</td>
<td>COM 2</td>
</tr>
<tr>
<td>Scanning thermometer</td>
<td>Serial</td>
<td>COM 3</td>
</tr>
<tr>
<td>DC power supply</td>
<td>GPIB</td>
<td>6</td>
</tr>
<tr>
<td>Multimeter</td>
<td>GPIB</td>
<td>9</td>
</tr>
<tr>
<td>Impedance analyzer</td>
<td>GPIB</td>
<td>17</td>
</tr>
</tbody>
</table>

Table 4.2: List of test and measurement equipment and their respective communication ports

The drivers for the test and measurement equipment are used in other LabVIEW programs that perform the experiments. The general function of each of the programs is to initialize the devices used in the experiment, sweep any number of variables such as speed or current, record a set of data at each of the points, perform any necessary post-processing of the data, and save the data to a file.

4.1.3 Control

The control software for the ISG implements a flux-weakening strategy having space-vector modulated gate signals that ultimately drive the IGBTs in the Danfoss inverter [4]. At motoring speeds, the controller regulates the \( d \)- and \( q \)-axis currents to the reference inputs. At high speeds, however, the current magnitude that the controller can supply is limited due to the fixed 42 V bus and the increasing amplitude of the back-EMF voltage. The current amplitude can be increased if the remanent flux density in the PMs could be decreased. Effectively, this is done by negative \( d \)-axis current. Thus, as the speed increases, the controller rotates the current vector to inject more negative \( d \)-axis current into the ISG. This reduces the \( q \)-axis voltage and increases the current magnitude that can be generated by the inverter.

The control software for the Danfoss inverter is executed by a dSPACE 1103 real-time controller board. This board is responsible for interfacing to the inverter and sensors. The three phase currents and DC bus voltage are read using 16-bit parallel ADCs in the dSPACE board. Before connecting to the dSPACE board, these signals are first conditioned by 2-pole filters with a 6.7 kHz cut-off frequency to filter ripple current and noise. The schematic for these filters is
The gate signals for the inverter are generated by pulse-width modulators (PWM) residing in the dSPACE board. The signals connect to an external board which converts them to fiber optic signals. The fiber optic signals are then routed to the Danfoss inverter gate inputs.

The shaft position is sensed with an Agilent encoder. The dSPACE board has dedicated inputs to specifically read the pulses directly from the encoder. The encoder wheel is the part of the encoder that mounts to the rotating shaft and has slots to break a light beam. This is the mechanism that the encoder uses to determine position. \( I_d \) and \( I_q \) are calculated directly from the position reading, and the flux-weakening control software is highly sensitive to any error signal in the position. In the present case, the encoder wheel is mounted off-center, and this generates an error signal in the position. This error is enough to inhibit the controller from operating above 4000 RPM. This position error is well defined though, and can be approximated as a sine wave and measured at low speed. If the equal and opposite sine wave is added to the position reading, the error is canceled in real time. This allows the control to operate up to 6000 RPM.

### 4.2 Mechanical Losses in the Drive Stand

<table>
<thead>
<tr>
<th>Data acquisition steps automated by LabVIEW</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 Initialization</td>
</tr>
<tr>
<td>Communicate with the load motor, enable the drive</td>
</tr>
<tr>
<td>2 Set the desired shaft speed with the load motor under speed control</td>
</tr>
<tr>
<td>3 Issue the <em>kill</em> command to the load motor to disable power to the drive</td>
</tr>
<tr>
<td>4 As the shaft freely spins down, record the shaft speed at fixed time intervals using the <em>vf</em> command to the load motor</td>
</tr>
<tr>
<td>5 Save the data to a file</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Post processing steps in Matlab</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 Calculate the total inertia of the shaft</td>
</tr>
<tr>
<td>2 Average the speed data to reduce the noise</td>
</tr>
<tr>
<td>3 Calculate the shaft acceleration from the averaged speed and time</td>
</tr>
<tr>
<td>4 Calculate torque from the acceleration and inertia</td>
</tr>
<tr>
<td>5 Fit a line of the form ( Ax + B ) to the torque versus average speed curve</td>
</tr>
</tbody>
</table>

Table 4.3: Spin down test procedure

The purpose of the spin down test is to measure the mechanical losses in the shaft such as drag from the bearings and windage. It is necessary to know the torque from these losses as a function of speed in order to properly correct measurements of torque while the ISG is motoring or generating. The torque measurement is taken from a torque transducer mounted between the ISG and the Kollmorgen load motor. The losses on the ISG side of the transducer ("downstream" losses) must be separated from the losses on the load motor side ("upstream" losses), and only the downstream losses are subtracted from the torque measurement when running experiments to characterize the ISG. It turns out that the upstream losses are highly dependent on temperature and dominate the downstream losses. Therefore, the results from this experiment are not used for correcting the torque measurement. Instead, the torque due to mechanical losses is directly measured with the torquemeter while the drive is spinning at constant speed and the current in the
ISG is zero. This torque is treated as an offset and is subtracted from subsequent measurements of torque at that speed. Nevertheless, the spins down results are included here because they provide a measure of the total losses in the drive stand.

The spin down test procedure is outlined in Table 4.3. The results are shown in Figure 4.8 for a test run when the bearings were warm. The bearings are warmed by running the drive at a high speed for a long period of time before taking any data. If the drive stand is cold, the torque due to losses is greater. The curve fit to the data takes the form $Ax + B$, where $A = 0.0004$ Nm/RPM and $B = 0.6582$ Nm for the results in Figure 4.8. This data indicates that the power dissipated in mechanical losses will be 1.9 kW at 6000 RPM. A spin down test, run on the load motor alone by separating the shafts, indicated that most of these losses are in the load motor. There are two conclusions that can be drawn from these results. At high speeds, a large amount of power is being dissipated somewhere in the load motor. This is most likely in the bearings, and could possibly cause overheating. Secondly, the losses in the ISG that must be separated from the torque measurement are dominated by losses in the load motor. Therefore, the ISG mechanical losses cannot be accurately measured using these spin down results.

The losses in the load motor did not always increase with speed. This behavior is most likely the result of pounding axially on the load motor's shaft during the assembly process. A thermocouple was placed near the front bearing of the load motor to detect any abnormal heating. None was ever detected, and the load motor has been operating since then.
4.3 DC Stator Resistance

In order to predict the losses in the machine and its output power capabilities, the resistance of the phases must be measured. Because the resistances are on the order of milliohms, high current is injected into the phases to permit an accurate reading of voltage with a multimeter. The procedure for measuring the resistance of each phase is outlined in Table 4.4. Each phase is measured separately, and DC current is swept from 10 to 120 A. The thermal dynamics are ignored for this test because the data is recorded much faster than the thermal time constant of the phases. The measurement of voltage in each case is made on the copper stator wires. This method does not incorporate the resistance of terminal blocks, or any of the wires that run from the inverter to the ISG.

<table>
<thead>
<tr>
<th>Data acquisition steps automated by LabVIEW</th>
</tr>
</thead>
<tbody>
<tr>
<td>1   Initialization</td>
</tr>
<tr>
<td>Communicate with the DC power supply, configure for constant current</td>
</tr>
<tr>
<td>Communicate with the multimeter, set to measure voltage</td>
</tr>
<tr>
<td>2   Command a constant current from the DC power supply into a single phase winding</td>
</tr>
<tr>
<td>3   Record the voltage on the phase terminals</td>
</tr>
<tr>
<td>4   Repeat steps 2-3 for a set of currents</td>
</tr>
<tr>
<td>5   Save the data to a file</td>
</tr>
</tbody>
</table>

Table 4.4: DC resistance test procedure

The measured resistances are summarized in Table 4.5. The important discovery to result from this experiment is that the resistance of phase C is 5% lower than either phase A or B. This is due to the routing of the end-turns. After inspecting the stator, it was found that all of the phase C end-turns route directly from one slot to another, whereas the phase A and B end-turns are bent around the phase C end-turns. The result of this winding pattern is longer circuits for phase A and B than phase C. This imbalance in resistances will cause DC current to not divide evenly in the phases. However, DC current is only applied when the ISG is stopped, and the current regulator decreases the contribution of the error. While the ISG is rotating, this imbalance is negligible.

<table>
<thead>
<tr>
<th>Phase</th>
<th>Resistance</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>8.7 mΩ</td>
</tr>
<tr>
<td>B</td>
<td>8.6 mΩ</td>
</tr>
<tr>
<td>C</td>
<td>8.3 mΩ</td>
</tr>
</tbody>
</table>

Table 4.5: Summary of measured phase resistances

4.4 Permanent Magnet Flux linkage

In order to estimate the torque in the ISG, PM flux linkage must be measured. Typically, only the fundamental component, $\lambda_{pm}$, contributes to average torque production. One way of measuring $\lambda_{pm}$ is from the open-circuit back-EMF voltage. The fundamental amplitude of the line to neutral voltage $V_{nl}$ is directly proportional to the fundamental PM flux linkage by $V_{nl} = \ldots$
Another use for the back-EMF voltage measurement is to detect demagnetization of the permanent magnets. Since $\lambda_{PM}$ is proportional to the remanent flux density of the PMs, any change in their magnetization will reflect in the back-EMF fundamental amplitude. Therefore, the back-EMF test should be run once initially to measure $\lambda_{PM}$ and then again periodically after high-stress tests to detect the presence of demagnetization. It is important to note that the PMs are not expected to demagnetize for any current vector within the rated current limit circle of 460 A$_{peak}$. Additionally, demagnetization was not observed in the prototype ISG after a maximum of 350 A$_{peak}$ along the negative $d$-axis. Another simple means of testing for demagnetization that does not require any post-processing of data is by measuring the rectified DC bus voltage versus shaft speed. This method is slightly less accurate, however, since the voltage drop across the rectifying diodes varies linearly with temperature.

Data acquisition steps automated by LabVIEW

1. Initialization
   - Communicate with the load motor, enable the drive
   - Communicate with the TDS754D oscilloscope, configure channel
2. Set the desired shaft speed with the load motor under speed control
3. Capture the back-EMF waveform on the oscilloscope five consecutive times
4. Repeat steps 2-3 for each speed
5. Save the waveforms to a file

Post-processing steps in Matlab

1. Take the FFT of each sampled waveform
2. Extract the fundamental amplitudes from the FFT results
3. Average the five fundamental amplitudes for every shaft speed point
4. Calculate a PM flux linkage at each speed point by dividing each average fundamental amplitude by the corresponding electrical frequency
5. Average the PM flux linkages across the range of speeds

Table 4.6: Back-EMF measurement test procedure

The procedure for measuring $\lambda_{PM}$ is outlined in Table 4.6. Essentially, the line to neutral voltage of any phase is sampled five times for every speed, and speed is varied from 1000 to 6000 RPM by increments of 1000 RPM. At each speed, the fundamental voltage amplitude is extracted from the five sampled waveforms using the FFT algorithm in Matlab. Then these five amplitudes are averaged, and a value of $\lambda_{PM}$ is calculated for that speed. These values of $\lambda_{PM}$ at each speed are then averaged to arrive at a single number for $\lambda_{PM}$, which can be used to predict torque.

A sample of the back-EMF voltage and fundamental is shown in Figure 4.9. This data was taken when the shaft was rotating at 4000 RPM. The fundamental was extracted as described in Table 4.6. The FFT of this waveform is shown in Figure 4.10. Note that the even harmonics are all zero, as should be the case for a symmetric machine. The five data points recorded at each speed are plotted in Figure 4.11. Note that at each speed, the five points lie on top of each other, indicating that each sampled data point is repeatable with high precision. The resulting fundamental $\lambda_{PM}$ is 8.74 mWb-turns.
Figure 4.9: Line to neutral phase voltage waveform at 4000 RPM

Figure 4.10: Harmonic content of back-EMF waveform at 4000 RPM
4.5 Inductances

The $d$- and $q$-axis inductances are measured in order to predict the torque in the ISG, and to determine the accuracy of the LPM and FEA predictions. The results are compared with LPM and FEA predictions in Chapter 6. The method used to measure the inductances is described in this section. An abandoned method of calculating inductance based on current ripple is described in Appendix A.

4.5.1 High-Current Experiment

Figure 4.12: Experimental setup for measuring inductance
The setup for measuring the inductance is shown in Figure 4.12. The 208 V\(_{\text{RMS}}\) voltage source is taken from line to line of a three-phase outlet available in the LEES laboratory. The variac can be used to continuously vary the voltage amplitude of the excitation from 0-208 V\(_{\text{RMS}}\). The 7:1 step-down transformer provides a current boost to work around the 20 A\(_{\text{RMS}}\) limit of the variac. The ISG terminals are wired with the neutrals connected together (Y-connected). The A1+ and A2+ terminals are connected together, and the B1+, B2+, C1+, and C2+ terminals are connected together. The single-phase voltage excitation is applied across the A+ to B+ and C+ points. This configuration is chosen so that \(I_b = I_c = -0.5I_a\), provided the impedance of phases B and C are matched. This mimics a three-phase current excitation, frozen at a time when \(I_a\) is at its peak value. The current in phase A is measured with a LEM sensor and the voltage applied to the ISG is measured with a differential probe at the locations indicated in Figure 4.12. The procedure for measuring these parameters and calculating inductance is outlined in Table 4.7. Because of the phase terminal connections for this experiment, the sinusoidal excitation \(V\) will drive a current vector that is always parallel with the stator phase A axis and varies only in amplitude. The rotor can then be positioned so that this current vector aligns with the rotor \(d\)- or \(q\)-axis. When the rotor \(d\)- or \(q\)-axis is aligned with the stator phase A axis, the inductance measured at the terminals of the ISG will be \(\frac{3}{2}L_d\) or \(\frac{3}{2}L_q\), respectively.

When the rotor is locked on the \(d\)- or \(q\)-axis, the \(d\)- or \(q\)-axis current resulting from the sinusoidal voltage excitation will be swept from \(-I_{\text{peak}}\) to \(+I_{\text{peak}}\), where \(I_{\text{peak}}\) is the peak current for a specific voltage excitation. When aligned on the \(q\)-axis, the measured phase current \(I\) does not appear sinusoidal due to the saturation in the core. This can be seen in the sample \(V\) and \(I\) waveforms in Figure 4.13, recorded for calculating \(L_q\).

In order to accurately calculate the inductance in the presence of saturation, flux linkage must first be calculated from

\[
\lambda_{d,q} = \frac{2}{3} \int_0^t (V - \frac{1}{2} R_a I) dt + K
\]

where \(\lambda_{d,q}\) is the \(d\)- or \(q\)-axis flux linkage depending on rotor orientation, \(V\) and \(I\) are measured as in Figure 4.12, \(R_a\) is the average phase resistance, and \(K\) is an arbitrary constant of integration subtracted during post-processing. Through integration, the saturation characteristics are traced out precisely, which can be seen in the results for \(\lambda_q\) shown in Figure 4.14 and \(\lambda_d\) shown in Figure 4.15. Note that \(\lambda_q\) is symmetric for positive \(I_q\) (motoring) and negative \(I_q\) (generating), however \(\lambda_d\) is not symmetric for positive and negative \(I_d\). The ISG will always be operating with negative \(I_d\) therefore this is the only portion of the \(\lambda_d\) curve that is important. It is interesting to note that the sharp increase in \(\lambda_d\) for positive \(I_d\) is due to the PM bridges becoming unsaturated. As \(I_d\) increases further, the PM bridges saturate in the opposite direction, and the slope of the \(\lambda_d\) curve approaches that for negative \(I_d\). Inductance is calculated using Matlab from the flux linkage by \(L_{d,q} = \lambda_{d,q} / I_{d,q}\).
Figure 4.13: V and I measurements recorded during q-axis inductance experiment

Data acquisition steps (not automated)

1. Initialization
   - Communicate with the load motor using TeraTerm, enable the drive
   - Load the dSPACE software for reading the shaft position
   - Setup the TDS754D oscilloscope
   - Configure ControlDesk to display the encoder position reading
2. Set the desired shaft position using the load motor in position control
3. Lock the shaft in place by tightening set screws that clamp onto the shaft
4. Increase the voltage excitation using the variac until the desired peak current is reached
5. Capture the voltage and current waveforms on the oscilloscope
6. Save the waveforms from the oscilloscope to a floppy disk in spreadsheet format
7. Repeat steps 2-6 for each desired shaft position and current amplitude

Post-processing steps in Matlab

1. Filter the V and I waveforms
2. Calculate flux linkage by integrating $V - R_i I$ over one electrical cycle
3. Subtract the DC offset required to center $\lambda$ on the origin
4. Average the hysteresis loop formed by plotting $\lambda$ versus $I$
5. Calculate inductance by dividing $\lambda$ by $I$

Table 4.7: Inductance measurement test procedure
Chapter 4: Physical Experiments

Figure 4.14: Measured $q$-axis flux linkage used to calculate $L_q$

Figure 4.15: Measured $d$-axis flux linkage used to calculate $L_d$
4.5.2 Q-axis Inductance Low-Current Experiment

One limitation of the single-phase experiment is at currents below approximately 50 A, the calculation of inductance becomes highly sensitive to errors in the measurements. This sensitivity affects \( L_q \) calculations more so than \( L_d \). When the ISG is generating maximum power at 6000 RPM, \( I_q \) is in this range. \( I_d \), however, will not be in this range while motoring at rated torque or generating at maximum power. Therefore, for predicting maximum torque and power, it is important to have accurate measurements of \( L_q \) below 50 A, and somewhat less important for \( L_d \).

As \( I_q \) increases from 0 to rated current, there are three distinct ranges where \( L_q \) is affected differently by the core material. When \( I_q \) is below 20 A, the permeability of the core material is low because the flux density in the core is low. This causes the inductance to drop as the current decreases to 0. When \( I_q \) is between 20 and 60 A, the permeability of the core material is very high and the \( q \)-axis reluctance path is dominated by the airgap. In this range, \( L_q \) is at its peak and is approximately independent of current. When \( I_q \) is above 60 A, the core material begins to saturate and its permeability decreases again. This causes \( L_q \) to drop as \( I_q \) is increased to rated current. For \( I_q < 60 \) A, \( L_q \) is approximated to be independent of current and its value is calculated from

\[
L_q \approx \frac{2}{3} \frac{1}{2\pi f} \left( \frac{V}{I} - \frac{1}{2} R_a \right)
\]  

(4.2)

where \( f \) is 60 Hz, \( V \) and \( I \) are measured as in Figure 4.12, and \( R_a \) is the phase resistance. The procedure for this experiment is outlined in Table 4.8.

<table>
<thead>
<tr>
<th>Data acquisition steps (not automated)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 Set the shaft position on the ( q )-axis using the load motor in position control</td>
</tr>
<tr>
<td>2 Lock the shaft in place by tightening set screws on a brace</td>
</tr>
<tr>
<td>3 Increase the voltage excitation using the variac until the desired peak current is reached</td>
</tr>
<tr>
<td>4 Measure the amplitudes of ( V ) and ( I )</td>
</tr>
<tr>
<td>5 Repeat steps 2-4 for each desired current amplitude</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Post-processing steps in Matlab</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 Calculate inductance from the equation above</td>
</tr>
</tbody>
</table>

Table 4.8: Low current \( q \)-axis inductance test procedure

4.5.3 Composite Inductance Results

The measured inductances are shown in Figure 4.16. The curve for \( L_q \) is composed of results from the low current measurement for \( 10 \) A < \( I_q \) < 60 A, and results from the integration of \( V - RI \) for \( I_q \geq 60 \) A. This is because below 50 A, the \( V - RI \) method is not accurate, and between 50 and 60 A, the results from both methods are equal and either result can be used. The curve for \( L_q \) does not extend to rated current. This is due to the voltage limitation of the setup. Because \( L_d \) is smaller than \( L_q \), \( I_d \) is larger than \( I_q \) for the same voltage excitation. Therefore, measurement of \( L_d \) is not limited.
Figure 4.16: Measured \( d \)- and \( q \)-axis inductances

4.6 Torque vs. Angle – Stationary

This experiment measures the stationary torque on the shaft produced by the ISG over a range of current vector angles. The purpose of this experiment is to determine the accuracy of the FEA predictions of torque ripple. The results are compared to FEA predictions in Chapter 6. The experimental setup is shown in Figure 4.17, and the procedure is outlined in Table 4.9. The ISG terminals are connected the same way they were for the inductance experiments. When the current source is turned on, the DC current in the ISG simulates a three-phase current excitation frozen at a time when \( I_A \) is at its peak.
During an experiment, the rotor is moved to any position and held there by the load motor in a position control loop. This has the advantage of allowing for complete automation of the experiment, but the disadvantage of sloppy position control. There are two flexible-shaft couplings between the load motor and the ISG rotor. These couplings are described in Section 4.1.1. They couple the shafts with a spring that distorts when torque is applied. Therefore, the ISG rotor position is offset from the load motor position when there is torque on the shaft. The position recorded during the experiment is taken from the encoder that mounts directly to the ISG rotor, therefore the position reading is accurate.

**Data acquisition steps automated by LabVIEW**

1. Initialization
   - Communicate with the load motor, enable the drive
   - Communicate with the torquemeter
   - Communicate with the DC power supply, configure for constant current
2. Set the output current limit of the DC power supply to the desired amount
3. Set the desired shaft position using the load motor in position control
4. Wait two seconds for the shaft to settle to its final position
5. Read the torque and shaft position
6. Repeat steps 3-5 over the desired range of positions
7. Write the data to a file

**Table 4.9: Torque versus angle measurement test procedure**
The play in the couplings only becomes a problem when attempting to measure torque near the positive or negative $d$-axes, which are unstable equilibria. This is apparent in the results shown in Figure 4.18. As the rotor position is stepped onto an unstable equilibrium, the torque changes polarity and the play in the couplings allows the rotor shaft to "swing through." The bandwidth of the torquemeter is not high enough to be able to capture the detail of the torque as the rotor is moving through the unstable equilibrium. Further, even if the torquemeter were instantaneously accurate, the value it would read would not be electromagnetic torque. The rotor eventually comes to a rest at a new position, and the torque is recorded there. Thus, there are windows in the stationary torque versus angle curve where data cannot be taken. This limitation only applies to the stationary torque versus angle experiment and does not affect any of the other experiments where torque and position are measured.

4.7 Motoring

The purpose of the motoring experiment is to verify the torque predicted by FEA and LPM is correct at low speeds and high currents, and to characterize the losses in the ISG. The results are compared with predictions from LPM and FEA inductances in Chapter 6. The experiment tests the functionality of the ISG as a starter motor, and its ability to deliver high torque for a short period of time. The ISG has not been tested up to rated torque, however. This is because the Danfoss inverter used to drive the ISG is current limited to $350 \text{ A}_{\text{peak}}$, which is $76\%$ of the rated current for the ISG, and corresponds to $63\%$ of the rated torque. The IGBTs in the inverter are made by Fuji Electric; part number $2MBI\ 300N-060$. These IGBTs are rated for a collector current of $300 \text{ A}$ continuous, and $600 \text{ A}$ pulsed for $1 \text{ ms}$. Some suggested future work includes increasing the current limit imposed by the Danfoss inverter to allow testing at higher current; see Chapter 7.

For every current magnitude, there is a specific current vector angle at which the ISG delivers the maximum torque at that magnitude. These currents and angles trace out a maximum torque per Amp trajectory. At motoring speeds, the ISG is not voltage limited and therefore capable of operating at any point along the trajectory. This trajectory is predicted from the LPM and FEA results, and is measured experimentally using automated testing; the procedure for which is outlined in Table 4.10. The ISG speed is held constant by the load motor, and the current vector magnitude and angle is swept over a range of values in the motoring quadrant. This range includes the predicted maximum torque per Amp trajectory. For each current magnitude, as the current angle is swept over the range, a maximum torque is recorded. The current magnitude and angle at the maximum torque point correspond to a single data point on the maximum torque per Amp trajectory. These steps are then repeated over a range of current magnitudes and shaft speeds. This experiment was run at $50 \text{ RPM}, 100 \text{ RPM},$ and $125 \text{ RPM},$ with current magnitudes sweeping from $50 \text{ A}$ to $350 \text{ A}$. The results are shown in Figure 4.19. The data points clearly trace out a maximum torque per Amp trajectory. A solid line has been fit to the data, which can be used in the controller to generate the angle for maximum torque at a commanded current magnitude. See Appendix B for a table of the data used to generate this curve.
Data acquisition steps automated by LabVIEW

1. Initialization
   - Communicate with the load motor, enable the drive
   - Communicate with the torque meter
   - Communicate with the DC power supply, configure for constant voltage
   - Communicate with the TDS754D oscilloscope
   - Load the flux-weakening control software on the dSPACE board
   - Communicate with the scanning thermometer

2. Start the shaft spinning at the desired speed with the load motor under speed control

3. Command a current magnitude (0-350 $A_{\text{peak}}$) and angle (90-180 degrees)

4. Wait 2 seconds for and transients to settle

5. Measure the DC bus voltage, current, and power from the Sorensen power supply

6. Measure the shaft speed from the load motor

7. Measure the torque from the torque meter

8. Read $V_{ac}$ and $V_{bc}$ line-line voltage waveforms and $I_a$ and $I_b$ current waveforms from the oscilloscope

9. Calculate the average three-phase power from the measured waveforms:

   $$P_{\text{avg}} = \frac{1}{T} \int_0^T (V_{ac} I_a + V_{bc} I_b) \, dt$$

10. Measure the stator winding temperature (if $> 95^\circ\text{C}$ pause to cool down)

11. Repeat steps 3-10 over a range of current magnitudes and angles

12. Write the data to a file

Table 4.10: Motoring measurement test procedure

The power flow in the ISG at 50 RPM while current is increased to 350 $A_{\text{peak}}$ is plotted in Figure 4.20. The dark gray area represents the shaft power, calculated from measured torque and speed. The medium gray area represents the $I^2R$ losses, calculated based on commanded current and phase resistance, and corrected for measured temperature. The light gray area represents the unassigned losses. These losses vary linearly with $I^2$, indicating that this discrepancy is due to an error either in the resistance or measured temperature. The line at the top of the graph represents the three-phase power, calculated as described in step 9 of Table 4.10. This data indicates that while motoring, all of the losses are dissipated in the stator resistance and therefore can be predicted as accurately as the resistance and temperature is known.

A second experiment was run to measure the torque versus speed curve for the ISG operating at a point on the maximum torque per Amp trajectory at 300 $A_{\text{peak}}$. The results are plotted in Figure 4.21. This data is used to verify the LPM and FEA predictions in Chapter 6. The setup and data acquisition for this experiment are identical to that outlined in Table 4.10, however for this experiment the current magnitude is fixed at 300 A, the angle is set by the maximum torque per Amp trajectory, and speed is varied. The speed at which the ISG can no longer sustain the maximum torque corresponds to the ISG becoming voltage limited for 300 $A_{\text{peak}}$. At this speed and beyond, the flux-weakening strategy in the controller must rotate the current vector in order to reduce the back-EMF voltage. From Figure 4.21, the limiting speed is just above 800 RPM. Test points above this speed correspond to 300 $A_{\text{peak}}$ at the angle set by the controller; these points are not on the maximum torque per Amp trajectory.
Chapter 4: Physical Experiments

Figure 4.19: Maximum torque per Amp trajectory curve fit to data from three speeds

Figure 4.20: Sample power flow at 50 RPM showing shaft power and losses
4.8 Generating

The purpose of the generating tests is to measure the maximum output power and efficiency of the ISG over the generating speed range. The results are compared with predictions from LPM and FEA inductances in Chapter 6.

![Graph showing maximum torque at 300 A_peak](image)

**Figure 4.21:** Maximum torque at 300 A_peak

The setup for the generating tests is shown in Figure 4.22. This setup uses a novel technique to provide a continuously variable load for the ISG. The dSPACE software for the ISG can be operated as a current controller. This is the mode used for the motoring tests. The difference between the generating and motoring tests lies in the angle of the commanded current vector. While generating, this vector is in the third quadrant; i.e., $I_d < 0$ and $I_q < 0$. In addition, at high speeds the current vector will be limited in range by voltage. This will force the vector to deviate from the maximum torque per Amp trajectory. The amount of power that the ISG generates is

![Experimental setup for generating test](image)

**Figure 4.22:** Experimental setup for generating test
dependent on the shaft speed and the magnitude and angle of this current vector. Therefore, as the vector is swept over a range of values, the output power will continuously vary.

In order to accommodate the varying power and keep the DC bus at 42 V, the bus is regulated by an external DC power supply with a 6 kW resistor bank connected across the bus. When the ISG is generating no power, the DC power supply will provide all the power to the 6 kW load and to the losses in the ISG. When the ISG is generating any amount of power between 0 and 6 kW, the DC power supply will automatically reduce its output current so that the bus remains at 42 V. In this configuration, the load bank always dissipates 6 kW, regardless of the power out of the ISG. The only limitation of this setup occurs when the ISG and resistor bank dissipate a combined power that is greater than the DC power supply can output. In this event, the bus voltage will sag, however the ISG is not functioning as a generator and this result is uninteresting for the purpose of the generating experiments.

---

**Data acquisition steps automated by LabVIEW**

1. **Initialization**
   - Communicate with the load motor, enable the drive
   - Communicate with the torquemeter
   - Communicate with the DC power supply, configure for constant voltage
   - Communicate with the TDS754D oscilloscope
   - Load the flux-weakening control software on the dSPACE board
   - Communicate with the scanning thermometer
   - Turn on the brake in the inverter to connect the 6 kW load bank to the DC bus

2. **Start the shaft spinning at the desired speed with the load motor under speed control**
   - If the speed is greater than 4000 RPM, –20 A<sub>peak</sub> of d-axis current is applied to keep the back-EMF from increasing the DC bus voltage

3. **Command a current magnitude (0-350 A<sub>peak</sub>) and angle (180-270 degrees)**

4. **Wait 2 seconds for any transients to settle**

5. **Measure the DC bus voltage, current, and power from the Sorensen power supply**

6. **Measure the shaft speed from the load motor**

7. **Measure the torque from the torquemeter**

8. **Read V<sub>ac</sub> and V<sub>bc</sub> line-line voltage waveforms and I<sub>a</sub> and I<sub>b</sub> current waveforms from the oscilloscope**

9. **Calculate the average three-phase power from the measured waveforms:**

\[
P_{avg} = \frac{1}{T} \int_{0}^{T} (V_{ac}I_{a} + V_{bc}I_{b}) \cdot dt
\]

10. **Measure the stator winding temperature, if > 95 °C pause to cool down**

11. **Repeat steps 3-10 over a range of current magnitudes and angles**

12. **Write the data to a file**

---

**Table 4.11: Generating measurement test procedure**

The experimental procedure is similar to that for the motoring tests, and is outlined in Table 4.11. At a constant speed, the current vector magnitude and angle is swept over a range of values. Since power is the output parameter of interest during generating, the maximum power point is determined during post-processing, as opposed to the maximum torque point for motoring. The maximum power points are plotted for the speeds at which the ISG was tested in Figure 4.23.
The dark gray area represents the three-phase power, calculated as described in step 9 of Table 4.11. The medium gray area represents the $\dot{I}R$ losses, calculated based on commanded current and phase resistance, and corrected for measured temperature. The light gray area represents the unassigned losses, which include core losses and eddy currents. The line at the top of the graph represents the shaft power, calculated from measured torque and speed. The DC bus voltage was regulated to 42 V for these experiments. The IGBTs used in the inverter have a larger on-voltage than the MOSFETs that would likely be used in an automobile. Therefore, the power reported here may be artificially lower than the maximum power achievable with a MOSFET inverter.

The efficiency is calculated from the measurements of shaft power and three-phase power. A common model used for estimating the core losses is

$$P_{core} = M_P \cdot f \cdot B^{ef}$$

where $M$ is the mass of the core, $P_{base} = 2.25$ W/g, $f$ is the fundamental electrical frequency, $ef = 1.54$, $B$ is the peak flux density in the core, and $eb = 1.86$. This calculation resulted in an overestimate of the total losses when applied to the power results in Figure 4.23. Therefore, a more accurate model of the losses is required to predict the ISG efficiency.

As mentioned earlier, the current trajectory does not follow the maximum torque per Amp trajectory because it is limited by an ellipse due to the voltage limit that shrinks with increasing speed [4]. This is evident in the data plotted in Figure 4.24. These current vectors correspond to the currents in the ISG recorded at the points of maximum power plotted in Figure 4.23. When the dSPACE controller is regulating the DC bus, the current vector magnitude is set by the
feedback control and the angle is adjusted automatically by the flux-weakening control strategy [4]. Therefore, these points are for experimental verification only and not useful in determining a maximum power per Amp trajectory. In addition, the current measurements fed back to the controller are filtered with a filter that adds significant phase to the current waveforms above 4000 RPM. This translates into a counter-clockwise rotation of the vector by the same phase angle. This error is discussed further in Chapter 6.

![Figure 4.24: Current trajectory for maximum output power and varying speed](image)

**4.9 Thermal Model**

The thermal model of the ISG in the drive stand is not intended to be representative of the ISG located in an engine compartment. The thermal capacity of the ISG in this setup is much greater than what can be expected in an automobile: the ambient temperature of the lab is around 25°C, the aluminum fixture and base plate act as very large heat sinks for the stator. The thermal dynamics are studied in order to predict the temperature response of the ISG when being tested in the drive stand at rated current. Also, to characterize the heat transfer from the stator to the aluminum fixture, which should not change much when the ISG is mounted in an automobile.

An approximate thermal model for the ISG as it is mounted in the drive stand is shown in Figure 4.25. The thermal resistance from the stator to ambient is assumed to be larger than the initial thermal impedance of the drive stand, and has been neglected. The thermal resistance of the stator to the test fixture is modeled by $R_{sf}$, and the heat capacity of the stator is $C_s$. The thermal resistance of the test fixture to ambient is modeled by $R_{fa}$, and the heat capacity of the test
fixation is $C_f$. The assumption made when calculating these parameters is that the thermal time constant of the stator, $R_s C_s$, is much faster than the time constant of the drive stand, $R_{fa} C_f$. The result is $T_w$ will increase rapidly while $C_s$ is charging, and then slowly ramp up as $C_f$ begins to charge. The temperature rise per kW as a function of time is plotted in Figure 4.26. This data is from a test run with a current excitation of 250 A_{peak}, corresponding to an average power of roughly 800 W. The exponential temperature rise indicates that the assumptions made for the thermal model are valid for this time interval. The values for the elements in the model, approximated from this data, are $R_{sf} = 62 \degree C/kW$ and $C_s = 2.5 \text{ kW-s}/\degree C$. The time constant is 155 seconds.

![Simplified thermal model of the ISG in the MIT drive stand](image)

Figure 4.25: Simplified thermal model of the ISG in the MIT drive stand

![Graph showing temperature rise divided by power](image)

Figure 4.26: Temperature rise per kW of the stator windings
Data acquisition steps automated by LabVIEW

1. Initialization
   - Communicate with the load motor, enable the drive
   - Communicate with the DC power supply, configure for constant voltage
   - Load the flux-weakening control software on the dSPACE board
   - Communicate with the scanning thermometer
2. Start the shaft spinning at the 100 RPM with the load motor under speed control
3. Command a current vector of 250 A_{peak} and 180 degrees
4. Record temperature of the windings $T_w$ and of the fixture $T_f$ at fixed intervals for the desired period of time
5. Write the data to a file

Post-processing steps in Matlab

1. Subtract $T_f$ from $T_w$ to get the temperature difference across the stator thermal resistance
2. Use $T_w$ to adjust $R_a$ from its value at 25°C by $R_{tw} = R_{25°C}(1 + 0.00385(T_w - 25°C))$
3. Calculate $I^2R$ losses by $P_{12R} = \frac{1}{2}I_{mag}^2R_a$
4. Divide the difference in temperature by $P_{IGB}$

<table>
<thead>
<tr>
<th>Table 4.12: Thermal experiment test procedure</th>
</tr>
</thead>
</table>

4.10 Summary

This chapter described the experimental setup and all of the components used to run experiments on the ISG. The encoder mounted on the end of the ISG rotor required the most attention in alignment. Due to poor alignment of the encoder wheel, a repetitive error in position is injecting noise into the control at the mechanical frequency that caused it to fail above 4000 RPM. This is fixed by mapping out this error versus position and subtracting it from the position measurement in the control software. After this, the control works satisfactorily up to 6000 RPM.

This chapter also described all of the setups and procedures for the experiments used to characterize the ISG. Results from each experiment were presented, however these results are not compared with LPM and FEA predictions until Chapter 6. Two methods for measuring the inductance were presented: a high-current and low-current method. The $q$-axis inductance is composed of results from the high and low-current methods, however $d$-axis inductance was only measured with the high-current method. This is because it is desirable to know the $q$-axis inductance at lower currents, however $d$-axis inductance at low currents is not as important.

The motoring experiment is limited to 350 A_{peak} because of current limit protection in the Danfoss inverter. For this reason, the ISG could only be tested to 63% of rated torque while motoring. Losses while motoring are dominated by $I^2R$ losses in the stator windings. The generating experiment is limited by the voltage drop across the IGBTs in the inverter. Results of the output power and efficiency were presented up to 6000 RPM. A simplified thermal model of the stator and drive stand was also presented. This thermal model is specific to the setup at MIT, however the thermal impedance from the windings to the test fixture should be similar to that when the ISG is mounted in an automobile.
Chapter 5 Numerical Experiments

This chapter discusses the simulations that are run with MagNet, a program that uses finite element analysis (FEA) to analyze 2D magnetic systems. These simulations generate FEA predictions of the $d$- and $q$-axis inductances, permanent magnet (PM) flux linkage, torque ripple, and cross-coupling effects. The MagNet model used for these simulation is described, as well as the differences between it and the model used in [3].

5.1 MagNet Model

The results in this chapter are generated from static FEA analyses of the two-dimensional model of the ISG (integrated starter/generator) shown in Figure 5.1. This model is an updated version of the model used to generate FEA results in [3]. The following five changes were made in the updated version:

- Increased the airgap from 0.635 to 0.650 mm, based on the prototype airgap measurement
- Increased the PM relative permeability from 1.00 to 1.05, based on measurements of $\mu$, made on sample magnet pieces
- Used the B-H curve shown in Figure 5.2, a more accurate model of the core material
- Changed the stator windings to reflect the basket wind described in Chapter 2
- Reduced the active length to reflect the 97% lamination packing factor

![Figure 5.2: B-H curve for the core material in the MagNet model](image)

---

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>PM permeability</td>
<td>1.05 $\mu_r$</td>
</tr>
<tr>
<td>PM flux density</td>
<td>0.2772 T</td>
</tr>
<tr>
<td>Airgap</td>
<td>0.650 mm</td>
</tr>
<tr>
<td>Leakage inductance</td>
<td>5 $\mu$H</td>
</tr>
<tr>
<td>PM fringing inductance</td>
<td>9 $\mu$H</td>
</tr>
<tr>
<td>Active length</td>
<td>58.2 mm</td>
</tr>
</tbody>
</table>

Table 5.1: Parameters used in MagNet model

The MagNet model is of a single pole with repeating boundaries on each side that form a complete machine. Material properties such as PM relative permeability, PM remnant flux density, and the $B-H$ curve of the core material can be arbitrarily set for a given simulation. This model has the capability of setting the rotor angle between 0 and 30 mechanical degrees, and setting the current vector to any arbitrary magnitude and angle. The nominal airgap is 0.635 mm, but the model can shift the rotor small distances along the radius to simulate a slightly larger airgap. The simulation is 2D only, therefore fringing and end-turn leakage inductance are not calculated by MagNet, and so are later added to the FEA results. The active length of the machine must be specified, but it is treated by MagNet as a scalar. A wide range of simulations can be run to characterize the ISG's dependence on each of these parameters. Table 5.1 is a summary of the parameters used in the simulations described in this chapter. Figure 5.2 is the $B-$
Chapter 5: Numerical Experiments

$H$ curve used for the core material. This is the same curve that is used for the lumped-parameter model (LPM) predictions.

### 5.2 Permanent Magnet Flux linkage

The PM flux linkage term $\lambda_{PM}$ is defined as the fundamental $d$-axis flux linkage when $I_d = I_q = 0$. The FEA simulations predict a single point value of PM flux linkage at a given rotor angle with respect to the stator phase $A$ axis. This value incorporates the harmonics of PM flux linkage due to the short-pitched windings and slot ripple. $\lambda_{PM}$ is calculated from FEA results by extracting the fundamental amplitude from the PM flux linkage waveform plotted as a function of electrical angle, shown in Figure 5.3. There is a second-order effect of slot-ripple on PM flux linkage that is not shown in the figure, but causes the amplitude of the waveform to vary. It was found experimentally that the plot in Figure 5.3 is the minimum predicted PM flux linkage. The fundamental amplitude from this plot is $8.32$ mWb-turns. At the maximum PM flux linkage, the fundamental is $8.48$ mWb-turns. The value used for $\lambda_{PM}$ is the average of these two extremes, which is $8.41$ mWb-turns.

![Simulated PM flux linkage and the resulting fundamental amplitude, $\lambda_{PM}$](image)

Figure 5.3: Simulated PM flux linkage and the resulting fundamental amplitude, $\lambda_{PM}$

### 5.3 Inductances

Two separate simulations are run to calculate the $d$- and $q$-axis inductances. The rotor position is fixed at 0 mechanical degrees for each simulation. The inputs to the MagNet model
are a current vector magnitude and electrical angle. To calculate \( L_d \), the magnitude is swept from 10 – 460 A
peak, and the angle is set to 0 degrees. \( L_q \) is calculated similarly, however the angle is set to 90 degrees. DC currents are then injected into the phase windings at the desired magnitude and angle. Section 5.4 discusses the effects of rotor position on the inductances. The separate \( d \)- and \( q \)-axis current excitations mimic the LPM predictions and the static experimental measurement setup in which the cross axis is not excited.

MagNet returns the flux linked by each phase, which is transformed to \( d \)- and \( q \)-axis quantities using the \( dq0 \) transformation (3.1). The inductances are then calculated by

\[
L_d = (\lambda_d - \lambda_{PM}) / I_d + L_{end} \tag{5.1}
\]
\[
L_q = \lambda_q / I_q + L_{end} + L_{fringe} \tag{5.2}
\]

where \( \lambda_d \) and \( \lambda_q \) are the \( d \)- and \( q \)-axis flux linkages and \( \lambda_{PM} \) is the PM flux linkage, all of which are calculated from MagNet simulations, \( I_d \) and \( I_q \) are the \( d \)- and \( q \)-axis currents, and \( L_{end} \) and \( L_{fringe} \) are the end-turn and PM fringing leakage inductances defined in Section 3.2.2. \( L_{end} \) and \( L_{fringe} \) are 3D effects and therefore not calculated by the FEA simulations. They are added as a post-processing step. For the \( d \)- and \( q \)-axis inductance results shown in Figure 5.4, \( L_{end} = 5 \) \( \mu \)H and \( L_{fringe} = 9 \) \( \mu \)H.
In Figure 5.4, the \( q \)-axis inductance saturates as expected. The saturation is mainly due to the concentration of flux in the stator teeth and paths between the magnet cavities. The \( d \)-axis inductance shows a small amount of saturation due to the PM bridges. The LPM for \( L_d \) assumes that the bridges are fully saturated by PM flux alone (Section 3.2.4). However, the MagNet results indicate that PM flux only saturates the bridges at their thinnest points and the broader sections remain unsaturated. Negative \( I_d \) aids the saturation of the bridges.

### 5.4 Torque Ripple

Ideally, \( L_d, L_q \), and \( \lambda_{PM} \) should be constant with respect to the position of the rotor. However, due to the presence of stator slotting and the pitching of the stator windings, these quantities will vary with rotor position. These variations translate into torque ripple as the rotor spins. This behavior is analyzed using FEA.

The ripple effects on the inductances are shown in Figure 5.5. The solid lines are the same inductance predictions given in Figure 5.4. The error bars at 50 A increments represent the maximum change in inductances due to the ripple effect.

![Figure 5.5: Inductance ripple predicted by MagNet](image-url)

The effect that these variations in inductances and PM flux linkage have on torque is shown by the simulation results in Figure 5.6. For this simulation, the magnitude of the current vector is fixed at 460 A\(_{\text{peak}}\). The current angle is varied from 90 electrical degrees (\( I_d = 0 \) A, \( I_q = 460 \) A) to
180 electrical degrees ($I_d = -460$ A, $I_q = 0$ A) in 5-degree increments. At each current angle increment, the rotor angle is then varied from 0 to 15 mechanical degrees in steps of 2.5 degrees. MagNet calculates the torque on the shaft at every point. The solid line in Figure 5.6 represents the torque at a particular current angle, averaged over the range of rotor angles. The error bars represent the range of torques measured over the range of rotor angles.

When the ISG is operating as a starter, it is required to start the engine from a stop. If the controller commands rated current at the angle that produces maximum torque, there is a possibility that the initial predicted torque on the shaft could be as low as 136 Nm or as high as 158 Nm, depending on the resting position of the rotor and the complexity of the control algorithm (the specification is 150 Nm).

![Figure 5.6: Torque ripple at rated current (460 $A_{\text{peak}}$)](image)

**5.5 Cross-Coupling**

The effects of cross-coupling are briefly discussed in Section 3.2.5. Cross-coupling refers to the effect of $q$-axis flux on the $d$-axis inductance and $d$-axis flux on the $q$-axis inductance. The assumption made for the LPMs is that cross-coupling flux will not affect the inductances. Figure 5.7 is a plot of the predicted inductances with cross-coupling taken into account. The two curves represent the extremes of the cross-coupled inductances predicted by MagNet. On one extreme are the solid lines representing the $d$- and $q$-axis inductances without any cross-coupling effects. These solid lines are the same as in Figure 5.4. The other extreme represents the inductances...
with maximum cross-coupling effects. This maximum is limited by the rated current $I_o$ of the machine. The inductances will be within these ranges during normal operation.

For the cross-coupling simulation, the $d$- and $q$-axis currents are related by

$$ I_o^2 = I_d^2 + I_q^2. \tag{5.3} $$

When simulating the effect of $d$-axis current on $q$-axis inductance, $I_q$ is swept from 10 A to 460 A (rated current) in 10 A increments and $I_d$ is calculated from

$$ I_d = \sqrt{I_o^2 - I_q^2}. \tag{5.4} $$

for each value of $I_q$. Therefore, when $I_q$ is low, $I_d$ is high and cross-coupling effects on $L_q$ are the highest. When $I_q$ is at rated current, $I_d = 0$ and there is no cross-coupling. This explains why the curves taper and meet at rated current. A similar argument holds for the cross-coupling effects on $L_d$.

![Graph showing cross-coupling effects on inductance predictions](image)

**Figure 5.7: Cross-coupling effects on inductance predictions**

The $d$-axis inductance in Figure 5.7 becomes negative for low $I_d$ and high $I_q$. The underlying cause for this is $I_q$ generating positive $d$-axis flux, aiding the PM flux. PM flux linkage $\lambda_{PM}$ is assumed to be constant and measured in MagNet when $I_d = I_q = 0$. When calculating $L_d$, the $d$-axis flux linkage $\lambda_d$ is measured for a given $I_d$ and $I_q$ excitation. For small $I_d$ and large $I_q$, $\lambda_d > \lambda_{PM}$. Since $I_d < 0$ for all $I_q$, $L_d = (\lambda_d - \lambda_{PM})/I_d + L_{end} + L_{fringe} < 0$. When substituted the
inductances and currents into the equation for torque, this negative inductance gives the correct result.

The contour maps in Figure 5.8 and Figure 5.9 are MagNet predictions of how the inductances vary for any current magnitude and angle. From these plots, the inductances can be approximated for any $I_d$ and $I_q$ operating point.

![Contour map of $L_d$](image)

**Figure 5.8: Contour map of $L_d$**

### 5.6 Summary

This chapter described the MagNet FEA simulations that characterize the ISG. The results for $d$-axis inductance indicate that the PM bridges in the rotor do not fully saturate until an appreciable amount of negative $d$-axis current is applied. The range of torque ripple at the point of maximum torque is from 136 to 158 Nm. Cross-coupling has a noticeable effect on the inductances. This must be taken into account for an accurate prediction of the torque produced by the ISG.
Figure 5.9: Contour map of $L_q$
This chapter compares the various measured parameters to predictions from lumped-parameter models (LPM) and finite element analysis (FEA). The impact of errors from the measured to predicted values on the torque and output power are also analyzed. In addition to the work completed for this thesis, identical prototype machines were analyzed at Ford Motor Company and the University of Wisconsin – Madison. See [7] for a comparison of collective results from this thesis and the additional test facilities.

6.1 PM Flux linkage

The fundamental back-EMF voltages in Figure 6.1 are calculated from the PM (permanent magnet) flux linkages in Table 6.1. The LPM result is a prediction of the fundamental PM flux linkage. The measured value is calculated from back-EMF voltage waveforms, sampled at speeds between 500 and 6000 RPM. The FEA prediction is the result of a simulation that calculates PM flux linkage as a function of electrical angle, from which the fundamental component is extracted. These results indicate that the LPM and FEA predictions for $\lambda_{pm}$ are accurate. Had the effects of fringing fields around the PM cavities and the details of the PM bridges saturating been factored into the LPM, this prediction would be lower.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Fundamental</th>
<th>Percent error</th>
</tr>
</thead>
<tbody>
<tr>
<td>FEA</td>
<td>8.41 mWb-turns</td>
<td>-3.8 %</td>
</tr>
<tr>
<td>LPM</td>
<td>8.97 mWb-turns</td>
<td>2.6 %</td>
</tr>
<tr>
<td>Measured</td>
<td>8.74 mWb-turns</td>
<td></td>
</tr>
</tbody>
</table>

Table 6.1: Comparison of PM flux linkages

6.2 Inductance

A comparison of measured inductances to results predicted by FEA and LPMs is shown in Figure 6.2. Cross-coupling effects are not incorporated in any of these plots because the cross currents are zero in all cases. The FEA solver is 2D and cannot predict end-turn leakage or fringing at the ends of the stack. Therefore, 5 $\mu$H of end-turn leakage has been added to the $L_d$ and $L_q$ FEA results, and 9 $\mu$H of fringing leakage around the PM cavities has been added to $L_d$, as a post-processing step. These leakage terms are included in Figure 6.2.
The \( q \)-axis inductance predictions show good matching with the experimental results. FEA experiments determined that \( L_q \) is sensitive to the airgap length for \( I_q < 100 \) A. The laser cutting process used to manufacture the laminations, defining the airgap in the prototype machine, introduces a \( \pm 7\% \) error in the overall airgap length. This error is enough to bring the low current portion of the FEA curve in-line with measured \( L_q \). For \( I_q > 100 \) A, FEA experiments have shown that \( L_q \) depends mainly on the \( B-H \) curve of the core material. The curve used in the FEA model is taken from a datasheet for M19 steel laminations, which has an undefined tolerance associated with it. The effect of slot ripple on FEA predictions is analyzed in Section 5.4, and represents a \( \pm 1.5\% \) error in \( L_q \) and \( \pm 5\% \) error in \( L_d \). This tolerance can be assumed for the measured inductances as well, because the rotor is locked at a position during the experiment, and the position is not known to better than \( \pm 1 \) electrical degree. The LPM predictions for \( L_q \) are closer to the measured results than FEA in spite of the inaccuracies in the airgap and \( B-H \) material. The offset in \( L_q \) between LPM predictions and measured results could be due to an over-estimate of the LPM leakage terms, or slot-ripple affecting the measurement of inductance.

![Figure 6.1: Comparison of fundamental peak back-EMF phase voltage](image)

The \( d \)-axis inductances are also compared in Figure 6.2. The LPM inductance is constant because of the assumption that the PM bridges are fully saturated and the core is infinitely permeable. The increase in \( L_d \) at low \( I_d \) in the FEA prediction and measured inductance indicates that the PM bridges are not fully saturated by PM flux alone, and this detail should be included in the LPM predictions. The FEA results alone predict a nearly constant offset from measured \( L_d \).
Therefore, when a 9 \( \mu \)H \( L_{\text{fringe}} \) leakage inductance (defined in Section 3.2.2) is added to \( L_d \) alone, FEA and measured \( L_d \) lie nearly on top of each other. FEA experiments also indicate that airgap and \( B-H \) curve inaccuracies do not affect \( L_d \) like they do \( L_q \). The results shown in Figure 6.2 include 5 \( \mu \)H for \( L_{\text{end}} \) and 9 \( \mu \)H for \( L_{\text{fringe}} \). The data used to generate this figure is included in Appendix B.

![Figure 6.2: Comparison of inductance measurements without cross-coupling]

**6.3 Torque versus Control Angle**

The stationary torque versus control angle measurements are compared with FEA predictions in Figure 6.3. Note how well the slot ripple detail of the measurements is matched by FEA. The maximum torque predicted is 13% higher than measured. The error in maximum torque is due to FEA not incorporating \( L_{\text{fringe}} \); the leakage inductance due to fringing around the PM cavities defined in Section 3.2.2. This emphasizes the importance of a 3D FEA solution. This figure demonstrates that the torque ripple is predicted by FEA with a relatively high degree of precision.
6.4 Motoring

The graph in Figure 6.4 is a comparison of the measured and predicted torque over a range of current magnitudes and angles while motoring at 125 RPM. Predicted torque is calculated from \( \tau_e = 1.5p(\lambda_{pM} + (L_d - L_q)I_d)I_q \), where \( p \) is the number of pole pairs. The LPM torque curve is predicted using the LPM inductances from Figure 6.2. The Measured \( \lambda \) torque curve is predicted using the inductances measured from the prototype ISG, also plotted in Figure 6.2. Neither of these predictions incorporates cross-coupling effects because the underlying data was collected in the absence of cross currents. The FEA cross-coupled torque curve in Figure 6.4 represents the best prediction of the torque. This prediction uses a combination of cross-coupled inductances calculated by FEA (Section 5.5), with a fringing leakage term of 9 \( \mu \)H added to \( L_d \) alone. \( L_d \) and \( L_q \) are therefore functions of both \( I_d \) and \( I_q \).

The predicted effect of cross-coupling on \( L_q \) is not as severe as \( L_d \). If the cross-coupling effect on \( L_q \) is ignored, the error in predicted torque is not much greater. This assumption simplifies the analysis of the ISG, and the LPM prediction of \( L_q \) can be used instead of cross-coupled data. Therefore, the existing LPM for \( L_q \) can be used without modification. Only the LPM for \( L_q \) must be refined to incorporate cross-coupling, leakage due to fringing, and the effect of the PM bridges.
A torque versus speed plot is shown in Figure 6.5. The current vector amplitude is 300 $A_{\text{peak}}$ and, at speeds up to 800 RPM, the commanded angle is chosen from Figure 4.19, the maximum torque per Amp trajectory. As the speed increases beyond 800 RPM, the amplitude stays at 300 $A_{\text{peak}}$, but the angle is rotated towards the negative $d$-axis by the flux-weakening strategy [4]. The results are compared with predictions from the same inductances used for predicting the torque curves in Figure 6.4. The cross-coupled FEA results, incorporating 9 $\mu$H of $L_{\text{fringe}}$ leakage inductance on the $d$-axis, again provide the best prediction of the torque. And just as before, if cross-coupling effects on $L_q$ are ignored, the change in the predictions for torque is minimal.

### 6.5 Generating

There are many complications that reduce the precision of the predictions of output power when the drive stand is operating at high speeds. The largest source of error is in estimating the maximum voltages the inverter will be able to provide the ISG. The voltages are related to current by $V_d = R_d I_d - \omega L_q I_q$ and $V_q = R_q I_q + \omega (L_q I_d + \lambda_{PM})$, where $V_{dq}$, $I_{dq}$, and $L_{dq}$ are the $d$- and $q$-axis voltages, currents, and inductances, $R_s$ is the phase resistance, $\omega_e$ is the electrical frequency in radians per second, and $\lambda_{PM}$ is the PM flux linkage. The magnitude of the $dq$ voltage vector,

$$V_{mag} = \sqrt{V_d^2 + V_q^2}$$

(6.1)
must be lower than a maximum voltage $V_{max}$, otherwise the inverter will operate in 6-step mode. This voltage maximum is determined by $V_{max} \approx \frac{2}{\pi}(V_{DC} - V_{sw} + V_D)$, where $V_{DC}$ is the DC bus voltage, $V_{sw}$ is the on-voltage drop across one IGBT, and $V_D$ is the on-voltage drop across a reverse diode. $V_{sw} = 2$ V and $V_D = 1$ V for the Danfoss inverter, however these voltages are not well defined and thus a source of error. A second source of error comes from filter inductors in the Danfoss inverter. There are three inductors, one for each phase, and they are mounted in series with the phase leads, carrying full current. The inductance is specified in a schematic of the drive to be 45 $\mu$H each, however this was not experimentally verified. The filter inductors are treated as additional leakage terms that add directly to the $d$- and $q$-axis inductances.

![Diagram](image)

Figure 6.5: Comparison of torque curves at 300 A_{peak}

The measured shaft power, taken at the maximum three-phase power points, is plotted in Figure 6.6. The predicted curve is generated with FEA cross-coupled inductances including $L_{cmd}$ and $L_{fringe}$, offset by 40 $\mu$H for the filter inductors in the inverter and an additional 9 $\mu$H on $L_d$ for the PM fringing. 40 $\mu$H is used instead of 45 $\mu$H because this value resulted in the best matching between measured and predicted powers, and a 10% error in inductance is not unreasonable. The curve is traced out by sweeping $I_d$ and $I_q$ between 0 and 350 A_{peak} (inverter current limit) at speeds between 500 and 6000 RPM. For every $I_d$ and $I_q$ point, $V_{ds}$, $V_{qs}$, three-phase power, and shaft power are calculated. If the magnitude of the current vector is greater than 350 A, or if the magnitude of $dq$ voltage vector from (6.1) is greater than $V_{max}$, that power point is thrown away. The maximum is found from the remaining three-phase powers at each speed point. The resulting
prediction of shaft power corresponding to the maximum three-phase power is plotted versus speed in Figure 6.6.

![Figure 6.6: Maximum shaft power with Danfoss inverter limitations](image)

The error bars on the measured data come from the precision of the torquemeter. Above 2000 RPM the curve is more sensitive to the voltage limit than to the values of $L_d$ and $L_q$. Below 2000 RPM, torque is sensitive to inductances and the predicted power is greater than measured. This is consistent with the motoring plots in Figure 6.4, where predicted torque exceeds measured torque for current vectors between the maximum torque per Amp trajectory and the negative $d$-axis. Therefore, in order to maximize the generating capabilities of the ISG, the parasitic inductances of the three-phase leads should be minimized, and the inverter switches should be chosen with the lowest possible on-voltage.

The power plotted in Figure 6.7 is calculated from the power prediction algorithm used to generate Figure 6.6, except with the limitations of the Danfoss inverter removed. That is, the 40 μH filter inductors and the IGBT and diode drops are removed. This represents the predicted theoretical maximum three-phase power that can be drawn from the ISG operating off a 42 V DC bus, assuming the windings are 25°C. Also plotted is the design specification for generating power. FEA predictions indicate the specification will be met up to 3500 RPM. However, even though the ISG will not generate 6 kW at 6000 RPM, the predicted power at 6000 RPM is 4.3 kW; nearly 3 times the power of a typical alternator used in vehicles in 2002. LPM predictions for power are much higher than FEA. This error is due to the low LPM prediction of $L_d$. If the $d$-
axis LPM is modified to incorporate the saturation characteristics of the PM bridges, $L_{fringe}$ described in Section 3.2.2, and cross-coupling effects, the predictions for power should be closer to FEA and experimental results.

![Diagram](image)

Figure 6.7: Theoretical maximum power without inverter limitations

Testing showed that at high speeds, the $I_d$ and $I_q$ signals inside the dSPACE controller are very noisy. This noise propagates to the $V_d$ and $V_q$ commands, which can affect the currents in the machine. The origin of this noise is unclear. The error correction signal in the position sensor may not be accounting for the entire error signal at high speeds. Regardless, the controller is able to function well enough, and meaningful data is taken.

To complicate matters further, the current measurement signals fed back to the controller are conditioned with external two-pole filters having a 3dB cutoff frequency of 6.7 kHz. The phase shift $\alpha$ from these filters is expressed as

$$\alpha = 2 \cdot \tan^{-1}\left(\frac{\omega_e}{2\pi \cdot 6700}\right)$$  \hspace{1cm} (6.2)

where $\omega_e$ is the electrical frequency in radians/s. At 6000 RPM, $\alpha = 10.2$ electrical degrees. When put through the $dq$ transformation, this translates directly into a 10.2 degree rotation of the current vector. Because of the implementation of the control software, the actual $I_d$ and $I_q$ current in the ISG are at the values dictated by the voltage limit, and this rotation only affects the variables inside the controller. Figure 6.8 demonstrates the consequence of this rotation. The
dots represent shaft power calculated from a torque and speed measurement on the shaft. At the same time the shaft power is measured, the $I_d$ and $I_q$ variables are read out of the dSPACE controller. The diamonds represent the shaft power predicted from these $I_d$ and $I_q$ readings with FEA cross-coupled inductances. These values are considerably lower than the measured power at high speeds. The measured current vector is then rotated by an angle $\alpha$ calculated at each speed using (6.2). The $I_d$ and $I_q$ from this rotated current vector are then used to calculate shaft power. These points are represented by the triangles, and they lie much closer to the measured power points. This verifies that the current vector is rotated before being read by the controller, and that this rotation does not have an effect on the maximum power that can be drawn from the ISG.

![Figure 6.8: Comparison of shaft power, predicted after adjusting for filter delay in currents](image)

**6.6 Summary**

The LPM predicts $\lambda_{PM}$ to within 2.6% of the measured value. FEA and LPM predictions for $L_q$ without cross-coupling match experimental results quite well. $L_q$ at $I_q < 100$ A is dominated by the airgap dimensions and at $I_q > 100$ A is dominated by the $B$-$H$ curve of the core material. The LPM for $d$-axis reluctance must be modified to account for the PM bridges, cross-coupling, and 3D fringing flux around the PM cavities.

The best predictions of motoring torque and generating power come from the cross-coupled FEA inductances, adjusted for the PM fringing by adding a 9 $\mu$H leakage inductance to $L_{dq}$.
The maximum generating power that can be pulled out of the ISG is limited by the Danfoss inverter. The IGBT on-voltage drops are relatively large and non-linear, and 45 μH filter inductors are in series with the three phases. The filter inductance is treated as a leakage term, and added directly to \( L_d \) and \( L_q \). A 10% decrease in the value of the filter inductor is necessary in order for experimental generating data to match the maximum power predicted from FEA inductances. The current vector measured by the controller is rotated due to filters placed in series with the current feedback signals. This does not inhibit the control from commanding the \( V_d \) and \( V_q \) required to generate maximum power at any speed.

The predicted theoretical maximum power that the prototype can generate from a 42 V DC bus does not meet the specification above 3500 RPM. However, the ISG is predicted to generate 4400 W of power at 6000 RPM, nearly three times the current output capacity of an average alternator in vehicles in 2002.
Chapter 7 Summary and Conclusions

7.1 Summary

Previous work includes the development of LPMs (lumped-parameter models) to analyze the electrical performance of IPM (interior permanent magnet) machines. Monte Carlo synthesis was used to search a wide design space and arrive at a cost-optimized design for an ISG (integrated starter/generator). Three working prototypes of the ISG were built and one was fully tested in the course of this thesis. A controller implementing a flux-weakening strategy was developed for this application and runs on a dSPACE board. The work discussed in this thesis begins with the assembly of a complete dynamometer test stand to perform automated tests on the prototype at MIT. These tests include measuring the DC phase resistance, fundamental PM flux linkage, $d$- and $q$-axis inductances, motoring torque, and generating power. Comparisons of the measured results to predictions made from LPMs and FEA (finite element analysis) are presented, and errors have been explained and justified.

7.2 Conclusions

The ISG tests are limited to 350 A$_{peak}$, therefore motoring torque at rated current could not be experimentally verified. Predictions for torque based on FEA cross-coupled inductances were experimentally proven to be the most accurate. These predictions indicate that the ISG is capable of producing 160 Nm of motoring torque at rated current. Generating results have proven that the ISG is not capable of meeting the specification for output power up to 6000 RPM. However, the ISG is predicted to supply 4.3 kW at 6000 RPM; nearly three times the power of a typical generator in vehicles in 2002.

The most accurate prediction of torque and power in the ISG comes from cross-coupled inductances predicted by FEA, with 5 $\mu$H of end-turn leakage added to $L_d$ and $L_q$, and an additional 9 $\mu$H of leakage inductance added to $L_d$ to account for fringing paths around the PM cavities at the ends of the machine stack.

The LPM used to predict the $q$-axis inductance is accurate. Effects such as slot ripple and cross-coupling are minimal on the $q$-axis inductance. The LPM used to predict the $d$-axis inductance should be updated to include a more detailed analysis of the PM bridges. A leakage inductance term associated with fringing fields around the PM cavities should also be included.
Finally, the effect of cross-coupling flux on the $d$-axis reluctance should be considered. With these improvements added, the $d$-axis LPM should adequately predict machine performance.

The alignment of the encoder wheel used for position sensing is critical for making an accurate measurement of the shaft position. However, some misalignment is inevitable. To correct for small errors, the error signal can be mapped to the position reading and then subtracted in real time. This method is used to correct the position signal used by the controller. Prior to implementing this correction, the maximum speed at which the control would operate was limited to 4000 RPM.

The filters used to condition the current sensing signals add a significant phase shift to the current waveforms at higher speeds. This phase shift translates directly into a rotation of the measured current vector calculated by the controller. The controller is capable of commanding voltages to produce the maximum output power in spite of the rotation. Therefore, this error in measured current vector angle does not limit the controller. The rotation only becomes an issue when analyzing the current readings in the controller and predicting what the torque should be based on these currents. Including a post-processing step to recover the phase is enough to bring torque predictions back in line with measurements.

### 7.3 Future Work

This thesis covers the majority of the characterization of the prototype ISG and its motoring and generating performance. However, due to limitations in the drive stand, there is room for improvement.

Motoring torque was not measured at rated current due to a 350 $A_{\text{peak}}$ current limit imposed by the Danfoss inverter. The datasheets for the IGBT assemblies in the inverter indicate that the IGBTs can sustain 300 A continuous current and 600 A pulsed current for 1 ms. The 350 $A_{\text{peak}}$ limit can be overridden on the drive, allowing higher current testing of the ISG. This modification is not recommended by Danfoss, and could possibly destroy the IGBTs in the drive. Therefore, care must be taken only to pulse high currents for very short durations, keeping within the power limitations of the IGBT specified in the datasheet. A 1 Hz filter built into the torquemeter was selected for all of the torque data gathered for this thesis. In order to measure pulsed torque, a higher frequency filter must be used, or a custom interface to the torque transducer must be built.

The $d$- and $q$-axis inductances were measured with the shaft locked on either the $d$- or $q$-axis and a 60 Hz current excitation applied along the respective axis. This does not measure cross-coupling effects of $q$-axis current on $d$-axis inductance. Experimental results to verify the accuracy of the cross-coupled FEA predictions would have a significant impact on the characterization of the ISG. This data could be gathered by locking the shaft at angles that excite both axes. This is a tedious process because it is not automated, and may be limited in accuracy. A better method would analyze the torque while motoring at low speeds. This requires measuring $V_d$ and $V_q$, which is currently not implemented in the dSPACE controller.

The signals in the controller are noisy when the ISG is generating at high speeds. If more experiments are to be done with the controller regulating the DC bus with closed-loop feedback,
then the source of this noise should be determined and either eliminated or rejected by the controller.

Regarding the lumped-parameter models, the $q$-axis model was verified experimentally to be accurate, however the $d$-axis model should be improved. One suggested improvement is to incorporate the saturation characteristics of the PM bridges in the model. The current model assumes the bridges are fully saturated by PM flux alone, and experimental results show that this assumption cannot be made. Another improvement is to add a leakage component to the model to account for fringing fields around the PM cavities at the ends of the stack. A final suggestion is to incorporate the cross-coupling effects of $q$-axis current on $d$-axis inductance.
Appendix A  Current Ripple Inductance Measurement

The first method attempted at measuring inductance involved applying a DC bias current to the ISG generated by PWM switching, and analyzing the ripple current [14]. At relatively high frequency (20 kHz), the impedance of the ISG is approximated as purely inductive. Therefore, the ripple current in the ISG is a triangle wave, and the slope of the wave is proportional to the inductance in the ISG and the voltage applied to its terminals. The setup for the experiment is shown in Figure A.1. The Danfoss inverter and ISG are configured as a buck converter with the stator resistance in the ISG acting as the load. The DC bias current in the ISG is proportional to a PWM signal with duty cycle $D$ applied to the top IGBT. The inductance measured by this experiment was consistently lower than the predicted value and lower than the inductance measured by the single-phase 60 Hz excitation described in Section 4.5.1. The reason for the discrepancy is thought to be due to the minor hysteresis loops in the core material excited by the combination of DC and ripple current. These minor loops may have a lower permeability due to the high frequency and low flux density associated with the ripple current. The net effect of the lower permeability in the core material would be a reduction in the inductance seen by the ripple. This explains why the measurements were consistently lower.

![Figure A.1: Experimental setup for inductance calculated from ripple current](image)

- 89 -
### Appendix B  Figure Data

<table>
<thead>
<tr>
<th>Current $[\text{A}_{\text{peak}}]$</th>
<th>Measured $L_d [\mu \text{H}]$</th>
<th>Measured $L_e [\mu \text{H}]$</th>
<th>LPM $L_d [\mu \text{H}]$</th>
<th>LPM $L_e [\mu \text{H}]$</th>
<th>FEA $L_d [\mu \text{H}]$</th>
<th>FEA $L_e [\mu \text{H}]$</th>
<th>Max Torque angle [deg]</th>
</tr>
</thead>
<tbody>
<tr>
<td>10</td>
<td>101.20</td>
<td>289.24</td>
<td>69.10</td>
<td>294.60</td>
<td>107.55</td>
<td>328.84</td>
<td></td>
</tr>
<tr>
<td>20</td>
<td>99.37</td>
<td>297.57</td>
<td>69.10</td>
<td>298.62</td>
<td>104.85</td>
<td>328.23</td>
<td></td>
</tr>
<tr>
<td>30</td>
<td>97.66</td>
<td>300.48</td>
<td>69.10</td>
<td>302.64</td>
<td>102.62</td>
<td>328.05</td>
<td></td>
</tr>
<tr>
<td>40</td>
<td>96.10</td>
<td>302.53</td>
<td>69.10</td>
<td>303.58</td>
<td>100.62</td>
<td>327.95</td>
<td></td>
</tr>
<tr>
<td>50</td>
<td>94.65</td>
<td>302.94</td>
<td>69.10</td>
<td>304.24</td>
<td>98.83</td>
<td>328.02</td>
<td></td>
</tr>
<tr>
<td>60</td>
<td>93.31</td>
<td>303.34</td>
<td>69.10</td>
<td>304.90</td>
<td>97.08</td>
<td>327.86</td>
<td></td>
</tr>
<tr>
<td>70</td>
<td>92.08</td>
<td>300.99</td>
<td>69.10</td>
<td>304.85</td>
<td>95.52</td>
<td>327.55</td>
<td></td>
</tr>
<tr>
<td>80</td>
<td>90.99</td>
<td>298.68</td>
<td>69.10</td>
<td>304.79</td>
<td>94.11</td>
<td>326.91</td>
<td></td>
</tr>
<tr>
<td>90</td>
<td>90.02</td>
<td>296.29</td>
<td>69.10</td>
<td>304.66</td>
<td>92.82</td>
<td>325.82</td>
<td></td>
</tr>
<tr>
<td>100</td>
<td>89.18</td>
<td>293.34</td>
<td>69.10</td>
<td>302.90</td>
<td>91.66</td>
<td>323.85</td>
<td></td>
</tr>
<tr>
<td>110</td>
<td>88.46</td>
<td>289.36</td>
<td>69.10</td>
<td>301.14</td>
<td>90.63</td>
<td>321.05</td>
<td></td>
</tr>
<tr>
<td>120</td>
<td>87.82</td>
<td>284.38</td>
<td>69.10</td>
<td>298.89</td>
<td>89.69</td>
<td>316.72</td>
<td></td>
</tr>
<tr>
<td>130</td>
<td>87.26</td>
<td>278.80</td>
<td>69.10</td>
<td>292.11</td>
<td>88.85</td>
<td>311.24</td>
<td></td>
</tr>
<tr>
<td>140</td>
<td>86.74</td>
<td>272.94</td>
<td>69.10</td>
<td>285.33</td>
<td>88.07</td>
<td>304.77</td>
<td></td>
</tr>
<tr>
<td>150</td>
<td>86.25</td>
<td>266.86</td>
<td>69.10</td>
<td>278.38</td>
<td>87.37</td>
<td>297.61</td>
<td></td>
</tr>
<tr>
<td>160</td>
<td>85.77</td>
<td>260.56</td>
<td>69.10</td>
<td>270.51</td>
<td>86.72</td>
<td>290.05</td>
<td></td>
</tr>
<tr>
<td>170</td>
<td>85.29</td>
<td>254.10</td>
<td>69.10</td>
<td>262.64</td>
<td>86.12</td>
<td>282.31</td>
<td></td>
</tr>
<tr>
<td>180</td>
<td>84.82</td>
<td>247.65</td>
<td>69.10</td>
<td>255.09</td>
<td>85.56</td>
<td>274.41</td>
<td></td>
</tr>
<tr>
<td>190</td>
<td>84.35</td>
<td>241.32</td>
<td>69.10</td>
<td>248.67</td>
<td>85.04</td>
<td>266.62</td>
<td></td>
</tr>
<tr>
<td>200</td>
<td>83.91</td>
<td>235.18</td>
<td>69.10</td>
<td>242.26</td>
<td>84.55</td>
<td>259.08</td>
<td></td>
</tr>
<tr>
<td>210</td>
<td>83.49</td>
<td>229.24</td>
<td>69.10</td>
<td>236.23</td>
<td>84.09</td>
<td>252.01</td>
<td></td>
</tr>
<tr>
<td>220</td>
<td>83.10</td>
<td>223.49</td>
<td>69.10</td>
<td>231.24</td>
<td>83.66</td>
<td>245.24</td>
<td></td>
</tr>
<tr>
<td>230</td>
<td>82.74</td>
<td>217.96</td>
<td>69.10</td>
<td>226.24</td>
<td>83.25</td>
<td>238.76</td>
<td></td>
</tr>
<tr>
<td>240</td>
<td>82.40</td>
<td>212.65</td>
<td>69.10</td>
<td>221.51</td>
<td>82.87</td>
<td>232.56</td>
<td></td>
</tr>
<tr>
<td>250</td>
<td>82.09</td>
<td>207.55</td>
<td>69.10</td>
<td>217.29</td>
<td>82.50</td>
<td>226.66</td>
<td></td>
</tr>
<tr>
<td>260</td>
<td>81.78</td>
<td>202.63</td>
<td>69.10</td>
<td>213.08</td>
<td>82.15</td>
<td>221.12</td>
<td></td>
</tr>
<tr>
<td>270</td>
<td>81.47</td>
<td>197.87</td>
<td>69.10</td>
<td>209.04</td>
<td>81.81</td>
<td>215.90</td>
<td></td>
</tr>
<tr>
<td>280</td>
<td>81.15</td>
<td>193.26</td>
<td>69.10</td>
<td>205.29</td>
<td>81.49</td>
<td>210.98</td>
<td></td>
</tr>
<tr>
<td>290</td>
<td>80.83</td>
<td>188.84</td>
<td>69.10</td>
<td>201.54</td>
<td>81.19</td>
<td>206.26</td>
<td></td>
</tr>
<tr>
<td>300</td>
<td>80.50</td>
<td>184.51</td>
<td>69.10</td>
<td>197.95</td>
<td>80.87</td>
<td>201.78</td>
<td></td>
</tr>
<tr>
<td>310</td>
<td>80.17</td>
<td>180.21</td>
<td>69.10</td>
<td>194.67</td>
<td>80.58</td>
<td>197.47</td>
<td></td>
</tr>
<tr>
<td>320</td>
<td>79.84</td>
<td>176.58</td>
<td>69.10</td>
<td>191.35</td>
<td>80.30</td>
<td>193.36</td>
<td></td>
</tr>
<tr>
<td>330</td>
<td>79.54</td>
<td>172.86</td>
<td>69.10</td>
<td>188.18</td>
<td>80.02</td>
<td>189.40</td>
<td></td>
</tr>
<tr>
<td>340</td>
<td>79.24</td>
<td>169.14</td>
<td>69.10</td>
<td>185.12</td>
<td>79.75</td>
<td>185.58</td>
<td></td>
</tr>
<tr>
<td>350</td>
<td>78.95</td>
<td>165.57</td>
<td>69.10</td>
<td>182.07</td>
<td>79.48</td>
<td>181.91</td>
<td></td>
</tr>
<tr>
<td>360</td>
<td>78.66</td>
<td>162.20</td>
<td>69.10</td>
<td>179.08</td>
<td>79.12</td>
<td>178.93</td>
<td></td>
</tr>
<tr>
<td>370</td>
<td>78.35</td>
<td>159.20</td>
<td>69.10</td>
<td>176.12</td>
<td>78.86</td>
<td>175.98</td>
<td></td>
</tr>
<tr>
<td>380</td>
<td>78.02</td>
<td>156.44</td>
<td>69.10</td>
<td>173.17</td>
<td>78.69</td>
<td>172.91</td>
<td></td>
</tr>
<tr>
<td>390</td>
<td>77.68</td>
<td>153.73</td>
<td>69.10</td>
<td>170.33</td>
<td>78.43</td>
<td>169.85</td>
<td></td>
</tr>
<tr>
<td>400</td>
<td>77.34</td>
<td>151.04</td>
<td>69.10</td>
<td>167.57</td>
<td>78.16</td>
<td>166.83</td>
<td></td>
</tr>
<tr>
<td>410</td>
<td>77.01</td>
<td>148.33</td>
<td>69.10</td>
<td>164.81</td>
<td>77.88</td>
<td>163.81</td>
<td></td>
</tr>
<tr>
<td>420</td>
<td>76.71</td>
<td>145.57</td>
<td>69.10</td>
<td>162.14</td>
<td>77.58</td>
<td>160.89</td>
<td></td>
</tr>
<tr>
<td>430</td>
<td>76.43</td>
<td>142.84</td>
<td>69.10</td>
<td>159.51</td>
<td>77.27</td>
<td>157.87</td>
<td></td>
</tr>
<tr>
<td>440</td>
<td>76.13</td>
<td>140.13</td>
<td>69.10</td>
<td>156.88</td>
<td>76.96</td>
<td>154.85</td>
<td></td>
</tr>
<tr>
<td>450</td>
<td>75.79</td>
<td>137.42</td>
<td>69.10</td>
<td>154.39</td>
<td>76.64</td>
<td>151.83</td>
<td></td>
</tr>
<tr>
<td>460</td>
<td>75.43</td>
<td>134.70</td>
<td>69.10</td>
<td>151.94</td>
<td>76.32</td>
<td>148.81</td>
<td></td>
</tr>
</tbody>
</table>

Table B.1: Data for inductance plots in Figure 6.2 and max torque per Amp plot in Figure 4.19
Figure C.1: Current filter board schematic
Figure C.2: Filter board PCB layout
Figure D.1: Danfoss Inverter Schematic

Appendix D  Danfoss Modified Schematic


