Development of Canned Line-start Rim-driven Electric Machines

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<tr>
<td>$\omega$</td>
<td>Angular frequency of the electric system</td>
<td>radians.s⁻¹</td>
</tr>
<tr>
<td>$\omega_s$</td>
<td>Synchronous angular frequency</td>
<td>radians.s⁻¹</td>
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## List of Acronyms

<table>
<thead>
<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>AC</td>
<td>Alternating current</td>
</tr>
<tr>
<td>AIM</td>
<td>Advanced Induction Motor</td>
</tr>
<tr>
<td>AMSC</td>
<td>American Superconductor Corporation</td>
</tr>
<tr>
<td>BC</td>
<td>Before Christ</td>
</tr>
<tr>
<td>CAD</td>
<td>Computer aided design</td>
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<td>CPP</td>
<td>Controllable-pitch propeller</td>
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<tr>
<td>DC</td>
<td>Direct current</td>
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<tr>
<td>DDAT</td>
<td>Drop-down azimuth thruster</td>
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<td>DOL</td>
<td>Direct-on-line</td>
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<tr>
<td>DP</td>
<td>Dynamic positioning</td>
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<tr>
<td>d-q</td>
<td>Direct-quadrature</td>
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<tr>
<td>DTI</td>
<td>Department of Trade and Industry</td>
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<tr>
<td>EMEC</td>
<td>European Marine Energy Centre</td>
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<tr>
<td>EMF</td>
<td>Electromotive force</td>
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<tr>
<td>FE</td>
<td>Finite element</td>
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<tr>
<td>FEA</td>
<td>Finite element analysis</td>
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<td>FPP</td>
<td>Fixed-pitch propeller</td>
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<tr>
<td>GE</td>
<td>General Electric</td>
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<tr>
<td>HTS</td>
<td>High-temperature superconducting</td>
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<tr>
<td>I/D</td>
<td>Inside diameter</td>
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<tr>
<td>IEPS</td>
<td>Integrated electric power system</td>
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<tr>
<td>IPS</td>
<td>Integrated propulsion system</td>
</tr>
<tr>
<td>LSPM</td>
<td>Line-start permanent-magnet</td>
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<tr>
<td>LTS</td>
<td>Low-temperature superconducting</td>
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<tr>
<td>Acronym</td>
<td>Full Form</td>
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<tr>
<td>MMF</td>
<td>Magnetomotive force</td>
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<tr>
<td>NAB</td>
<td>Nickel-aluminium-bronze</td>
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<tr>
<td>O/D</td>
<td>Outside diameter</td>
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<tr>
<td>PM</td>
<td>Permanent-magnet</td>
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<tr>
<td>RID</td>
<td>Rotor inside diameter</td>
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<tr>
<td>RMS</td>
<td>Root mean square</td>
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<tr>
<td>rpm</td>
<td>revolutions per minute</td>
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<tr>
<td>RTD</td>
<td>Resistive thermal device</td>
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<tr>
<td>SID</td>
<td>Stator inside diameter</td>
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<tr>
<td>TGL</td>
<td>Tidal Generation Ltd.</td>
</tr>
<tr>
<td>TTCP</td>
<td>The Technical Co-operation Program</td>
</tr>
<tr>
<td>U.K.</td>
<td>United Kingdom</td>
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<tr>
<td>U.S.</td>
<td>United States</td>
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<tr>
<td>VPI</td>
<td>Vacuum pressure impregnation</td>
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<tr>
<td>2-D</td>
<td>Two-dimensional</td>
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<td>3-D</td>
<td>Three-dimensional</td>
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Abstract

Electric machines are being deployed in industrial applications where previously only mechanical systems were considered, as environmental concerns from burning fossil fuels and energy costs are becoming a more dominant factor in system design considerations. Electric machines offer greater operational flexibility and typically higher efficiencies. There has therefore been a growing demand to develop electric machines to replace traditional mechanical systems in a number of industrial applications.

One such suitable electric machine topology is the ‘direct-drive’ machine. These machines can be used where implementation does not require a high operating speed, therefore eliminating the necessity of a gearbox. Furthermore, direct-drive machines offer a number of advantages including reductions in through-life costs, noise and vibration, and overall system volume.

This thesis explores the development of direct-drive rim-driven machines, constructed by integrating a propeller with the electric machine that is driving it, by mounting the machine directly around the outside of the propeller. A novel machine topology was developed by integrating a conducting-can onto the rotor structure capable of producing induction torque, to create a motor that can start directly from the main electric supply. This eliminated the need for a power electronic converter, gearbox and complicated drive shafts arrangement, for use in applications where only a low duty cycle of operation was required such as secondary propulsion systems for marine applications or where safety and reliability is of significant importance. A number of other industrial applications that may benefit from this canned rim-driven topology were also identified including seal-less pumps and ‘run-of-the-river’ generators.
Permanent-magnet and induction motor topologies operating in fluid environments were investigated, using finite element analysis and thermal analysis techniques, to examine and optimise the design of the rim-driven topology for a specific operational requirement, in each industrial application area identified.

A 30 kW canned line-start rim-drive induction motor was designed and developed for use as a bi-directional thruster on-board a tidal stream turbine. A novel induction motor topology was developed utilising only a conducting-can on the rotor, which eliminated the need for a traditional squirrel-cage, due to the ratio of the relatively large mean air-gap diameter to the small output power requirement; creating a simple yet reliable direct-drive canned induction motor. The design of this motor was manufactured and successfully tested to validate the design process.
Declaration

No portion of the work referred to in this thesis has been submitted in support of an application for another degree or qualification of this or any other university or other institute of learning.
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Acknowledgements

The completion of this research project would not have been possible without the dedication and valuable guidance from my supervisor, Professor Sandy Smith. I would therefore like to take this opportunity to extend my sincere gratitude and appreciation to Professor Smith for his continual supervision and encouragement, along with his endless patience, which has helped me to navigate the fascinating world of electromagnetic machine design and to complete this research project as a result.

I would like to thank all the academic staff, along with the Research Associates/Assistants and Ph.D. candidates of the Power Conversion Group, for their support and laughter during my time at the university. I would also like to thank the staff from both the electrical and mechanical support workshops, whose help assisted in the completion of this research project. I would like to extend this thanks to all the staff of the School of Electrical and Electronic Engineering, who have supported me during my time at the university.

I wish to acknowledge Rolls-Royce Plc. for the opportunity to conduct an interesting and practical research project, and for the financial assistance provided to cover the costs associated with the manufacture of the prototype rim-drive motor developed, as explained within this thesis. A special thanks also goes to Mark Husband and Dr. Paul Hopewell from Rolls-Royce, for their input to the project.

I am grateful to Dr. Dave Staton, Dougie Hawkins and Dr. Mircea Popescu from Motor-Design Ltd., for all their assistance in helping me to understand the intricacies of both the finite element analysis and thermal analysis software packages used in the investigation of the electric motors designed within this thesis.
A special note of appreciation goes to Ian Sanders for his invaluable wisdom and guidance in helping me to navigate the journey through life.

Finally, I would like to thank my family for their blessings and encouragement, which gave me the opportunity to return to university after a number of years away from the academic world.
Chapter 1

Introduction

Rotating electric machines are energy conversion devices, which are classified either as a motor or a generator depending on their mode of operation. A motor converts electric energy to mechanical energy by the creation of magnetic fields, whereas a generator functions in the opposite way. Following on from the work of Hans Christian Oersted in 1820, and Pacinotti in 1860, Zenobe Theophile Gramme developed a direct current (DC) motor, which was the first motor to be of commercial significance [1]. Due to the limitations of transmitting DC power over large distances, however, Nikola Tesla developed an alternating current (AC) distribution system and subsequently, in 1888, an AC motor [2]. The first commercial application of Tesla’s AC system, on a large-scale, was the Niagara Falls hydro power plant, which was completed in 1895 [3].

It has been noted that most industrial motors greater than 746 W can be classified as three-phase induction machines [3]. The invention of the electric machine revolutionised the world and facilitated industrial development. They are used today in ever increasing numbers to both generate and consume electric energy. As an example, the U.S. Department of Energy has estimated that motors consume 55% of all the electric energy generated in America, 25% of which is consumed by motors in excess of 750 kW. In specific sectors such as the domestic manufacturing sector, motors consume as much as 70% of all the electric energy generated [4].

With generally rising energy costs and environmental concerns from burning fossil fuels, there is a growing demand to improve system efficiencies. Today electric machines are being utilised, therefore, in an increasing number of applications such as
the ‘more-electric aircraft’ [5-7], the ‘more-electric ship’ [8-12] and the ‘more-electric car’ [13-15]. Furthermore, electric machines are being used in the fluid process industry to increase safety and reliability [16-19]. By replacing traditional mechanical systems in these areas with electric machines, operational flexibility and overall system efficiency improvements are generally achievable.

1.1 Low-speed Direct-drive Machines

In applications where high speed is not required, there is the potential for electric machines to be directly coupled to the load, eliminating the need for a gearbox. These are referred to as ‘direct-drive’ machines. Direct-drive machines offer several advantages including:

- reduced through-life costs;
- reduced weight;
- reduced noise and vibration; and
- reduced system volume, i.e. space allocation.

The reduction in through-life costs arises from such things as reduced requirements for maintenance such as gearbox oil/grease top-up/replacement; replacement of dynamic shaft seals and gearbox removal, overhaul, and replacement. Although the direct-drive machine will be larger in volume to compensate for the elimination of the gearbox, it is considered that the reduced weight and system volume is achieved if the overall system volume is considered, i.e. including drive shafts and associated support structures. Finally, the reduction in noise and vibration is achieved again because of the elimination of the gearbox and hence, associated gear teeth backlash. Furthermore, direct-drive machine generally operate at slower speeds reducing windage noise.

Direct drive machines are presently being used in a number of industrial applications including renewable energy wind turbines [20, 21] marine propulsion systems [8, 22, 23] and elevators [24, 25]. There is the possibility, therefore, for the development of novel electric machines for use in, but not solely limited to, these application areas.
1.2 Rim-driven Electric Machines

One such electric machine is the ‘rim-driven’ topology. In marine or pumping applications, a rim-driven thruster/pump can be constructed by integrating a propeller with the electric machine that is driving it by mounting the machine directly around the outside of the propeller. The concept of driving propeller tips directly, rather than via a central propeller shaft, was suggested in 1957 using a mechanical design [26]. Electric designs have also been proposed more recently [27, 28]. An illustration of the electric rim-driven concept as a marine thruster is presented in Figure 1.1.

![Illustration of a Rim-driven Thruster](image_url)

Rim-driven machines naturally have a large diameter; a topology that favours large torque production in electric machines. They are therefore suitable for a number of industrial applications, some of which will be identified and investigated in this thesis. These machines, however, usually have a short axial length. The large diameter necessitates that the radial depth of the core-backs are kept as thin as possible to avoid excessive weight. These machines are typically required to operate at slow speeds, therefore a high pole number is usually required, which enables the radial depth of the core-backs to therefore be reduced and hence, the weight and size of the machine to be minimised. A simple illustration of the aspect ratio (diameter to axial length) of a conventional machine compared to a rim-driven machine is shown in Figure 1.2.
For most marine propulsion systems, or in the case of pumping applications, embedding the electric machine into the thruster/pump usually means operating in a fluid environment. This can be used, however, to improve the cooling and hence, increase the torque density of the machine from that which is normally achieved with natural or forced air cooling. Environmental containment of the rotor (and stator) assemblies is therefore a key requirement to protect them from contamination and potential pressure issues. This environmental protection is commonly in the form of an epoxy resin layer or a metallic containment can/sleeve, for example.

1.3 Research Project Approach and Objectives

The approach of this research project is to investigate, initially using finite element analysis (FEA) and thermal analysis software, the design and implementation of electric machines integrated directly into the structure of thrusters/pumps, specifically the housing around propellers themselves, creating novel rim-driven electric machines. Several potential and suitable application areas for the rim-driven topology have been identified, which include:

- an emergency back-up marine propulsion system (for use in the event of a primary propulsion system failure);
• a low-speed manoeuvring (docking) marine propulsion system;
• a seal-less pump (for use in safety critical environments);
• a bi-directional thruster (for use in a tidal stream turbine); and
• a ‘run-of-the-river’ generator.

The electric machine topologies that are to be investigated using the rim-driven concept are permanent-magnet and induction machines operating in fluid environments, potentially offering the advantage of large torque densities. The difficulty with this technology, however, is the need for a power electronic converter to condition the electric power supplied to the motor to control the operating speed. The additional volume and significant cost associated with this converter offsets the potential benefits of a rim-drive motor solution for low duty cycle applications where speed control is not a necessity, for example.

The need for the converter can be avoided, however, by integrating a conducting-can onto the rotor structure capable of producing induction torque, to produce a novel topology, i.e. a canned rim-drive motor that can start directly from the main electric supply. The design of a line-start rim-drive solution without the requirement for a power electronic converter would enhance the market by providing a low-cost electrical solution in direct-drive applications with low duty cycle requirements.

The main aim of this research project, therefore, is to develop an understanding of the requirements for the design and successful implementation of the canned rim-driven electric machine topology. The final objective is to manufacture and test a prototype canned rim-drive motor to validate the design process.

1.4 Significance of the Work

This research project has significance both fundamentally and practically. Several paper design concept studies are presented, which are considered for different industrial applications utilising the line-start rim-drive motor topology with the addition of cans introduced into the air-gap, to modify their performance and provide environmental protection, creating novel electric machines.
The effect of the rim-drive motor topology upon the operational performance of each machine has been investigated using FEA and thermal analysis software. The predicted results from the numerical models were compared to industrial benchmark machines, to highlight the effects the design modifications produced and to give validity to the predicted results. Conclusions were then drawn on the key design considerations required for each machine.

An understanding of the key design considerations led to the design, manufacture and successful testing of a 30 kW prototype rim-drive canned induction motor. This prototype rim-drive motor has a novel canned rotor construction, which eliminated the requirement for a traditional squirrel-cage. The rotor construction was therefore simplified and hence, the reliability of the motor was increased.

An illustration of the industrial applications and the associated motors that were investigated for each application during this research project is presented in Figure 1.3.

![Figure 1.3: Block diagram of the Motors Investigated within this Thesis](image-url)
1.5 Thesis Structure

This section provides an overview of the structure of the thesis and gives a brief summary of each chapter. The remainder of the thesis, which is arranged into seven chapters, is presented as follows:

Chapter 2: Literature Review identifies a number of industrial application areas where the rim-driven electric machine topology has the potential to be utilised. These areas include electric marine propulsion motors such as ‘bow thrusters’ and ‘azimuth thrusters’, which typically have a low duty cycle operational requirement; seal-less pumps for the fluid process and transportation industry, where safety and reliability are generally the key design requirements and finally, a renewable energy run-of-the-river generating system for remote locations where connection to the national grid is not economically viable or only a small power requirement is necessary.

Chapter 3: Background begins with an introduction to magnetic material theory, presenting an explanation of the typical materials used in the manufacture of electric machines and the reasons for their choice. The chapter goes on to review the advancements in permanent-magnet materials including their history and development, an examination of the hysteresis loop, energy product and the effect of temperature on these materials. The chapter finishes with a discussion of FEA; a description of its history and development is given, along with an overview of the electromagnetic software package used to model and analyse the electric motors designed in the subsequent chapters of this thesis.

Chapter 4: Rim-drive Topology Concept Studies describes the work conducted on the design of electric motors utilising the rim-drive motor topology for several industrial applications including a secondary marine propulsion system, a main coolant pump for a nuclear reactor and an emergency propulsion system for submarines. An overview of the key design parameters for each concept design, along with the FEA predicted performance results is presented, which are compared to comparable industrial motors that are selected as benchmark machines.
Chapter 5: Prototype Rim-drive Motor for Tidal Stream Turbine Positioning Thruster presents the work conducted on the design, analysis and development of a prototype rim-drive motor for use as a bi-directional thruster on-board a tidal stream turbine. The key design requirements and considerations for the application are explained, along with the fundamental design calculations that are used to formulate the initial design of the prototype rim-drive motor. A FEA and thermal analysis sensitivity study is also discussed, which was used to optimise the final prototype motor design.

Chapter 6: Manufacture and Experimental Validation of the Prototype Rim-drive Motor describes the process undertaken to manufacture the prototype rim-drive motor, along with the support structures for the stator and rotor assemblies, and the test-rig. Experimental tests conducted to characterise the operation of the prototype rim-drive motor are then presented. These test results are compared with the FEA predicted results to validate the design process and conclusions are drawn.

Chapter 7: Conclusions and Recommendations for Further Work summarises the achievements of this research project and outlines additional work that could be conducted to further develop the rim-driven electric machine topology. Publications and patents that have been created during this research project are also listed.
Chapter 2

Literature Review

2.1 Introduction

The previous chapter gave a description of the rim-driven electric machine concept and identified several industrial application areas where it could potentially be implemented. These included electric propulsion motors for marine vessels, seal-less pumps for the fluid process and transportation industry and renewable energy run-of-the-river generating systems. Each application area will therefore be explored in greater detail in this chapter and a review of the present technology in each area will be given. This will be followed by identifying gaps in the present state of the technology where the rim-driven electric machine concept has the potential to be utilised to further enhance these markets.

2.2 Marine Applications

2.2.1 Mechanical Propulsion System

Historically, propulsion for marine vessels has been achieved with mechanical systems. A conventional mechanical propulsion system is generally composed of a prime mover such as a diesel engine or gas turbine, which is connected to a reduction gearbox, through to a shaft that drives a propeller. An illustration of the components of a traditional mechanical propulsion system is shown in Figure 2.1.
Figure 2.1: Components of a Traditional Mechanical Propulsion System - a). Shafting and Accessories, b). Reduction Gearbox, c). Propeller, and d). Prime Mover [30]

Figure 2.1 highlights that the propeller drive shaft of a mechanical propulsion system can sometimes extend a considerable distance within the hull of a marine vessel, in relation to the other components of the system. They waste valuable space, therefore, within the hull. Furthermore, prime movers require large air intakes and exhaust systems, which both require piping through the hull, wasting additional space. Due to the strict alignment requirements between a prime mover and propeller, the propeller drive shaft is usually mounted at an angle. This diminishes the ability of the propeller to produce maximum directional propulsion thrust and hence, reduces the efficiency of the propulsion system.

A prime mover is typically coupled to a reduction gearbox to convert the low torque and high speed of the prime mover to the high torque and low speed that is required to rotate a propeller. A reduction gearbox, however, is expensive, can be large in both weight and volume, and requires regular maintenance. Furthermore, they increase the noise and vibration level of a vessel, which reduces passenger comfort in commercial vessels or stealth ability in military vessels. It would be beneficial, therefore, to remove the
requirement for a reduction gearbox and also the long propeller drive shaft of the propulsion system.

Auxiliary electric power for the ‘hotel loads’ on-board marine vessels has traditionally been supplied via dedicated generators. Hotel loads are defined as the energy requirement for non-propulsion duties and include such things as lighting, air conditioning, water purifiers and entertainment systems, etc. The hotel load demand onboard marine vessels, however, has been continually increasing and is now estimated to account for 20% or more of the total installed power [10]. The separate mechanical propulsion system and the electric generation system just described are usually referred to as a segregated propulsion system, as illustrated in Figure 2.2.

![Figure 2.2: Marine Vessel Segregated Propulsion System [31]](image)

The example of the segregated propulsion system shown in Figure 2.2, is composed of the traditional mechanical propulsion system, along with several smaller prime movers coupled directly to generators, to provide auxiliary power to the vessel. The auxiliary power is distributed throughout the vessel via a distribution ring system to supply all the hotel loads.

Mechanical propulsion systems are typically designed to be operated at a single speed. Operating at or close to this speed, fuel efficiency is maximised and emissions levels are at their lowest [32]. Marine vessels, in particular military vessels, for example, rarely operate at full speed for extended periods of time, however, which results in a reduction in efficiency, increased operating costs and also emissions [8]. It would be beneficial,
therefore, to keep the prime movers operating in their optimal efficiency range regardless of the propulsion requirements of any particular vessel. Furthermore, in the mechanical propulsion system, each propeller and associated drive shaft is directly coupled to a reduction gearbox, which is driven by a prime mover. Consequently, 75-80% of the combined prime movers output power is reserved solely for propulsion duties, as illustrated in Figure 2.2. As mentioned previously, the demand for auxiliary electric power on-board marine vessels has generally been increasing and is predicted to increase further. As an example, today’s military destroyers are generally built with three 2.5-3 MW generators to supply the hotel loads whereas in 1902, two 5-10 kW generators were installed [10].

Taking the factors presented above into account and considering the ever-tightening emissions legislations that are being introduced for ships [33, 34] and the continual general increasing cost of fossil fuels [35], there has been a growing demand for electric propulsion systems to replace the conventional segregated propulsion system in certain marine applications. This is because electric propulsion systems can be operated more efficiently over a wider power range. Furthermore, by removing the reduction gearboxes and associated long propeller drive shafts, naval architects would have greater freedom to design marine vessels to optimise the pay load capacity without the constraints of the optimum location of the propulsion system components.

The space currently associated with the gearboxes and propeller drive shafts, therefore, could be utilised more profitably in commercial vessels by maximising the internal volume of the hull for additional cargo or cabin space, for example. With the internal space more easily accessible, cargo loading and unloading cycle times could be minimised, which would reduce port costs and further increase the profitability of a vessel [36].

2.2.2 Electric Propulsion System

The standard topology of an electric propulsion system is to couple a prime mover directly to a generator. The generator converts the rotational energy of the prime mover into electric power, which can then be circulated through the main power distribution system. Some of the electric power can be converted back to rotational mechanical
energy by means of an electric motor designated for propulsion duties. A diagram of a mechanical propulsion system compared to an electric propulsion system, along with their typical associated component efficiencies, $\eta$, is shown in Figure 2.3.

An electric propulsion system is less efficient when compared directly to a mechanical propulsion system because of the additional energy conversion steps, as shown in Figure 2.3. However, this is only when the mechanical propulsion system is operating at full speed and hence, maximum efficiency. The efficiency of a mechanical propulsion system drops off more quickly with reduced shaft speed compared to an electric propulsion system and therefore, an electric propulsion system can be more efficient at lower speeds [8]. For example, 80% power provides 93% speed but 25% speed only requires about 1.5% power [37].

Over the whole operating speed range of a marine vessel therefore, an electric propulsion system is generally more efficient. This fact has resulted in the design and development of electric propulsion systems, which are more flexible and efficient if operation over a wider speed range is required. An electric propulsion system is commonly referred to as an Integrated Electric Power System (IEPS). An illustration of the general architecture of an IEPS, utilising fixed speed motors with a variable pitch propeller system (although a fixed pitch propeller system with power electronic converters can also be implemented), is presented in Figure 2.4.
In the example of the IEPS shown in Figure 2.4, all of the prime movers installed on-board a vessel are connected directly to generators and their rotational energy is converted into electric power. This power is distributed through the main electric distribution system, which is operated at a constant voltage and frequency. The distribution system consists of a ring, which allows the flexible allocation of power and controllable switching architecture to be implemented. As a result, the reliability and survivability of a vessel can be improved, which is especially beneficial in military vessels. An illustration of the flexibility that an IEPS offers military vessels to distribute the total installed power is shown in Figure 2.5.
Figure 2.5 shows that with an IEPS, the electric power can be used to supply both the propulsion and non-propulsion loads. Furthermore, the power can be redistributed relatively quickly depending on the requirements of a vessel, which allows the available power to be utilised most appropriately, where and when it is needed. This is particularly beneficial when propulsion is not an operational requirement or the primary propulsion system has been damaged, for example. The majority of the generated power is therefore available for directed energy weapons such as lasers and rail guns, etc.

The implementation of an IEPS on-board a marine vessel typically results in less prime movers being required, although the installed power is generally the same [8]. This is illustrated in Figures 2.2 and 2.4, which have seven and five prime movers installed, respectively. Fewer prime movers typically reduce the initial and through-life costs of a vessel, since:

- fewer prime movers require less initial capital to buy;
- less maintenance is required; therefore, less staff are needed and also less spare stock inventory is required to be carried, reducing through-life costs;
- fewer prime movers require less fuel, reducing running costs [8, 10]; and
- fewer prime movers require less space within the hull of a vessel, which can subsequently be used for additional revenue generating purposes, i.e. cargo or cabin space, for example.

For example, it has been calculated that for a military frigate, the running costs of the prime movers can be between 29.8% and 37.4% lower, depending on the operating profiles, when an IEPS is compared to a mechanical propulsion system [8].

In an IEPS, the gearboxes and propeller drive shafts of a mechanical propulsion system are removed and replaced with an electric propulsion equivalent system composed of generators, variable-speed drives (converters) and electric motors with fixed pitched propeller systems or fixed speed motors with controllable pitch propeller systems, with the electric components connected via electric cables. The IEPS topology, therefore, allows for flexibility in the placement of these electric components, which can maximise the internal volume within a hull and also simplify the construction of a vessel. A number of electric motor topologies have therefore been examined and developed for use in electric marine propulsion systems.
2.2.3 Hybrid-electric Propulsion System

A third alternative to the mechanical and electric propulsion systems is the hybrid-electric propulsion system. This system is seen as an attractive alternative to the two systems presented previously in certain marine applications. The hybrid-electric propulsion system combines elements of both the mechanical and electric propulsion system. An illustration of a typical hybrid-electric propulsion system is shown in Figure 2.6.

![Figure 2.6: Marine Vessel Hybrid-electric Propulsion System [38]](image)

Generally, hybrid-electric propulsion systems utilise a common gearbox with two input shafts. The first input shaft is coupled to a prime mover directly, similar to the mechanical propulsion system, and supplies the high speed requirements or high duty cycle operation for the vessel. The second input shaft is coupled to an electric motor, similar to the electric propulsion system, and is used for the low speed requirements or low duty cycle operation [39]. The advantage of the hybrid-electric propulsion system is that it can increase the profitability of a vessel for the reasons previously discussed.

2.2.4 Electric Motor Topologies

The exploitation of electric motors in marine propulsion systems is not a new concept. Their use can be traced back over a hundred years in both commercial [40] and military [9, 41] vessels. The first generation of electric propulsion systems used DC motors [42, 43]. The utilisation of these motors, however, was limited because of brush and commutator issues. Although improvements have been made to these constraints, they still presently limit the output power of DC motors to approximately 7.5 MW, which is too low for most of today’s marine vessel requirements [10]. Furthermore, DC motors
require continual maintenance and replacement of the brushes, which increases through-
life costs and has therefore discouraged ship manufacturers from using these motors
[10, 42].

Ship manufacturers have instead opted for AC motors, for the most part, due to the
development in high power solid-state switching devices and hence, multi-megawatt
converters. Before the development of converters, the speed of an electric motor
coupled to a propeller was governed by the supply frequency of the vessel. If the speed
of the motor, and hence propeller, needed to be changed, the supply frequency had to be
altered to the desired frequency for the appropriate speed required. This limitation made
electric propulsion systems unappealing for many decades compared to the traditional
mechanical propulsion system [10]. Converters, however, have now allowed electric
propulsion systems to overcome this limitation.

As an example, all cruise ships produced since 1997 by the French Saint-Nazaire
Chantiers De L’Atlantique shipyard have been ordered with electric propulsion systems
[44]. This highlights the acceptance and utilisation of electric propulsion systems in
modern marine vessels. The demand for electric propulsion systems has been driven
largely by cost, which has therefore resulted in promising recent developments in
power-dense electric propulsion AC motors for marine applications.

### 2.2.5 AC Motor Topologies

Presently, three AC motor topologies appear to be under significant development for
use in marine electric propulsions systems, although there are more novel technologies
also being investigated including the superconducting homopolar motor. However, the
three main topologies are the induction motor, the permanent-magnet (PM) motor and
the wound-rotor superconducting motor.

### 2.2.6 Induction Motors

Squirrel-cage induction motors have been utilised for marine propulsion applications
due to their simple mechanical structure. The rotor assembly is composed of a magnetic
core that houses a squirrel-cage, resulting in a rugged construction. Furthermore, they
are relatively inexpensive to manufacture and require little maintenance, which increases their reliability. The axial length of induction motors, however, is typically larger than the other two motor topologies and hence, their weight and volume are also usually larger [45]. Furthermore, their efficiency and power factor are usually lower due to their poorer tolerance to a larger air-gap.

The magnetic flux in an induction motor is developed by passing current through stator coils, which are wound in discrete slots. The generated magnetic flux is carried through the magnetic iron circuit of the stator-core, across the air-gap and through the rotor-core. The rotor-core houses conductors or bars also in discrete slots. There is a balance in the design of induction motors, therefore, between the area of core-back necessary to carry the magnetic flux and the area required for the conductors to carry the current. These effective areas are controlled by the magnetic and electric loadings of the motor, which control the magnetic saturation levels and the joule losses, respectively. If the physical size or losses of the motor are not altered, therefore, the torque output is limited [23].

The electric loading, however, is not as stringent for marine applications as it is for industrial applications because marine motors can typically exploit water for cooling. Since water is a better thermal conductor than air, the electric loading of the motor can be increased, which increases the torque-density of the motor. Furthermore, the physical space envelope is often not as critical in marine applications and since torque-density is usually the biggest key design requirement, it can result in motors with different aspect ratios, i.e. diameter to length. This is because torque is approximately proportional to the air-gap volume, i.e. directly proportional to the length of the motor and the square of the mean air-gap diameter. Therefore, relatively short axial length but large diameter motors, for example, can be designed to produce a large torque-dense motor. Furthermore, the physical dimensions are flexible and can be modified to adapt to specific marine applications.

As an example, over the past 25 years ALSTOM has been developing their Advanced Induction Motor (AIM) for applications where low speed but large torque is required. The AIM is a torque-dense motor, of the order of 20 MW at 180 rpm, and is therefore suitable for marine applications, especially warships where space is limited [46]. Consequently, the AIM was selected for the United States of America Integrated
Propulsion System (IPS) programme in 1995 and also as the main propulsion motor for use on the next generation of Type 45 Destroyers for the British Royal Navy [47].

Although wound-rotor induction motors have also been considered and developed, they have the same limiting factors as the DC motors discussed previously, due to the need for connection to the rotor, which in this topology is via slip-rings and brushes. Furthermore, they occupy a greater volume than a squirrel-cage motor and are more expensive and complicated to manufacture due to the rotor windings. These motors, therefore, have been more limited in marine propulsion applications [10].

2.2.7 Permanent-magnet Motors

The development of rare-earth PMs, which have a remanant magnetic flux density greater than 1.2 T and a coercivity of approximately 1 kA.mm\(^{-1}\) [22], has facilitated the development of PM electric motors. PM motors generally have increased efficiencies and larger torque densities when compared to conventional induction motors [48, 49]. There are three main classes of PM motor topologies presently receiving consideration for use as torque-dense motors for marine propulsion applications, namely radial-flux, axial-flux and transverse-flux motors. Their names are derived from the direction of the magnetic flux path within the motor.

2.2.7.1 Radial-flux PM Motors

The radial-flux motor is the most common class of PM motor currently used in industry [50]. The radial-flux PM motor consists of a cylindrical rotor and stator, the same as in an induction motor. In a PM motor, however, the rotor windings are replaced with PMs, which provide the field excitation. PM motors tend to be more efficient than induction motors because there is no magnetising field current and also no rotor winding losses. There will be an eddy current loss generated in the PMs; however, they are usually segmented to minimise this loss.

Radial-flux PM motors generally have a larger torque output than squirrel-cage induction motors in the same power ratings, mainly because the stator windings do not carry the main field magnetising current. Their efficiency is also slightly higher,
particularly on low load, because there are no magnetising current joule losses. PM motors also have a greater tolerance to a larger air-gap compared to an induction motor, since the air-gap magnetic field is produced by the PMs rather than the stator windings. Furthermore, a larger air-gap tolerance makes PM motors more robust in regards to vibration and shock. The cost to manufacture PM motors, however, is more expensive. This is due to the price of the rare-earth elements used in the manufacture of PMs and also the assembly procedures required to mount them onto the rotor. Another factor that is becoming an increasing issue in the consideration of manufacturing PM motors, is the availability and costs of PM materials. If they become prohibitively expensive, the advantages of a PM motor may be outweighed by these material costs [51, 52].

PM motors must be carefully designed so that damage to the PMs or demagnetisation during operation does not occur, as this will reduce the performance of the motor. Furthermore, the influence of temperature on PMs must also be considered carefully, as this will also reduce the motor’s performance. PM motors, therefore, are generally more complicated to design than induction motors.

Several examples of radial-flux PM motors developed for ship propulsion duties include the work conducted by Jeumont Industries, who have designed and validated a 1.8 MW PM motor [53]; Bianchi et al. [54] who designed a 6-pole, 750 kW interior PM motor and are presently testing a prototype motor, and Krovel et al. [55] who have designed a smaller 100 kW surface-mount PM motor, which is illustrated in Figure 2.7.

![Figure 2.7: Topology of a Radial-flux PM Motor for Ship Propulsion](601)
2.2.7.2 Axial-flux PM Motors

Axial-flux PM motors typically have a large aspect ratio, with the stator and rotor assemblies consisting of disc shaped structures. They are often referred to as disc-type motors, therefore, due to their pancake profile. It is this profile that makes them suitable for marine propulsion applications.

Windings are located on the stator, as before, but wound orthogonally to the shaft, as opposed to the radial-flux motor which is wound parallel to the shaft. Since, the air-gap is also orthogonal to the shaft, torque is generated on the active end faces of the motor. Consequently, the torque now becomes approximately proportional to the cube of the outer radius rather than to the air-gap volume [56], as in a radial-flux motor.

As the diameter of an axial-flux PM motor increases, the stator can accommodate a higher number of poles, which makes these motors suitable for low speed applications and hence, marine propulsion duties. A high pole number allows the core-back material to be minimised, reducing the weight of the motor and at the same time increasing its power-density [57]. The use of PMs on the rotor increases the efficiency of the motor, since there are again no joule losses in the rotor windings, as with an induction motor.

Depending on the torque output required, several stator and rotor discs can be implemented together, creating a modular construction. This configuration results in a number of air-gaps all contributing to the torque, which makes these motors compact yet torque-dense. Therefore, axial-flux PM motors are typically smaller than radial-flux motors but can have a larger power-to-weight ratio and efficiency [50, 56]. An example of an axial-flux PM motor that is capable of being constructed in the modular fashion is illustrated in Figure 2.8.
The advantages of the axial-flux PM motor are the fact that the end-windings are very short. This results in less winding resistance and hence, power loss, which increases the efficiency of the motor [57]. The stator can be constructed without slots, which eliminates the torque pulsations typically caused by the teeth and hence, reduces the acoustic noise of the motor [59]. The slot-less stator design, however, would require that the windings be held onto the stator by some form of banding or glue.

A disadvantage of the axial-flux PM motor, however, is the large windage loss. There is the potential for large windage loss due to the multiple air-gaps, if modular construction is used. The windage loss would not be a fundamental issue in marine applications, however, as the required operating speeds are relatively low and the rotor and stator assemblies would be hermetically sealed, which would create smooth surfaces. An additional disadvantage is that the motor produces axial forces and therefore more specialist and expensive bearings are required such as thrust bearings if a single rotor topology is implemented [60].

Caricchi et al. [22] conducted a design study on a number of motors for ship propulsion with ratings from 1 MW to 20 MW and speed ranges from 120 rpm to 200 rpm. They also compared the weight and volume of a 14 MW axial-flux PM motor with a conventional wound-rotor synchronous motor, and a radial-flux PM motor all of the same power rating. The results are presented in Table 2.1.
Table 2.1: Comparison of the Volume and Weight of Several Motor Topologies for Ship Propulsion [22]

<table>
<thead>
<tr>
<th>Synchronous Motor</th>
<th>Radial-flux PM Motor</th>
<th>Axial-flux PM Motor</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Volume</td>
<td>40</td>
<td>30</td>
<td>16</td>
</tr>
<tr>
<td>Weight</td>
<td>101</td>
<td>78</td>
<td>45</td>
</tr>
<tr>
<td>Saving of volume</td>
<td>-</td>
<td>25</td>
<td>60</td>
</tr>
<tr>
<td>Saving of weight</td>
<td>-</td>
<td>23</td>
<td>55</td>
</tr>
</tbody>
</table>

Table 2.1 illustrates that there is a clear weight and volume saving with an axial-flux PM motor over the other two topologies, especially the wound-rotor synchronous motor. This is due to the efficient utilisation of the active materials in the motor [22]. Consequently, Kaman Aerospace Corporation was selected by the U.S. Navy to develop a 19 MW prototype axial-flux PM motor for the DD(x) destroyer [53].

2.2.7.3 Transverse-flux PM Motors

Transverse-flux PM motors are the most novel class of PM motors currently under development. The rotor is constructed from PMs and pole-pieces positioned in a magnetic flux focussing formation to produce a large air-gap magnetic flux density. Considering a double-sided motor, the rotor rotates between two stator-cores that are positioned on both the inner and outer radial surfaces of the rotor. Both sides of the stator, therefore, contribute to the generation of torque. An illustration of this topology and the resultant magnetic flux paths are presented in Figure 2.9.
As shown in Figure 2.9, the windings are constructed from a simple coil on each side of the rotor. There are no end-windings with this configuration because the whole coil contributes to the main air-gap field. Furthermore, the electric and magnetic circuits are not competing for the same space, as with conventional motors. The transverse-flux motor can operate at a high electric loading, therefore, because of good thermal cooling, which results in a torque-dense motor. However, these motors are known to operate at a relatively low power factor. For example, the authors in [23] claim that a transverse-flux motor loaded with an air-gap shear stress of 120 kN.m⁻² would have a power factor of only 0.4. Consequently, a converter would need to be 250% bigger to supply this motor. This would evidently increase the overall cost of a marine propulsion system utilising a transverse-flux motor.

Following on from the work of Weh et al. [61, 62] Rolls-Royce modified the design of their transverse-flux motor to improve the mechanical aspects and the manufacturability of the motor to make it suitable for use as a marine propulsion motor [63]. This was achieved by using a shorter and less complex rotor rim structure and a stator-core comprising of a C-core arrangement, as illustrated in Figure 2.10.

Rolls-Royce has subsequently developed a motor assembly capable of 20 MW utilising the topology shown in Figure 2.10. This power output is achieved with four rotor discs each having four rotor rims, which are connected to a common shaft. Consequently, there are 16 C-cores and armature coils, and hence, 16 phases. A schematic of the motor assembly is shown in Figure 2.11.
The motor assembly shown in Figure 2.11 results in a motor that has a low torque ripple and hence, low noise and vibration. Furthermore, it has excellent fault tolerance due to the high number of phases. These factors make the transverse-flux PM motor topology suitable for marine applications, particularly military vessels. A disadvantage of this topology, however, is that the motor operates at a low power factor [23, 64]. A further disadvantage is that they are complicated to construct, as illustrated in Figures 2.9, 2.10 and 2.11. This fact would increase the initial capital costs of these motors [65].

2.2.7.4 Summary

PM motors for marine propulsion duties are presently being used and also further developed for a number of marine vessel platforms due to their perceived benefits of quiet operation, high efficiency and torque density. Furthermore, PM motors are considered to be up to 30% more compact and up to 50% lighter than conventional induction motors [66]. Consequently, they have been selected for consideration in the DD(x) destroyer by the U.S. Navy [53], in addition to other marine vessel applications.
2.2.8 Superconducting Motors

Traditional superconducting motors are a type of AC synchronous motor that uses superconducting material for the rotor field windings rather than copper wire. Superconducting motors have the potential to be manufactured at lower costs, are significantly smaller in weight and volume, have less vibration and noise, and have increased operating efficiencies compared to conventional motors [67]. They are a strong candidate, therefore, for marine propulsion motors.

Superconducting material can conduct at a considerably larger current density compared to standard copper wire and hence, produce a stronger magnetic field. In an electric motor, this can be greater than 2 T [68]. The increased magnetic field strength results in an increased torque per-unit volume. The larger current carrying capability of a superconducting material is a result of it having no DC resistance, as long as the temperature, current density and the magnetic field strength are maintained below critical values, as illustrated in Figure 2.12.

![Figure 2.12: Graphical Representation of Superconducting Region of a Superconducting Material [69]](image)

In Figure 2.12, $J_c$ is the critical current density, $H_c$ is the critical magnetic field strength and $T_c$ is the critical temperature. If any one of these critical values is exceeded, the superconducting material will ‘quench’. Quenching is a sudden temperature rise and associated resistive heating in the superconducting material. Once quenched, the
material will behave as a normal conductor and have a finite resistance, as illustrated in Figure 2.13.

![Graph: Resistivity of a Superconducting Material as a Function of Temperature][70]

**Figure 2.13: Resistivity of a Superconducting Material as a Function of Temperature [70]**

The discovery of superconducting materials started with materials, which are referred to as low-temperature superconducting (LTS) materials, as they were cooled and maintained typically around 4 K [67]. This ultra-low temperature required a very complex and expensive refrigeration system, using liquid helium, to maintain the superconducting properties of the LTS material, which consequently inhibited its general use. However, the discovery and development of high-temperature superconducting (HTS) materials that utilise liquid nitrogen as a coolant, which is cheaper than liquid helium, started with the discovery of a HTS material by IBM researchers in 1986 [71].

The research and development of the latest HTS materials, which typically have a $T_c$ of approximately 105 K, has created the opportunity to design new torque-dense electric motors with refrigeration systems that are more cost effective. HTS motors can be up to a third of the volume and weight of a conventional copper wound motor for the same power rating [72], as illustrated in Figure 2.14.
A traditional HTS motor is similar to a conventional wound-rotor synchronous motor; however, there are several differences. Firstly the rotor conductors are made of HTS material, which is fabricated into coils. These coils are mounted onto the rotor and then housed in a vacuum chamber, which is subsequently cooled. Secondly, the stator-core may not have teeth to hold the stator windings, due to the large magnetic fields developed in the motor. Instead, the stator windings are generally held in a non-magnetic and non-conductive core. In some HTS motor designs, the frame of the motor is used as the return path for the magnetic flux, which is laminated to reduce losses, as in conventional motors. An illustration of the topology of a HTS motor is shown in Figure 2.15.

Figure 2.14: Volume and Weight Comparison of a 36.5 MW Copper vs. HTS Motor [72]

Figure 2.15: Topology of a HTS Motor [9]
There are a number of manufacturers who have been developing HTS motors including Siemens, General Electric (GE) and American Superconductor Corporation (AMSC). AMSC designed and built a 5 MW HTS motor suitable for offshore support vessels, product tankers and hybrid propulsion naval vessels, which was successfully tested in 2003 [72, 73]. More recently (2009), they successfully tested a 36.5 MW HTS motor in collaboration with Northrop Grumman Corporation for U.S. Navy ship propulsion applications [74]. The specifications of the two motors produced by AMSC are shown in Table 2.2.

**Table 2.2:** Specifications for the Two HTS Motors Produced by AMSC [72]

<table>
<thead>
<tr>
<th>Output [MW]</th>
<th>5</th>
<th>36.5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Speed [rpm]</td>
<td>230</td>
<td>120</td>
</tr>
<tr>
<td>Torque efficiency [%]</td>
<td>96</td>
<td>97</td>
</tr>
<tr>
<td>Pole-pairs</td>
<td>3</td>
<td>8</td>
</tr>
<tr>
<td>Voltage [kV]</td>
<td>4.16</td>
<td>6</td>
</tr>
<tr>
<td>Armature Current [A&lt;sub&gt;arm&lt;/sub&gt;]</td>
<td>722</td>
<td>1,270</td>
</tr>
<tr>
<td>Phases</td>
<td>3</td>
<td>9</td>
</tr>
<tr>
<td>Power factor</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Frequency [Hz]</td>
<td>11.5</td>
<td>16</td>
</tr>
<tr>
<td>Weight [tons]</td>
<td>23</td>
<td>75</td>
</tr>
<tr>
<td>Dimensions (L × W × H) [m]</td>
<td>2.5 × 1.9 × 1.9</td>
<td>3.4 × 4.6 × 4.1</td>
</tr>
<tr>
<td>Stator cooling</td>
<td>Liquid</td>
<td>Liquid</td>
</tr>
<tr>
<td>Drive</td>
<td>Commercial Marine</td>
<td>Commercial Marine</td>
</tr>
</tbody>
</table>

HTS motors offer a number of advantages including [67, 72]:

- reduced manufacturing costs due to the use of less materials and labour;
- reduced electric losses and hence, increased efficiency; and
- reduced volume and weight, which facilitates utilisation in novel applications not previously possible.

These advantages are why HTS motors are presently being considered for the latest range of naval vessels. There are several disadvantages associated with HTS motors, however, which must also be considered. For example, the external cooling system, i.e. cryostat, which is composed of pumps, compressors, coolers, etc., will take up additional space within the hull and are expensive. The rotor will require a liquid nitrogen rotating cryostat, which is difficult to implement. Furthermore, if the superconducting material quenches during operation, then the motor will normally have
to be disconnected, rendering it useless until the superconducting state is once again attained. Presently, the wound-rotor synchronous motor is the main topology utilising HTS materials for superconducting motors. These motors, however, are more prone to speed instability because the rotor does not have the inherent damping of a conventional induction rotor. The rotor must also be designed to be able to cope with the strong magnetic forces generated by the superconducting windings.

### 2.2.9 Electric Motor Torque-density Comparison

A method to compare the relative torque densities of the electric motor topologies discussed previously was presented by Lewis, using the value of the air-gap shear stress, which is the force per-unit area on the rotor surface due to the motor torque [46]. The air-gap shear stress is calculated as follows:

\[
\sigma_g = \frac{\tau}{2\pi r_r^2 l_r}
\]

where:

- \(\sigma_g\) = Air-gap shear stress \([N.m^{-2}]\)
- \(\tau\) = Torque \([Nm]\)
- \(r_r\) = Outer radius of the rotor-core \([m]\)
- \(l_r\) = Length of the rotor-core \([m]\)

Using Equation 2.1, a comparison of several motor topologies is presented in Table 2.3.

<table>
<thead>
<tr>
<th>Motor Topology</th>
<th>Motor Air-gap Shear Stress (kN.m(^{-2}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Standard large industrial induction motor</td>
<td>13</td>
</tr>
<tr>
<td>High performance industrial 1,500 rpm induction motor</td>
<td>35</td>
</tr>
<tr>
<td>Low speed mill motor (origins of the AIM) (1992)</td>
<td>45</td>
</tr>
<tr>
<td>IPS Motor for US Navy, 19 MW at 150 rpm, (1997)</td>
<td>76</td>
</tr>
<tr>
<td>AIM design, 20 MW at 180 rpm, (2002)</td>
<td>100</td>
</tr>
<tr>
<td>Permanent-magnet Motor</td>
<td>120</td>
</tr>
<tr>
<td>High-temperature Superconducting (HTS) Motor, 25 MW at 120 rpm</td>
<td>340</td>
</tr>
</tbody>
</table>
Table 2.3 shows that the AIM is approaching the air-gap shear stress of the PM motor. However, both the AIM and PM motor topologies are considerably lower than the HTS motor, which highlights the present advantage of the HTS motor in terms of air-gap shear stress over the other two topologies.

2.2.10 Failures of Electric Motors for Marine Propulsion

There have been a number of failures of AC electric motors in marine electric propulsion systems. However, the majority of failures have been a result of thrust and support bearing failures, shaft seal failures and contamination of lubricating oils with water and debris [75]. Consequently, the development of electric motors for propulsion duties on-board marine vessels looks set to continue.

2.2.11 Electric Motor Propulsion Topologies

There are three main classes of marine topologies that utilise electric motors for propulsion duties. These are the pod, the azimuth thruster and the bow thruster.

2.2.11.1 Propulsion Pod

A propulsion pod is a device that merges steering and propulsion duties into a single housing. The concept was first developed by Pleuger et al. in 1955 [76]. The external propulsion pod housing is designed to act as a rudder with an electric motor located inside the housing; the motor drives the propeller via a shaft to provide propulsion. The architecture of a typical propulsion pod manufactured by Rolls-Royce, trade name ‘Mermaid’, is shown in Figure 2.16.
Propulsion pods are usually mounted at the rear of a marine vessel outside the bottom of the hull, as illustrated in Figure 2.17.

To manoeuvre a vessel, the propulsion pod is capable of 360° rotation via slewing gears that are fitted inside the hull above the propulsion pod, although some propulsion pods are also installed as fixed units. In 1990, ABB installed the first propulsion pod to a marine vessel. This had a power rating of 1 MW [75]. The latest designs from ABB, however, are now of the order of 30 MW.

Propulsion pods offer many advantages over the traditional mechanical propulsion system of marine vessels including improved manoeuvrability, increased volume available within the hull and increased hydrodynamic efficiency [10]. Furthermore, propulsion pods can reduce the building costs of a marine vessel, as they are able to be
installed later in the constructional timeline of the vessel. As a result, the latest developments in modularity marine vessel manufacturing can be implemented [36].

### 2.2.11.2 Azimuth Thruster

An azimuth thruster is a device found on marine vessels, which is used for propulsion or positioning duties, depending on the size and thrust requirements of a particular vessel. It is composed of either a controllable- or fixed-pitch propeller housed in an open pod around a propeller, which can be rotated 360° in the horizontal plane; therefore, no rudder is required. There are two variants of the azimuth thruster depending on the shaft arrangement used, i.e. the ‘L-drive’ and the ‘Z-drive’. In the L-drive configuration, the propeller is driven by an electric motor via a vertical input shaft that is connected to a horizontal output shaft through right-angled gears, forming an L shape. In the Z-drive configuration, an additional horizontal shaft and right-angled gears are included, forming a Z-drive, as illustrated in Figure 2.18.

![Figure 2.18: A Z-drive Azimuth Thruster Produced by Rolls-Royce [80]](image)

Azimuth thrusters offer the same advantages as the propulsion pod discussed previously, except that they are not capable of achieving the same output power levels. They are typically installed, therefore, on smaller marine vessels or used as secondary propulsion systems on larger vessels. Azimuth thrusters are diversified in their
implementation in marine propulsion systems; consequently, they are manufactured in a number of constructional topologies including ‘fixed-install’, ‘retractable’ and ‘underwater-mountable’. Fixed-install azimuth thrusters are typically found on tug boats, supply boats and ferries, whereas retractable thrusters are generally implemented as dynamic positioning (DP) propulsion systems or as emergency back-up propulsion systems on military vessels. Finally, underwater-mountable thrusters are again utilised in DP propulsion systems but can be found on larger vessels such as semi-submersible drilling rigs.

Due to the large diameter and relatively short axial length of the azimuth thruster unit, as illustrated in Figure 2.18, they are therefore a suitable candidate for the rim-driven electric machine topology presented in Chapter 1. The implementation of this topology would also have the advantage of eliminating the requirement for the complex mechanical linkage between the electric motor and propeller, i.e. the L- or Z-drive.

2.2.11.3 Bow Thruster

A bow thruster or as they are also known, a tunnel thruster, is a transverse propulsion device that is mounted through the bow of a marine vessel below the waterline, as illustrated in Figure 2.19.

![Figure 2.19: Location of a Bow Thruster On-board a Large Marine Vessel [81]](image)
Bow thrusters are typically found on-board large marine vessels (although they can also be implemented on smaller vessels) and are utilised as a secondary propulsion system. They are used to increase the manoeuvrability of a vessel by allowing it to turn or dock without the aid of a tugboat. Bow thrusters provide bi-directional thrust and can be driven by an electric motor or hydraulically via a hydraulic power pack.

Hydraulic power packs are prone to leaking and have to be rated higher than the desired output power of the bow thruster, to account for inefficiencies in the various components. They are also a potential health and environmental hazard due to the leaking of hydraulic fluid. It is preferential, therefore, to move away from hydraulically driven bow thrusters to an electrically driven equivalent system. As a consequence, bow thrusters are another suitable candidate for the rim-driven electric machine topology presented in Chapter 1, again due to the large diameter and short axial length, as illustrated in Figure 2.20.

![Figure 2.20: Scale of a Typical Bow Thruster On-board a Relatively Large Marine Vessel][82]

### 2.2.12 Controllable- and Fixed-pitch Propellers

A controllable-pitch propeller (CPP) is a propeller that has multiple blades that can be swivelled in position to vary their pitch and hence, vary the thrust developed, as illustrated in Figure 2.21.
The benefit of a CPP over a fixed-pitch propeller (FPP) is the ability to vary the pitch of the propeller blades to the load demands of a marine vessel. FPPs are typically designed to be most efficient at one speed and load condition. As discussed previously, however, most marine vessels rarely operate at a single speed. Furthermore, marine vessels carry different load levels, i.e. full to empty, depending on their chartered requirements. Fuel savings can be achieved, therefore, by adjusting the pitch of the blades to produce optimal propulsion efficiency over an increased range of operating speeds [84, 85]. Furthermore, vessels with CPPs are also capable of implementing emergency manoeuvres safer and faster. This is because a CPP can be rotated to a negative angle of pitch, which generates reverse thrust without changing the rotational direction of the propeller itself. The reverse thrust can be used, therefore, to decelerate the vessel.

Considering these factors, therefore, an option that deserves further research, if the rim-driven electric machine topology is proven to be a viable and successful technology, is the replacement of a FPP with a CPP. For example, if a marine vessel had a requirement to save space within the hull or costs, these requirements could be achieved by replacing the FPP and variable-speed motor propulsion system with a CPP and fixed-speed motor propulsion system. The FPP system is achieved electrically through the control of a motor via a power electronic converter. If variable speed is only required occasionally, however, the power electronic converter wastes valuable space and would also be considered an expensive component of the propulsion system, which could be eliminated with the implementation of a CCP, whilst still retaining the flexibility and functionality of the propulsion system [86].
2.2.13 Summary

The literature has mainly focused on large electric motors for primary propulsion systems on-board marine vessels. Less emphasis has been placed on smaller motors for secondary propulsion systems used for docking or as an emergency back-up system. These secondary propulsion systems typically have a low duty cycle of operation on large vessels.

The majority of the electric propulsion motors described in this chapter require a converter to vary their output speed and hence, the overall system costs inhibit their use in low duty cycle applications where variable-speed is not particularly important. A simple fixed speed motor would be sufficient to fulfil the requirements of a low duty cycle secondary propulsion system. There appears to be a gap in the literature and market place, therefore, for relatively cheap but torque-dense electric motors for use in secondary propulsion systems. Consequently, the rim-drive motor topology presented in Chapter 1 has the potential to fill this gap by providing a simple alternative to a traditional mechanical propulsion system, whilst still retaining the benefits of an electric propulsion system. The rim-drive motor topology is particularly suitable for the azimuth and bow thruster propulsion systems due to their relatively large diameters and short axial lengths.

2.3 Seal-less Pump Application

2.3.1 Background

Centrifugal pumps are implemented in a vast number of industrial processes to transport fluids. There are many types of fluids that need to be pumped in the process industry, from sterile or highly-pure fluids to highly toxic ones, including acids and flammable fluids. One of the fundamental problems with centrifugal pumps, however, is sealing the rotating drive shaft to prevent leaking. This is particularly difficult as a static seal is required to be in contact with a moving interface, which is trying to retain fluids, usually under high pressure. This is known as a dynamic seal. The dynamic seal has to allow a small amount of fluid to leak past the seal, however, to provide lubrication between the faces of the shaft and the seal. The seal wears out over time and as it does,
a larger amount of the pumped fluid escapes. This can be costly in terms of maintenance, production down-time and wasted fluid. Exxon Chemical Ltd., for example, has estimated that 80% of pumps are removed from production because of dynamic seal failures [87].

Although there has been a significant effort from many pump manufacturers to design new sealing solutions for pumps, there has been no solution capable of completely eliminating the leakage of fluid past the dynamic seal, irrespective of how small this may be. Even a minor amount of leaking fluid can create a potential safety hazard if the fluid is ecologically harmful, a danger to occupational health or flammable. As a result, a new type of pump topology – the seal-less pump – was developed. Presently, there are two main pump technologies under development for the transportation of fluids without dynamic seals, namely the ‘magnetic coupling pump’ and the ‘canned motor pump’.

### 2.3.2 Magnetic Coupling Pump

A magnetic coupling pump is a centrifugal pump without dynamic seals. There are, therefore, no shaft penetrations through the pump housing and as a result, no potential for fluid leakage. In one example of a magnetic coupling pump from [88], the impeller/shaft is submersed in the process fluid and a static containment shroud/shell forms a sealed boundary to eliminate any leakage from the pump. The impeller is rotated by the interaction of magnetic fields between an inner and outer ring of PMs. The topology of this magnetic coupling pump is illustrated in Figure 2.22.

![Figure 2.22: Schematic of a Magnetic Coupling Pump [88]](image-url)
There are two topology variants for this particular magnetic coupling pump, which depend on the operational temperature requirement. The ‘synchronous drive’ topology, as shown in Figure 2.23a, is limited to an operational temperature of 260 °C. It consists of a ring of outer PMs that magnetically couple with a ring of inner PMs through the containment shroud/shell. This results in the impeller rotating at a constant speed and torque.

![Figure 2.23: Topologies of a Magnetic Coupling Pump - a). Synchronous Drive, and b). Torque Ring Drive][88]

If a higher operational temperature is required, the ‘torque ring drive’ topology, as illustrated in Figure 2.23b, can be implemented, which can operate up to 450 °C. In this topology the PMs of the outer ring are the same as the synchronous drive topology. The inner ring of PMs, however, are replaced with a torque ring. Voltages are induced in this torque ring, which drive eddy currents and hence, cause rotation.

Both of the topologies – the synchronous drive and the torque ring drive – are coupled to an external prime mover such as an electric motor, gas turbine or air motor to rotate the outer ring of PMs. As PMs are utilised in the design, magnetic coupling pumps can operate at high efficiencies. This generally means, however, that the initial capital costs of these pumps are more expensive when compared to an equivalent standard pump. Their through-life costs are reduced, however, because there is no loss of fluid and the pumps have minimal maintenance requirements, making magnetic coupling pumps an attractive option for the fluid process industry [89].
2.3.3 Canned Motor Pump

A canned motor pump is a machine that generally incorporates an electric motor and pumping impeller in the same housing. There are a few differences, however, in the design of the motor used in a canned motor pump compared to a conventional motor. Firstly, the fluid being pumped surrounds the rotor and can potentially be utilised for cooling and providing lubrication for bearings. However, if the fluid is detrimental to the rotor or if the pump is intended to be operated at a high pressure, the rotor must be protected by encapsulating it with a sleeve or can.

The stator must also be protected by a containment-can for the same reasons. The containment-cans are therefore inserted within the air-gap between the rotor and stator assemblies and are typically manufactured from non-magnetic material, if possible, to minimise the losses of the motor and hence, increase its efficiency.

The canned induction motor pump was not widely accepted when first proposed, as it typically had a poorer power factor when compared to conventional motors. The reduced power factor was due to the increased effective air-gap length required to accommodate the containment-cans; this increased the reluctance of the air-gap and hence, increased the current required to magnetise the air-gap. The increased magnetising current also reduced the efficiency of the motor due to the increased joule losses.

It was the requirement of leak-free pumping systems in the nuclear industry, however, that pushed this technology forward [90]. Canned motor pump technology has subsequently been introduced into other environmentally sensitive industries including the petro-chemical industry, where the pumped fluids are usually flammable and can be ecologically harmful. There are now standard canned motor pumps up to 300 kW for use in the chemical industry [90]. Furthermore, the nuclear industry presently has machines up to 5 MW installed.
There are many designs of canned motor pumps but one particular design is illustrated in Figure 2.24.

![Topology of a Canned PM Motor Pump](image)

**Figure 2.24:** Topology of a Canned PM Motor Pump [91]

Figure 2.24 shows a canned PM motor pump developed by Peralta-Sanchez *et al.* to eliminate the need for a dynamic seal for the reasons discussed previously [91]. They also used PMs on the rotor to increase the power factor and efficiency of the motor. However, there are also induction motor variants using a squirrel-cage rotor or solid rotor [92]. The selection between which motor topology is the most appropriate will depend on the application requirements including efficiency, temperature, safety, cost, etc.

### 2.3.4 Rim-drive Motor Pump Design

A new motor pump topology could be developed by combining a propeller and the rotor of a motor into a single unit, creating the rim-driven electric machine topology described in Chapter 1. Implementing this topology would result in no external shaft; therefore, no dynamic shaft seal would be required, which would eliminate this potential source of fluid leakage. This is especially beneficial with hazardous or expensive fluids, as previously highlighted. Furthermore, the elimination of the dynamic seal would mean that a rim-drive motor pump would be considered as a seal-less pump and not be regarded as a source of fluid release. An industrial working area associated with the rim-drive motor pump, therefore, would not need to be reclassified in terms of safety [90]. Moreover, the removal of the dynamic seal would reduce the maintenance
requirements and also potential down-time associated with their replacement. This would result in a reduction in the through-life costs of the rim-drive motor pump.

By incorporating both the pumping mechanism, i.e. propeller, with the electric motor that is driving it, the pump would be more compact. A rim-drive motor pump design, therefore, could potentially be smaller in volume than both the magnetic coupling pumps and canned motor pump discussed previously.

In the rim-driven electric machine concept, the electric motor is attached to the tips of the propeller and hence, the mean air-gap has a large diameter, leading to a relatively large torque output. A more compact electric motor could be designed and implemented, therefore, reducing the overall cost of the pump, since less material would be required. With suitable containment, the rotor could be submersed directly in the fluid to be pumped, irrespective of its properties, e.g. acidic, flammable, etc., making this topology a viable option in a number of industries. It is considered, therefore, that there is the potential for a novel electric rim-drive motor pump to be developed.

2.3.5 Summary

Fluid process pumps are usually driven by a prime mover that is external to the propeller being driven, therefore requiring a dynamic seal. The dynamic seal leads to some of the pumped fluid being wasted, which also increases safety issues if the fluid is toxic. Furthermore, they are one of the main sources of standard pump failures and hence, increased operating costs.

The literature has shown the development of magnetic and canned motor pumps to try and overcome the issues with the dynamic seal. The rim-driven electric machine topology presented in Chapter 1, however, has the potential to be implemented to eliminate the requirement for the dynamic seal whilst also creating a reliable motor pump, which can be permanently submerged in the fluid being pumped. Again, there appears to be a gap in the literature and marketplace for the development of a seal-less rim-drive motor pump.
2.4 Run-of-the-river Rim-driven Generator Application

2.4.1 Background

A third potential application area that was considered for the rim-driven electric machine topology presented in Chapter 1, is in ‘run-of-the-river’ hydroelectricity projects. A run-of-the-river project utilises the natural altitude drop and flow of a river to generate electricity. They are different from hydro power plants that use dams to flood huge areas of land such as the Three Gorges Dam in China, which is the world’s largest dam and hydro project at 18 GW [93]. Hydro power plants are typically multi-billion pound projects and as such, are prohibitive in remote areas where connection to the national grid is very expensive. Run-of-the-river projects, however, are usually found in remote locations. In these locations, where finances are often limited, small communities reside and only a small power requirement is usually necessary. Run-of-the-river projects, therefore, are an ideal candidate for power generation for these communities.

2.4.2 Rim-driven Generator Concept

A new rim-driven generator topology could be realised through the combination of the propeller and rotor into a single unit. By submerging the electric machine into the river directly, electric power could be generated relatively cheaply. Furthermore, this technology has the potential to limit the environmental impact, as the only disruption would initially be to the river bed to mount the machine (although some form of protective system would have to be implemented such as a mesh to protect river-life from the propeller blades). Furthermore, it would be out of sight once installed, therefore eliminating visual impact.

The electric power that could potentially be generated from a rim-driven generator can be calculated as follows [94]:

\[ P = \frac{\eta \rho AV^3}{2} \]  

(2.2)
where:

\[
P = \text{Output power [W]} \\
\eta = \text{Efficiency of the turbine [%]} \\
\rho_d = \text{Fluid density [kg.m}^{-3}\text{]} \\
A_b = \text{Cross-sectional area of the turbine blades [m}^2\text{]} \\
V = \text{Free-stream velocity [m.s}^{-1}\text{]} \\
\]

Using Equation 2.2, a three metre diameter propeller driving a rim-driven generator operating in water, with a fluid density of 1,000 kg.m\(^{-3}\), a river flow velocity of 3 m.s\(^{-1}\) and a turbine efficiency of 40%, therefore, would yield a theoretical output power of approximately 51 kW, for example. This result, although only theoretical, clearly shows the potential for the rim-driven electric machine topology to be utilised as a run-of-the-river generator. A rim-driven generator with a mean air-gap diameter over three metres, however, could generate far more power than is available from the flow of the river. To reduce the output power, it would be normal to reduce the axial length of the machine, since the diameter is fixed by the size of the propeller. This may lead to a reduction in its structural integrity, given the large diameter of the machine. An alternative rim-driven electric machine topology concept, therefore, could be to implement only a partially active stator, creating a rim-driven arc generator. An illustration of this concept is shown in Figure 2.25.

![Proposed Rim-driven Arc Generator](image)
Figure 2.25 shows that the rim-driven arc generator would have only a partially active stator, which would reduce the overall output of the machine, as only part of the stator-core would carry windings. This design feature would also result in a weight reduction in the machine and therefore the requirements of its support structure. These design features, therefore, would result in a reduction in the overall capital costs of the generating system, making it particularly suitable for remote locations.

### 2.4.3 Summary

Run-of-the-river generators utilise the natural flow of a river caused by altitude drop to generate electricity. As these projects are typically located in remote areas where connection to the national grid is too expensive and where a relatively small power requirement is only necessary, a new machine topology could be developed by combining a propeller and rotor into a single unit with a partially active stator to create an arc rim-driven generator. The rim-driven electric machine topology presented in Chapter 1 has the potential, therefore, to be implemented to create a novel run-of-the-river generator.

### 2.5 Conclusions

This chapter began with a description of the traditional mechanical propulsion system generally used on-board marine vessels and the move towards using more-electric propulsion systems, due to the flexibility, cost savings and overall system efficiency improvements that they offer. A number of different electric motor topologies considered for ship propulsion duties were presented including induction, PM and superconducting motors. Examples of recent developments in each category were also given. The chapter then went on to discuss marine topologies that incorporate electric motors for propulsion duties including the propulsion pod, the azimuth thruster and the bow thruster. It was identified that there is the potential for the development of rim-drive electric motors, for low duty cycle applications in marine environments.

The chapter then presented the challenges in the fluid process industry and the difficulty with leakage from pumps with dynamic seals, and the present development of seal-less
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pumps to combat this limitation. A novel topology, utilising the rim-drive electric motor concept was presented as an alternative to the seal-less pumps presently available.

Finally, a run-of-the-river generator application was also discussed, which highlighted the potential for using the rim-driven electric machine concept in this application to develop a new technology. The design incorporated an arc stator that is only partially active to provide a simple, relatively cheap electric generator for remote areas, where only fairly small power requirements are necessary. In the next chapter, basic magnetic material theory will be presented, along with the identification of materials used in the manufacture of electric machines and the reasons for their choice. This will be followed by an explanation of the finite element analysis numerical technique and an introduction to the software package chosen to conduct the analysis of the electric motors developed within this thesis.
Chapter 3

Background

3.1 Introduction

The previous chapter discussed the potential for the rim-driven electric machine topology to be utilised in a number of applications including secondary marine propulsion systems, seal-less motor pumps and as a run-of-the-river generator. This chapter begins with an introduction to magnetic material theory, presenting the typical materials used in the manufacture of electric machines, the reasons for their choice and an examination of the magnetic hysteresis loop. This is followed by a discussion of permanent-magnet materials, including the history and development of these materials, the reasons for their utilisation within electric machines, their typical demagnetisation curves and the effects of temperature on these materials. The chapter concludes with a description of finite element analysis. Its history and development will be explained, along with an overview of the electromagnetic software package used to model and analyse the electric motors discussed in the subsequent chapters of this thesis.

3.2 Magnetic Materials Theory

There exist a small number of elements in the periodic table including iron, nickel, cobalt and some rare-earth elements including dysprosium and gadolinium, that exhibit a phenomenon known as ferromagnetism. Ferromagnetism refers to the observable fact that these elements exhibit strong magnetic properties. This phenomenon is caused by
the atomic structure of these elements. The magnetic fields created by the orbital spins of their electrons do not self-cancel and hence, are susceptible to external magnetic fields. Experimental results have shown that ferromagnetic materials consist of microscopic regions known as ‘magnetic domains’. Each magnetic domain can contain in the order of $10^{17}$ to $10^{21}$ atoms in a volume of about $10^{-12}$ to $10^{-8}$ m$^3$ [95]. The specific direction of magnetisation of each magnetic domain, in a previously unmagnetised piece of material (or one that has been systematically demagnetised), is randomly orientated, as illustrated in Figure 3.1.

![Magnetic Domain Orientation](image)

**Figure 3.1:** Magnetic Domain Orientation [96]

Although the illustration in Figure 3.1 is two-dimensional (2-D), the phenomenon is in fact three-dimensional (3-D). As a result of the random orientations therefore, a ferromagnetic material has no net magnetisation outside of its own volume. Within each magnetic domain, the magnetic dipoles are aligned and each magnetic domain is separated by a thin region known as a domain wall. Figure 3.2 illustrates the dipole orientation through the domain wall of adjacent magnetic domains.

![Dipole Orientation Through a Domain Wall](image)

**Figure 3.2:** Dipole Orientation Through a Domain Wall [96]
If an external magnetic field is applied to a ferromagnetic material, however, some of the magnetic domains will grow in size at the expense of neighbouring magnetic domains, as they try to align themselves in parallel with the externally applied field. Figure 3.3 illustrates this effect as the magnetic field strength is increased.

![Figure 3.3: Magnetic Domain Size Variation with - a). No External Magnetic Field, b). Weak External Magnetic Field, and c). Strong External Magnetic Field [96]](image)

As the magnetic field strength, $H$, is increased there will be a resultant change in the magnetic flux density, $B$, of a ferromagnetic material. If a sinusoidal cycle of magnetisation is followed, a ferromagnetic material will exhibit the sigmoid shape illustrated in Figure 3.4.

![Figure 3.4: General Magnetic Hysteresis Loop [97]](image)
The dashed line, ‘oa’, represents the initial path that an unmagnetised piece of ferromagnetic material will track as the strength of an external magnetic field is increased. At point ‘a’, the vast majority of the magnetic domains have been aligned with the external magnetic field and the material is said to be saturated. Once saturated, the material will exhibit little gain in magnetic flux density to any increase in magnetic field strength. If the magnetic field strength is reduced to zero, the material will retain some magnetic flux density; this is known as the ‘remanent magnetic flux density’, $B_r$ (represented by point ‘b’ in Figure 3.4). This property of ferromagnetic materials makes permanent-magnets (PMs) possible.

To remove the remanent magnetic flux density entirely, a reverse magnetic field has to be applied to the material. This is known as the coercive force or more appropriately, the coercive field strength, $H_c$ (point ‘c’). If the magnetic field strength continues to increase in the negative direction, the material will once again saturate, represented by point ‘d’. Reducing the magnetic field strength again to zero, results in a negative remanent magnetic flux density remaining in the material (point ‘e’). To remove the negative remanent magnetic flux density, a positive coercive force is then applied to the material (point ‘f’). Finally, if the magnetic field strength is increased until the material is once again saturated, the full sigmoid shape of Figure 3.4 will be completed.

A change in the magnetic flux density will always lag behind a corresponding change in the magnetic field strength. This is due to defects in the crystal lattice of ferromagnetic materials and is known as magnetic hysteresis. Consequently, the closed sigmoid shape of Figure 3.4 is referred to as a hysteresis loop. The area of the hysteresis loop is directly related to the energy dissipated in the material for each cycle round the loop.

A ferromagnetic material that is easy to magnetise and demagnetise exhibits a narrow hysteresis loop and hence, has a small loop area. This is referred to as a soft magnetic material. Iron is an example of a soft magnetic material and as such, is used in electric machines to minimise the hysteresis loss. Conversely, a material that is hard to magnetise and demagnetise exhibits a wide hysteresis loop and hence, has a large loop area. This is referred to as a hard magnetic material. PMs typically fall into this category because of their high resistance to being demagnetised. An illustration of the hysteresis loops for a soft and hard magnetic material is presented in Figure 3.5.
3.3 Permanent-magnet Materials

The first discovery of a PM material was the naturally occurring iron oxide mineral, magnetite, $Fe_3O_4$, which was also known as ‘lodestone’ (or loadstone). There are early literary references to it in Chinese texts dating back to 800 BC [98] and by the Greek philosopher Thales (c. 600 BC) [99]. The name lodestone comes from the Anglo-Saxon meaning of ‘leading stone’, or literally ‘the stone that leads’, as its first application was thought to be in a compass [100]. Since that time, however, the use of PM materials has continued to advance with the latest developments in rare-earth PMs, namely, samarium-iron-nitride ($Sm_2Fe_{17}N_3$), which has properties that can theoretically surpass neodymium-iron-boron [101].

There are a variety of PM materials and alloys presently available and utilised in electric machines. However, they can generally be categorised into three main groups, namely: Alnico, ceramic (ferrite) and rare-earth PMs [102]. The history and development of each group is discussed below, along with their magnetic characteristics.

3.3.1 Alnico Permanent-magnets

In 1930, the modern history of PM materials commenced when I. Mishima from Japan developed the Alnico PM [103]. Alnico PMs are manufactured from an alloy composed of aluminium (Al), nickel (Ni) and cobalt (Co) and hence, the ‘al-ni-co’ name is derived from the chemical symbol of each element. There are two varieties of Alnico PMs; the
second contains the element iron (Fe), in addition to the primary elements, even though they are commonly named.

The advantages of this new PM material were its high remanent magnetic flux density of approximately 1.3 T and its high Curie temperature (~ 850 °C). Unfortunately, Alnico PMs have a low coercive force, which makes them easily susceptible to demagnetisation. They also have the undesirable property of a non-linear demagnetisation curve. Due to their relatively low cost, however, they were the first modern PM material to be mass produced, creating the possibility for major developments in electric machines. Alnico PMs were used extensively until the development of ceramic PM materials in the 1950s.

3.3.2 Ceramic (ferrite) Permanent-magnets

In 1952, J.J. Went et al. from the Phillips Company developed the first commercially available ceramic PM material. There are two varieties: barium ferrite, $\text{BaFe}_{12}\text{O}_{19}$, and strontium ferrite, $\text{SrFe}_{12}\text{O}_{19}$, and hence, they are more commonly referred to as ferrite PMs.

Ferrite PMs had a weaker remanent magnetic flux density (approximately 0.4 T) and a lower Curie temperature (~ 450 °C) when compared to Alnico PMs. They offered several advantages, however, including a linear demagnetisation curve, larger coercive force, and a high material resistance. This high resistance was beneficial in AC applications, as it reduced the eddy current loss in the PM material, which generally increased the efficiency of the technology utilising the PMs. Ferrite PMs have been the most extensively used PMs to date, simply because there is an abundance of their basic raw materials.

3.3.3 Rare-earth Permanent-magnets

In the 1960s there was a major development in PMs when rare-earth elements such as samarium and neodymium were used in alloy compounds. These PM materials exhibited magnetic properties that were the strongest to date. They came to be known as
'rare-earth' PMs. The two most common forms of rare-earth PMs at present are samarium-cobalt and neodymium-iron-boron, which are discussed below.

3.3.3.1 Samarium-cobalt Permanent-magnets

In 1966, Dr. Karl J. Strnat of the U.S. Air Force Materials Laboratory at Wright-Patterson Air Force Base developed the first magnetic material composed of a rare-earth element, namely, samarium. The first form was samarium-cobalt (SmCo5). Later in 1972, a second composition (Sm2Co17) was developed, which had better temperature properties than the former material. These rare-earth PM compositions offered a considerable increase in coercive force compared to both Alnico and ferrite PMs. Other advantages included a linear demagnetisation curve and a high energy product, (B·H)max. Unfortunately, due to the inconsistent supply of cobalt and the high cost of samarium, neodymium-iron-boron has become the common choice in applications requiring high-energy PMs, although samarium-cobalt PMs are still used in many high temperature applications.

3.3.3.2 Neodymium-iron-boron Permanent-magnets

In 1983, General Motors and Sumitomo Special Metals almost simultaneously developed the second generation of rare-earth PM material, namely, neodymium-iron-boron (Nd2Fe14B). The two companies, however, used two different processing methods to manufacture the PM material, even though both have similar compositions. General Motors used a melt-spinning process whereas Sumitomo Special Metals used a sintering process.

Neodymium-iron-boron PMs offered superior characteristics when compared with all earlier PMs. Its advantages included a higher remanent magnetic flux density and coercive force. Consequently, this resulted in the highest energy product from a PM at that time. The main disadvantages of this PM material, however, were its low Curie temperature (~ 250 °C) and the corrosion and oxidation of the material due to its high iron content.

The demagnetisation curves of the PM materials discussed above are illustrated in Figure 3.6. In addition, Table 3.1 lists the principal properties of each material.
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Background

Figure 3.6: Typical Demagnetisation Curves for Several PM Materials [102]

Table 3.1: Principal Properties of Several PM Materials (@ 20 °C) [104]

<table>
<thead>
<tr>
<th>Property</th>
<th>Alnico Magnets</th>
<th>Ferrite Magnets</th>
<th>Sm-Co Magnets</th>
<th>Nd-Fe-B Magnets</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>B_r</td>
<td>0.6 – 1.35</td>
<td>0.35 – 0.43</td>
<td>0.7 – 1.05</td>
<td>1.0 – 1.3</td>
<td>T</td>
</tr>
<tr>
<td>H_{ci}</td>
<td>40 – 130</td>
<td>180 – 400</td>
<td>800 – 1500</td>
<td>800 – 1900</td>
<td>kA.m^{-1}</td>
</tr>
<tr>
<td>Magnetising force</td>
<td>200 – 600</td>
<td>600 – 1700</td>
<td>1600 – 4000</td>
<td>2000 – 3000</td>
<td>kA.m^{-1}</td>
</tr>
<tr>
<td>(B-H)_{max}</td>
<td>20 – 100</td>
<td>24 – 36</td>
<td>140 – 220</td>
<td>180 – 320</td>
<td>kJ.m^{-3}</td>
</tr>
<tr>
<td>Max. working temp.</td>
<td>500 – 550</td>
<td>250</td>
<td>250 – 300</td>
<td>80 – 200</td>
<td>°C</td>
</tr>
<tr>
<td>Curie temp.</td>
<td>850</td>
<td>450</td>
<td>700 – 800</td>
<td>310 – 350</td>
<td>°C</td>
</tr>
<tr>
<td>B_r temp. coeff.</td>
<td>-0.01 – -</td>
<td>0.2</td>
<td>-0.045 – -</td>
<td>-0.08 – -</td>
<td>%,°C^{-1}</td>
</tr>
<tr>
<td>H_{ci} temp. coeff.</td>
<td>0.02</td>
<td>0.2</td>
<td>0.05</td>
<td>0.15</td>
<td>%,°C^{-1}</td>
</tr>
</tbody>
</table>

3.4 Utilisation of PMs in Electric Machines

The utilisation of a PM in an electric machine is governed by its hysteresis loop. The most important part of this loop is found in the second quadrant (upper left-hand quadrant), known as the demagnetisation curve. An illustrative PM demagnetisation curve is shown in Figure 3.7.
Referring to Figure 3.7, if a PM is inserted into an external circuit (i.e. an electric machine) and a reverse magnetic field applied, the operating point of the PM will descend down the hysteresis loop to point ‘K’, for example. This will result in a reduction of the remanent magnetic flux density available from the PM. If the reverse magnetic field is subsequently removed, the PM will regain some of its remanent magnetic flux density by following a minor hysteresis loop to point ‘L’. If this cycle is continually repeated, a full minor hysteresis loop will be tracked, as illustrated in Figure 3.7. This minor hysteresis loop, however, can be replaced with little error by a straight line, known as a ‘recoil line’.

If the PM is subsequently subjected to a reverse magnetic field of larger intensity than was previously applied, the PM will descend down the demagnetisation curve past point ‘K’. Once the field is removed, a further reduction in the remanent magnetic flux density will occur. It is for this reason that linear demagnetisation curves are desirable in PMs. Furthermore, the remanent magnetic flux density remains relatively constant during operation unless very high overload magnetic fields or temperatures occur.

Another factor that requires consideration when utilising PMs in electric machines, is that all PMs will exhibit changes to their intrinsic magnetic properties with changes in
operating temperature. Figure 3.8 shows the effect of temperature on a sample of samarium-cobalt, for example.

There is a temperature for all PMs, known as the Curie temperature, at which point the magnetisation of the PM will be diminished to zero. The PM can be re-magnetised if no alterations to the metallurgical composition of the materials has taken place. However, if major metallurgical changes have taken place, the materials may be rendered non-magnetic.

It is very important, therefore, not to operate a PM close to its Curie temperature. It is for this reason that PM material manufacturers specify a maximum working temperature for each material they produce. As an example, if PMs were used in an electric machine and operated close to or even past their Curie temperature, then the operation, output and efficiency of the machine would generally be compromised.
3.5 Finite Element Analysis

3.5.1 Introduction

Finite element analysis (FEA) is the practical application of the finite element (FE) method; a numerical technique developed to model complicated engineering problems. The FE method is used to obtain numerical solutions to partial differential equations and integral equations that describe engineering or physical models that are too large or complex to be solved with traditional analytical techniques. The FE method reduces a problem, which is continuous, by imposing boundary conditions such as ‘Neumann’ and ‘Dirichlet’ boundaries. These boundary conditions reduce the problem to a finite set of equations and hence, yield an approximation to the exact solution. The approximation can be improved by increasing the number of equations but this increases the computational time required to determine a solution.

3.5.2 History

The FE method was originally developed in the early 1940s by Hrennikoff [106] and Courant [107], to overcome limitations with previous numerical techniques such as the ‘finite difference method’. The finite difference method struggled to cope with irregular geometric problems and complex material boundaries because it sub-divided an engineering or physical problem, known as the ‘solution domain’, into a simple regular grid of squares or rectangles. The use of a rectangular grid reduced the accuracy of the method, particularly in areas with curved surfaces, for example.

The advantage of the FE method was its ability to divide the solution domain into a discrete set of finite, usually triangular (but also rectangular if appropriate) ‘elements’, rather than a grid. The triangular elements could be applied to the contours of a problem more precisely, yielding more accurate results. The process of dividing the solution domain into a discrete set of elements, or ‘discretisation’, is now commonly referred to as ‘mesh generation’. It was this mesh generation, along with the ability to vary the density of elements at critical points in the problem domain, which increased the flexibility of the FE method.
The FE method was initially used to analyse structural and elasticity problems in the aircraft industry. It was not until the work of Winslow [108] and the subsequent work of Zienkiewicz et al. [109] in the late 1960s, that the FE method was used in electrical engineering to analyse electromagnetic problems; with more specific research into electric machines beginning in the early 1970s [110-112]. The benefits of the FE method in analysing electric machines are:

- the ability to mesh complicated geometries such as rotor and stator teeth, and curved surfaces including the air-gap;
- the ability to increase the mesh density in critical areas of interest where the field is varying rapidly such as the air-gap;
- the ability to solve for non-linear material properties;
- the ability to calculate induced eddy currents in solid materials, which are a result of time-varying electric and magnetic fields;
- the ability to combine the model with an external circuit to provide constant or time-varying solutions [113, 114]; and
- the ability for the FE model to be solved in conjunction with mechanical and thermal models.

Considering these factors and also the continued development of computer processing power (which is still increasing), the ability to model and analyse electric machines in great detail and in a short space of time, has become almost routine.

3.5.3 2-D versus 3-D FE Modelling

All electric machines are inherently 3-D in nature. Although they can be modelled in 3-D, the computational time required to solve a 3-D model is vastly increased compared to a 2-D model. Furthermore, the data generated can be excessive. In most instances, however, an electric machine can be simplified by ignoring field variations in one coordinate direction and hence, the problem can be represented in 2-D whilst still producing results of sufficient accuracy. Consequently, 2-D analysis is usually preferred in the early stages of a machine design when an iterative process is needed; only using 3-D analysis, if necessary, during the final stages of the design or when the problem cannot be approximated in 2-D with good results.
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The approach in this thesis is to utilise 2-D FE modelling via dedicated electromagnetic software, to design and analyse electric machines. A prototype rim-drive machine will subsequently be built to verify the FE modelling predicted results. There are a number of electromagnetic FEA software packages available for the design and analysis of electric machines including ‘Opera’, ‘FEMM’, etc. However, the software used in the development of the motors presented in this thesis is ‘Flux2D’.

3.5.4 Flux2D FEA Software Package

The FE modelling and analysis detailed in this thesis has been undertaken using the Flux2D software package (version 10) from Cedrat Group. Flux2D is an integrated computer aided engineering package, designed for electrical engineering problems, which can be used to perform electromagnetic and thermal-physical simulations. It is capable of a large number of functionalities including extended multi-parameter analysis, advanced electrical circuit coupling and also kinematic coupling. Furthermore, it can conduct static, harmonic and transient analyses. Flux2D comprises three main modules, namely the pre-processor, solver, and post-processor.

3.5.4.1 Flux2D Pre-processor

The Flux2D pre-processor is a graphical user interface environment, where the geometry of the model to be solved is created. Firstly, the physical dimensions of the model are entered to create 2-D face regions. Once the geometry is completed, all the face regions are typically meshed using the mesh generator. The mesh generator provides different mesh types and meshing technologies. For example, triangular elements can be obtained by ‘Delaunay type’ or ‘advancing front method’ algorithms whereas quadrilateral elements can be generated through either mapped or extrusive meshing [115].

Meshing can be quickly achieved by the user by using the automatic mesh generation tool and linking meshes between identical geometric entities. Furthermore, the user can manually adjust the mesh size and distribution as required. Secondly, a circuit model is created, which explains the properties and configuration of the stator and rotor windings. Finally, the physical properties of the materials are entered including electric
and magnetic properties, and assigned to the appropriate face regions created within the FE model. An illustration of the pre-processor graphical user interface environment is presented in Figure 3.9.

*Note: This Figure does not show an illustration of a FE model investigated within this thesis, since rim-drive motors are more difficult to graphically represent due to their relatively large diameter.*

**Figure 3.9:** Flux2D Pre-processor Graphical User Interface Environment

### 3.5.4.2 Flux2D Solver

The Flux2D solver is a graphical user interface environment for the numerical implementation of the FE method. The solver is fully parametric, allowing a user to define geometrical or physical parameter sweeps. The solver contains a number of different linear and non-linear solvers depending on the model to be solved and even allows a user to define their own linear solver. The solver utilises embedded electromechanical solving, coupling circuit equations and also rigid body motion, solving the motion equations at each time-step with automatic re-meshing around moving parts.
[115]. An illustration of the solver graphical user interface environment is presented in Figure 3.10.

![ Flux2D Solver Graphical User Interface Environment ]

**Figure 3.10:** Flux2D Solver Graphical User Interface Environment

### 3.5.4.3 Flux2D Post-processor

The Flux2D post-processor is the final environment and is used to obtain the results generated from the solver. The post-processor is also fully multi-parametric, enabling a user to analyse the results from multi-parametric solutions. The post-processor can present an extensive range of quantities including [115]:

- vector potential, magnetic flux density, temperature, electric and magnetic fields;
- iron loss in electric steels and joule loss in conductors;
- electric component quantities: voltage, current, power, inductance, etc.; and
- mechanical quantities: position, velocity, force, torque, speed, etc.
Furthermore, the results can be presented in a number of different ways including:

- colour shaded maps and ‘isovalue’ plots;
- vector plots;
- 2-D curves as a function of a varying parameter on a path, grid, etc.;
- spectral analysis; and
- extensive export capabilities including Excel and text, etc.

An illustration of the post-processor graphical user interface environment is presented in Figure 3.11.

*Note: This Figure does not show an illustration of a FE model investigated within this thesis, since rim-drive motors are more difficult to graphically represent due to their relatively large diameter.*

*Figure 3.11: Flux2D Post-processor Graphical User Interface Environment*
3.6 Conclusions

This chapter began with an introduction to magnetic material theory, presenting ferromagnetic materials and explained the reasons for their choice, and utilisation in the manufacture of electric machines. This was followed by a discussion of PM materials including the history and development of these materials from Alnico PMs in the 1930s to the present day rare-earth PMs. The reasons for their use within electric machines were explained, including a graphical illustration of their demagnetisation curves and the effect of temperature on a sample of PM material. The chapter concluded with a description of finite element analysis, explaining its history, development and application to 2-D and 3-D FE modelling. Finally, the Flux2D software package, which is used to model and analyse the electric motors discussed in the subsequent chapters of this thesis was also discussed, presenting the key environments of the software package and its available functionality. The next chapter will present the work conducted on three concept studies utilising the rim-driven electric machine topology for different industrial applications.
Chapter 4

Rim-drive Topology Concept Studies

4.1 Introduction

The previous chapter provided an introduction to magnetic materials used in the design and manufacture of electric machines. This was followed by a description of the finite element analysis (FEA) numerical technique and an introduction to the software package chosen to conduct the simulations of the rim-drive designs that will be presented in this chapter.

This chapter will present the work conducted on three electric motor concept studies utilising the rim-drive motor topology for different industrial applications. An overview of the key parameters for each concept design, along with the FEA predicted performance results will be given. A full design study, however, will be presented in Chapters 5 and 6, where a prototype rim-drive motor is designed, analysed, manufactured and tested.

The first concept study that will be discussed in this chapter will be a line-start permanent-magnet (LSPM) rim-drive motor. A description of the LSPM synchronous motor will be given, along with typical rotor configurations, highlighting the key advantages and disadvantages of each topology. An introduction to the canned LSPM motor concept will then be presented, followed by the analysis conducted on the synchronisation process of this type of motor. The effects of altering the frequency,
conducting-can thickness and position of the PMs in the rotor-core on the line-start and synchronisation capabilities of the motor are then assessed.

The second concept study that will be presented is a seal-less rim-drive motor pump designed for use in the nuclear industry. A comparable industrial motor is chosen and initially modelled in Flux2D. This is used as a benchmark for comparison purposes. The benchmark FE model is subsequently modified to meet the required specifications of the desired seal-less rim-drive motor pump and simulations are conducted to determine its predicted operating performance. The predicted results are compared to the test data results from the industrial motor and conclusions are drawn.

The final concept study that will be investigated in this chapter is a drop-down azimuth rim-drive electric thruster for use as an emergency marine propulsion system. Again, an industrial motor is selected and used as a benchmark to compare the predicted performance of the rim-drive motor design.

### 4.2 Rim-drive Marine Propulsion Motor

#### 4.2.1 Introduction

Rolls-Royce Marine has developed a PM rim-drive motor for use as a thruster for the commercial marine vessel market. A PM motor topology was selected due to its tolerance to a larger air-gap, increased torque density and higher efficiency, when compared against an equivalent induction motor solution. The PM motor is coupled with a fixed-pitch propeller (FPP) to create the thruster.

The speed and hence, torque of the PM motor are adjusted by a power electronic converter. The in-board footprint within the hull and the associated cost of a full-rated converter, however, offsets the benefits of the electric rim-driven thruster in applications where controllable-pitch propeller (CPP) operation is an option or preferred [116]. A direct-on-line (DOL) PM rim-drive motor without the requirement of a fully-rated converter, therefore, would enhance the market by providing both FPP and CPP rim-driven electric thruster solutions. The removal of the converter would reduce the
overall system costs and the overall propulsion system would also require less space within the hull of a marine vessel.

Environmental containment is required on both the rotor and stator due to the operation of the motor in a sea-water environment. The containment on the rotor, however, can be altered to provide two beneficial effects. By using a conducting material rather than a conventional non-conducting material, an induction cage can effectively be created to produce starting torque from a vessel’s fixed frequency supply, in addition to providing containment.

The CPP configuration also offers the advantage of being able to swivel the propeller blades into a non-attacking angle to reduce the starting load on the rim-drive motor. Rolls-Royce Marine, therefore, commissioned a feasibility study to design a DOL LSPM rim-drive motor, which would be compared to their FPP converter controlled PM motor solution.

### 4.2.2 Line-start Permanent-magnet Motor

A conventional LSPM motor is an electric machine that is composed of a squirrel-cage and PM excitation system both incorporated into the rotor-core. The machine starts as an induction motor from a fixed frequency supply, using the squirrel-cage to generate asynchronous torque, which accelerates the motor up to almost synchronous speed.

The PM excitation system interacts with the stator magnetic field producing a pulsating torque, in addition to the steady torque generated by the squirrel-cage. The pulsating torque is caused by the PM field interacting with the stator field as the rotor accelerates. As the speed of the motor approaches synchronous speed, the magnitude of the pulsating torque is hopefully sufficient to ‘pull’ the motor into synchronisation, after which time the machine will run as a synchronous motor with a high power factor and operating efficiency. An illustration of the simulated run-up response of a FE modelled LSPM motor is shown in Figure 4.1.
4.2.3 Synchronisation Process

Figure 4.1 shows the rotor speed oscillating with increasing amplitude as it accelerates towards synchronous speed. To successfully synchronise, the rotor speed will initially overshoot due to the large pulsating torque produced by the interaction of the stator and rotor magnetic fields, as illustrated at approximately 0.22 s. After the initial overshoot, the oscillations will decay to zero as the motor synchronises, after which time the machine will run as a synchronous motor (as shown at approximately 0.6 s).

The most fundamental aspect for the successful operation of a LSPM motor is the synchronisation process. Using a classical d-q model, Miller [117] identified that the key parameters for successful synchronisation are the maximum load torque and the combined motor/load inertias. Rahman et al. [118] also determined the maximum load torque for successful synchronisation but by means of an analytical solution. Cheng et al. [119] further developed Miller’s work to include the rotor resistance and pulsating torque magnitude during the pull-in process. All of these papers, however, assumed a classical squirrel-cage motor topology. The analysis of the effect of removing the squirrel-cage and replacing it with a simple conducting-can, mounted over the outer...
surface of the rotor-core, would be of benefit to the development of this type of motor configuration.

4.2.4 LSPM Motor Topologies

With the arrival of rare-earth PMs in the 1980s, LSPM motors started to be developed to replace even the most efficient low power induction motors (typically between 100 W and 1000 W). The perceived benefits were that LSPM motors had higher efficiencies than standard induction motors due to their synchronous operation. LSPM motors could also be designed to operate with a power factor closer to unity due to the PM field excitation. These factors typically resulted in reduced operating costs, although the initial capital costs of these motors was more expensive. Their starting capability, however, was reduced due to the drag torque caused by the interaction of the PMs with the stator windings.

There has been extensive analysis of both single-phase and three-phase LSPM motors covering all the main types of machine analysis in the literature including steady-state performance calculation models [48, 49, 120, 121], linked electrical and mechanical dynamic models in the d-q reference frame [117, 122-124] and 2-D FEA used directly or combined with d-q or steady-state models [125-127].

A review of the literature reveals that there are two main LSPM motor topology configurations, which are related to the design of the rotor; the stator typically being a standard induction motor arrangement with either a distributed or concentrated winding excitation system. The two rotor configurations can be separated into two classes: PMs buried into the rotor-core and PMs mounted around the surface of the rotor-core.

4.2.4.1 Buried LSPM Motors

A large majority of the prior literature relates to the buried PM configuration. In this arrangement, PMs are buried into the rotor-core along with an induction cage. Two examples of motors that have been proposed previously with the buried rotor configuration are illustrated in Figure 4.2.
The advantage of the buried PM rotor configuration is that the magnetic flux from the PMs can be concentrated, resulting in a larger air-gap magnetic flux density and hence, increased torque. Furthermore, the PMs are protected somewhat from demagnetisation, as they are buried into the rotor-core. This topology compromises the design of the motor, however, since both systems, i.e. the PMs and the squirrel-cage, are competing for the same space within the rotor-core. The complexity of the rotor-core makes the buried PM rotor configuration difficult and expensive to manufacture, which has limited its widespread implementation in industrial applications.

### 4.2.4.2 Surface-mount LSPM Motors

The second topology of LSPM motors is where the PMs are mounted around the outer surface of the rotor-core. Again, there are two rotor configurations, which are dependant on the placement of the PMs. The first rotor configuration is the surface-mount LSPM motor. In the surface-mount configuration, the PMs are located on the surface of the rotor-core with a squirrel-cage positioned below the PMs in the rotor-core, as illustrated in Figure 4.3.
The advantage of the surface-mount PM rotor configuration is the simplicity with which the rotor can be manufactured and assembled, resulting in a cheaper LSPM motor. The PMs, however, are now directly exposed to the stator excitation field and could therefore be fully or partially demagnetised if the motor is not designed correctly. Furthermore, the PMs are mounted to the surface of the rotor by bonding them in place, typically with glue. There is the potential, therefore, for the PMs to become detached at high operating speeds under the influence of centrifugal force. The detachment of the PMs can be circumvented, however, by using a containment system over the PMs such as carbon fibre banding or a sleeve.

The second rotor configuration is the inset LSPM motor. In the inset motor configuration, the PMs are buried just below the outer radius of the rotor-core, along with a squirrel-cage. Figure 4.4 illustrates an example of the inset PM rotor configuration.
The advantage of the inset PM configuration over the surface-mount PM configuration is that the rotor-core is moderately salient, which creates a reluctance torque. This reluctance torque is in addition to the asynchronous torque generated by the squirrel-cage and the pulsating torque generated by the PMs. The disadvantage, however, is that the inset PM configuration is more expensive to manufacture.

4.2.4.3 Canned PM Motors

The earliest reference found of a canned PM motor is the work conducted by Binns et al. [128]. This paper investigated the effects of using either a non-magnetic (Inconel) or a ferromagnetic-can (or sleeve) mounted over the surface of a rotor-core. The rotor-core had PMs buried within it and consequently, the output speed of the motor was limited by centrifugal force. The can was implemented to contain and hence, strengthen the rotor. The introduction of the can reportedly increased the operational speed of the motor up to 5,000 rpm. Binns et al. explained that introducing the Inconel-can reduced the iron losses of the motor since the reluctance of the air-gap was increased. The ferromagnetic-can, however, increased the air-gap magnetic flux density and therefore the torque of the motor when compared to the Inconel-can version.

In a subsequent paper, Binns et al. [129] looked at the effects of varying the radial thickness of the can. It was established that as the can thickness was increased, the air-gap magnetic flux density and also torque were consequently reduced. It is evident from these papers that the can material and thickness are of considerable importance when designing a canned PM motor.

4.2.5 Canned LSPM Rim-drive Motor Concept

A variation from the LSPM rotor configurations and the canned PM motors just discussed is to completely remove the squirrel-cage and replace it with a conducting-can mounted on top of the PMs, simplifying the design and manufacture of the rotor-core. This concept is illustrated in Figure 4.5.
The previous examples of canned PM motors used the can for containment purposes only. The first known example of using a conducting-can to produce torque directly was the work presented by Peralta-Sanchez et al. [91]. In this paper, they developed a motor pump for the fluid process industry to pump hazardous liquids. A can of copper was inserted between the rotor-core and a protective containment-can, creating a LSPM motor. This was initially a small motor, only 2.5 kW, which was further developed to produce a 10 kW version [130]. These papers proved the validity of the canned LSPM topology. No study, however, has been found within the literature implementing the topology at higher output power ratings than presented in [91, 130] or for industrial applications other than pumps. Furthermore, a LSPM rim-drive motor does not appear to have been investigated.

A LSPM rim-drive motor incorporating a conducting-can has several apparent benefits, including:

- a can composed of conducting material could potentially replace a typical squirrel-cage and hence, produce induction torque;
- in the event of a strong centrifugal force, the conducting-can could be additionally utilised as a mechanical containment system to keep the PMs in place;
- the manufacture of the rotor is greatly simplified with the removal of the squirrel-cage, reducing costs; and
- the conducting-can could be used to hermetically seal the rotor, allowing the motor to be submersed in liquids.
These benefits suggest that the LSPM rim-drive motor configuration, incorporating a conducting-can, is an ideal candidate for use as a marine propulsion motor or as a seal-less motor pump. It must be noted, however, that the stator may also need to be hermetically sealed but a non-conducting-can could be implemented instead, using epoxy resin or carbon fibre banding, for example, to maintain efficiency.

4.2.6 Canned LSPM Rim-drive Motor Design

As discussed previously, Rolls-Royce Marine had developed a power electronic converter controlled, FPP rim-drive PM motor thruster. Some of the specifications and preliminary testing results for this motor were available. A FE model was therefore created using the Flux2D software package.

4.2.6.1 Benchmark Rim-Drive PM FE Motor Model

Rolls-Royce Marine provided the basic specifications for their 800 kW FPP rim-drive PM motor, to allow a FE model to be created, which could be subsequently used as a benchmark for the canned LSPM rim-drive motor being developed. The PM motor specification parameters are provided in Appendix A. The parameters presented in Appendix A were used to create the Flux2D FE model shown in Figure 4.6.

![Benchmark Rim-drive PM Motor FE Model](image)
### 4.2.6.2 Implementation of a 3-phase Concentrated Winding

The rim-drive PM motor had an operational speed of 288 rpm and was supplied from a converter with a full-load frequency of 158 Hz. A 33 pole-pair stator magnetic field was therefore required. A special 3-phase concentrated winding configuration was designed to achieve this requirement, since concentrated windings can be implemented to produce a high pole number. An additional benefit of concentrated windings is that the coils of the windings can be preformed, resulting in a high packing factor and therefore reducing the overall assembly costs of the motor. The 3-phase, 6-pole, concentrated winding layout per stator-slot is shown in Table 4.1.

#### Table 4.1: 3-phase Concentrated Winding Layout per Stator-slot for Benchmark Rim-drive PM Motor

<table>
<thead>
<tr>
<th>Slot number:</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>10</th>
<th>11</th>
<th>12</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
</tr>
<tr>
<td>Slot number:</td>
<td>13</td>
<td>14</td>
<td>15</td>
<td>16</td>
<td>17</td>
<td>18</td>
<td>19</td>
<td>20</td>
<td>21</td>
<td>22</td>
<td>23</td>
<td>24</td>
</tr>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
</tr>
<tr>
<td>Slot number:</td>
<td>25</td>
<td>26</td>
<td>27</td>
<td>28</td>
<td>29</td>
<td>30</td>
<td>31</td>
<td>32</td>
<td>33</td>
<td>34</td>
<td>35</td>
<td>36</td>
</tr>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
</tr>
<tr>
<td>Slot number:</td>
<td>37</td>
<td>38</td>
<td>39</td>
<td>40</td>
<td>41</td>
<td>42</td>
<td>43</td>
<td>44</td>
<td>45</td>
<td>46</td>
<td>47</td>
<td>48</td>
</tr>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
</tr>
<tr>
<td>Slot number:</td>
<td>49</td>
<td>50</td>
<td>51</td>
<td>52</td>
<td>53</td>
<td>54</td>
<td>55</td>
<td>56</td>
<td>57</td>
<td>58</td>
<td>59</td>
<td>60</td>
</tr>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
</tr>
<tr>
<td>Slot number:</td>
<td>61</td>
<td>62</td>
<td>63</td>
<td>64</td>
<td>65</td>
<td>66</td>
<td>67</td>
<td>68</td>
<td>69</td>
<td>70</td>
<td>71</td>
<td>72</td>
</tr>
<tr>
<td><strong>Winding orientation:</strong></td>
<td>S+</td>
<td>S-</td>
<td>S+</td>
<td>S-</td>
<td>T-</td>
<td>T+</td>
<td>T-</td>
<td>T+</td>
<td>R+</td>
<td>R-</td>
<td>R+</td>
<td>R-</td>
</tr>
</tbody>
</table>

Note: ‘+’ equates to positive orientation of current in the FE model, whereas ‘-’ equates to negative orientation of current

The implementation of the 3-phase concentrated winding configuration in Table 4.1 can be thought of as two separate conventional 3-phase windings, which have a spatial offset between them, as shown in Table 4.2 for only two of the magnetic poles.
Table 4.2: Layout of 3-phase Concentrated Winding Layout Separated into Two Equivalent 3-phase Windings Over Two Magnetic Poles for Benchmark Rim-drive PM Motor

<table>
<thead>
<tr>
<th>Slot number:</th>
<th>1</th>
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<th>11</th>
<th>12</th>
</tr>
</thead>
<tbody>
<tr>
<td>First equivalent winding:</td>
<td>S-</td>
<td>S-</td>
<td>T+</td>
<td>T+</td>
<td>R-</td>
<td>R-</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Second equivalent winding:</td>
<td>S+</td>
<td>S+</td>
<td>T-</td>
<td>T-</td>
<td>R+</td>
<td>R+</td>
<td></td>
<td></td>
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<td></td>
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</tbody>
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<table>
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<tr>
<th>Slot number:</th>
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<th>18</th>
<th>19</th>
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<th>21</th>
<th>22</th>
<th>23</th>
<th>24</th>
</tr>
</thead>
<tbody>
<tr>
<td>First equivalent winding:</td>
<td>S+</td>
<td>S+</td>
<td>T-</td>
<td>T-</td>
<td>R+</td>
<td>R+</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Second equivalent winding:</td>
<td>S-</td>
<td>S-</td>
<td>T+</td>
<td>T+</td>
<td>R-</td>
<td>R-</td>
<td></td>
<td></td>
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</tr>
</tbody>
</table>

The equivalent two 3-phase windings can be mathematically expressed in conventional Fourier harmonic form as:

\[
F_1 = \sum_{n=1}^{\infty} F_n \cos(\omega t - np \theta + \psi) \tag{4.1}
\]

\[
F_2 = \sum_{n=1}^{\infty} F_n \cos(\omega t - np(\theta - \gamma) + \psi) \tag{4.2}
\]

where:

- \( F_n = \text{Magnitude of the magnetomotive force (MMF) } n^{\text{th}} \text{ harmonic [A-t]} \)
- \( \omega = \text{Angular frequency of the electric system [radians.s}^{-1}] \)
- \( t = \text{Time [s]} \)
- \( n = \text{Harmonic number} \)
- \( p = \text{Pole-pairs} \)
- \( \theta = \text{Angle [radians]} \)
- \( \psi = \text{Angle [radians]} \)
- \( \gamma = \text{Winding offset angle [radians]} \)

The offset angle \( \gamma \) is calculated from:

\[
\gamma = \frac{\pi}{p} + \beta \tag{4.3}
\]
where $\beta$ is the slot pitch, which is calculated from:

$$
\beta = \frac{2\pi}{N_s}
$$

where:

$N_s =$ Number of stator-slots

The total MMF of the complete concentrated windings configuration, therefore, is simply the sum of the two components from Equations 4.1 and 4.2, which yields:

$$
F_T = F_1 + F_2 = \sum_{n=1}^{\infty} 2F_n\ K_\alpha \cos(\omega t - np(\theta - \frac{\gamma}{2}) + \psi)
$$

$K_\alpha^n$ represents a harmonic attenuation factor that can be determined from:

$$
K_\alpha^n = \cos(\frac{np\gamma}{2})
$$

Therefore, for the harmonics $n = 1$ and $n = 11$, the 3-phase concentrated winding configuration for the rim-drive PM motor produces a $K_\alpha^n$ of:

$$
K_\alpha^1 = 0.13
$$

$$
K_\alpha^{11} = 0.99
$$

This particular 3-phase concentrated winding configuration clearly attempts to cancel the fundamental MMF harmonic whilst reinforcing the 11th harmonic. The resulting dominant pole number is thus $11 \times 6 = 66$-poles. The fundamental and the 11th harmonic air-gap magnetic flux density waveforms for the first and second equivalent 3-phase windings are illustrated in Figures 4.7 and 4.8, respectively.
With the implementation of the 3-phase concentrated winding configuration in Table 4.1, therefore, the resultant air-gap magnetic field for the fundamental and 11\textsuperscript{th} harmonic are shown in Figure 4.9. A Fourier harmonic analysis of the air-gap magnetic
field produced by this 3-phase winding clearly illustrates the result of the implemented concentrated winding configuration, as shown in Figure 4.10.

![Figure 4.9](image1.png)

**Figure 4.9:** Resultant Air-gap Magnetic Flux Density Waveforms for the Addition of the First and the Second Equivalent 3-phase Winding Waveforms

![Figure 4.10](image2.png)

**Figure 4.10:** Fourier Harmonic Analysis from the Benchmark FE Model of the Air-gap Magnetic Field Produced by the 3-phase Concentrated Winding Configuration
Figure 4.9 shows that this particular 3-phase concentrated winding configuration reduces the fundamental harmonic by approximately 75% of its original magnitude, whereas the magnitude of the 11\textsuperscript{th} harmonic is effectively doubled. Furthermore, Figure 4.10 shows the result from the FE model of the 800 kW benchmark motor, highlighting that this 3-phase concentrated winding produces a dominant 33 pole-pair harmonic air-gap magnetic field, which arises from the 11\textsuperscript{th} harmonic of the 3-phase, 6-pole winding.

Once the FE model was completed, a transient analysis was conducted at synchronous speed to determine the maximum theoretical torque that the motor was capable of producing. The results of the simulation are illustrated in Figure 4.11.

![Figure 4.11: FEA Predicted Results for Rotor Torque produced by the Benchmark Rim-drive PM Motor](image)

The FE model of the rim-drive PM motor predicted that the motor was capable of producing an average torque of 25,346 Nm, with a ripple torque of ± 60 Nm, as shown in Figure 4.11. A prototype version of this PM motor had been developed by Rolls-Royce Marine, and therefore there was some limited experimental test data available from a sea trial conducted on the prototype PM motor. The experimental results determined that the prototype PM motor was capable of producing approximately 26,525 Nm of torque. The FEA predicted results, therefore, showed approximately 4.5% difference from this value.
A number of factors could account for the difference in torque between the FEA predicted results and the experimental test results from the prototype PM motor including the operating temperatures of the various components, the PM field orientation and grade of the material, manufacturing tolerances, accurate material data, i.e. B-H data for the electric steel used in the rotor- and stator-core and 3-D effects not accounted for in the 2-D FE model. The difference of 4.5% from the FEA predicted results was therefore deemed acceptable in view of these factors.

4.2.6.3 Canned LSPM Rim-drive Motor FE Model

The benchmark rim-drive PM FE model described in the previous section was shown to have an acceptable level of accuracy; therefore, the next stage in the development of the FE model was to introduce a conducting-can into the air-gap. The FE model was subsequently modified to create a LSPM synchronous motor. An extra layer was created in the air-gap of the FE model with an initial thickness of 2 mm, which was defined as copper. The material data for copper was obtained from the Flux2D materials library database and assigned to the newly defined region in the FE model.

4.2.7 Canned LSPM Rim-drive Motor FEA Predicted Performance

Initially the PMs were defined as air so that the operation of the motor (effectively an induction motor) could be examined. The frequency was reduced to the standard vessel supply frequency of 50 Hz, as no converter was required for the starting of this motor. A transient FE model was solved and the run-up response of the canned rim-drive induction motor is illustrated in Figure 4.12.
Figure 4.12: FEA Simulation Run-up Response of a Canned Rim-drive Induction Motor

Figure 4.12 shows that the FE model with the addition of the copper-can is able to operate, as expected, as an induction motor. This can be seen by the fact that the motor reaches a rotor speed slightly below the synchronous speed of 91 rpm. It should be noted, however, that the motor was modelled with zero load torque and a low value of inertia to reduce the computational time required to solve the FE model. The simulation was conducted simply to verify the correct operation of the FE model as an induction motor.

Once the correct operation of the FE model had been established, the PMs were reintroduced into the FE model by implementing the inset PM rotor configuration. A transient analysis was conducted on this new FE model, to observe the effects on the predicted performance of this design modification. Figures 4.13 and 4.14 show the predicted results obtained for rotor speed and torque, respectively.
Figure 4.13: FEA Predicted Results of Rotor Speed for the Canned LSPM Rim-drive Motor with the Inset PM Rotor Configuration

Figure 4.14: FEA Predicted Results of Rotor Torque for the Canned LSPM Rim-drive Motor with the Inset PM Rotor Configuration
Figure 4.13 clearly shows the effect of the pulsating torque on the rotational speed of the motor as it runs-up to synchronous speed. After approximately 0.2 s, the torque pulsations cause the rotor to ‘pull’ into synchronisation. Furthermore, after approximately 0.6 s, the motor synchronises and commences operation as a synchronous motor.

If the motor was to lose synchronous operation for any reason including a mechanical shock, temporary loss of the power supply or even a transitory blockage to the rotor/propeller, for example, the conducting-can would once again accelerate the motor close to synchronous speed and then hopefully the PMs would pull the motor back into synchronisation. If the motor did not resynchronise, however, it would run as a very poor induction motor and could even permanently damage the stator windings because of the large currents required to magnetise the air-gap.

Figure 4.14 shows that as the motor is started there are initially large torque pulsations, which reduce to a ripple torque once synchronisation has been successfully achieved. The large torque pulsations are the mechanism that helps the motor to reach a speed greater than synchronous speed and hence, cause the motor to synchronise. These initial results showed that a conducting conducting-can could potentially be implemented to line-start the motor.

**4.2.7.1 Effect of Buried vs. Surface-mount Permanent-magnets**

The FE model was modified by positioning the PMs on the surface of the rotor-core, creating the surface-mount PM rotor configuration, with the conducting-can mounted on top. This was implemented to determine the effect on the starting and synchronisation performance compared with those presented previously for the inset PM rotor configuration. Figures 4.15 and 4.16 show the predicted results obtained for rotor speed and torque, respectively.
Figure 4.15: FEA Predicted Results of Rotor Speed for the Canned LSPM Rim-drive Motor Comparing Buried vs. Surface-mount PM Rotor Configurations

Figure 4.16: FEA Predicted Results of Rotor Torque for the Canned LSPM Rim-drive Motor Comparing Buried vs. Surface-mount PM Rotor Configurations
Figure 4.15 shows that the surface-mount PM rotor configuration takes a longer time to run-up and the speed oscillates for a greater period of time compared to the buried PM rotor configuration, until it overshoots synchronous speed at approximately 0.4 s. It is not until around 1 s that the rotor speed oscillations decay to zero and the machine commences operation as a synchronous motor. This is about 66% slower than the inset PM rotor configuration.

Figure 4.16 also shows that the large torque pulsations continue for a longer period of time for the surface-mount PM rotor configuration compared to the inset PM rotor configuration. The main reason for the quicker synchronisation time of the latter is that the rotor-core is slightly salient due to the rotor-core material between the adjacent PMs, which creates some additional torque and therefore accelerates the motor quicker than the surface-mount configuration. These results show the effects the position of the PMs on the rotor has on the starting and synchronisation capabilities of the motor.

### 4.2.7.2 Effect of Conducting-can Thickness on Motor Performance

Various operating parameters of the motor can be altered such as the starting torque and current, and the speed at which peak torque is achieved, by modifying the resistance of the conducting-can. This may be altered in two obvious ways; firstly, by modifying the radial thickness of the conducting-can and secondly, by changing the material from which it is composed, i.e. from copper to aluminium. A number of simulations were therefore conducted to initially determine the effect of altering the thickness of the conducting-can. The thickness was increased from 1 mm to 6 mm in steps of 1 mm. The predicted results for torque as a function of slip are illustrated in Figure 4.17.
Figure 4.17: FEA Predicted Results of Torque vs. Slip for the Canned LSPM Rim-drive Motor with Several Conducting-can Thicknesses

Figure 4.17 shows that as the conducting-can thickness is increased, the available starting torque is reduced; however, the speed at which maximum torque is produced is closer to synchronous speed. Implementing a thin conducting-can, therefore, may improve the starting capability of the motor but the reduced torque close to synchronous speed may result in the motor failing to synchronise.

4.2.7.3 Effect of Conducting-can Material on Motor Performance

The second option that could be implemented to affect the operating performance of the motor is to change the conducting-can material. For example, aluminium or stainless steel could be used as an alternative to copper, since they have higher material resistivities. A detailed study of this has not been undertaken, however, because it is believed the effects will be similar to those shown in the previous section.

The conducting-can material choice and thickness, therefore, require careful consideration and subsequent detailed analysis to optimise a canned LSPM rim-drive motor design for its intended application.
4.2.8 Summary

The purpose of this concept study was to investigate the potential of creating a LSPM rim-drive motor that could start directly from the power supply on-board a marine vessel. The design was based on a motor previously developed by Rolls-Royce Marine, which was subsequently used as a benchmark. The benchmark motor was modified by introducing a conducting-can into the air-gap, which was used to provide line-start capabilities and hence, acceleration torque. The effect of the location of the PMs on the rotor was investigated and the conducting-can thickness was also altered to determine the effect it would have on the operating performance of the motor. The material composition of the rotor conducting-can was also considered, although no analysis was conducted due to commercial pressures.

The presented results show the potential for a LSPM rim-drive motor to be developed, although it must be noted that this was a preliminary investigation and due to commercial pressures, the design had not been optimised but was simply to highlight the potential and probable topology.

4.3 Seal-less Rim-drive Motor Pump

4.3.1 Introduction

The second potential application area for the rim-driven electric machine topology that was explored was as an in-line seal-less pump, since pumps are usually driven by motors. The rim-drive concept could combine these two functions together resulting in a single machine. Rolls-Royce identified an application area for the rim-drive motor topology as a main coolant pump in a nuclear power plant. Coolant pumps are located in the primary circuit of the nuclear reactor and are used to continuously re-circulate coolant (pressurised demineralised water) between the reactor pressure vessel and the steam turbine. Coolant pumps are a safety critical component in the system and hence, reliability is important. A concept study was undertaken, therefore, to determine the applicability of the rim-drive motor topology for this type of pumping application. The innovative feature was that the rim-drive motor pump would be required to be
hermetically sealed due to the critical operating environment and could therefore potentially take advantage of the canned configuration.

### 4.3.2 Application

The nuclear industry presently provides about 14% of the world’s electricity, with approximately 440 commercial nuclear reactors operating in over 30 countries [131]. These numbers are set to increase in the future due to the increasing demand for power, with an additional 10 countries planning to develop nuclear power generating facilities [131, 132]. The demand from existing reactors, along with these new nuclear power facilities is set to increase the growth of the coolant pump market. For example, the demand for coolant pumps was approximately 48 in 2009 but is estimated to increase to 108 per year by 2015; an annual average growth rate of 13.51% [132]. There is a strong demand, therefore, for reliable coolant pumps into the foreseeable future.

### 4.3.3 Seal-less Rim-drive Motor Pump Design

A rim-drive motor pump integrates the propeller with the electric machine that is driving it by mounting the motor directly around the outside of the propeller. The concept of driving the propeller tips, rather than a central propeller shaft, was suggested in 1957 using a mechanical design [26]. Electrical designs have also been proposed more recently [27, 28]. A cross-sectional illustration of the proposed rim-drive motor topology for this application is presented in Figure 4.18.

![Figure 4.18: Cross-sectional View of the Proposed Topology for the Rim-drive Motor Pump (no mechanical support structure is drawn)](image)

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From the illustration in Figure 4.18, it can be seen that the complete rotor assembly will be encapsulated by the primary coolant pipe and submersed permanently in the coolant. The stator, however, will be wrapped around the outside of the primary coolant pipe and insulated from the ambient heat of the coolant by a ceramic layer.

The purpose of this concept study was to develop a paper design of a 200 kW rim-drive motor pump. Sheffield University were also commissioned to design an equivalent hydraulic pump. Rolls-Royce could subsequently compare the advantages and disadvantages of both designs to each other, and also to an original main coolant pump design.

The motor specifications were: 200 kW, 440 V, 3-phase supply, 60 Hz, and a rotational speed of 1,800 rpm. The operating temperature of the de-mineralised, de-oxidised coolant was approximately 250 °C. The final constraint was a minimum rotor inside diameter (RID) of 245 mm, which was the diameter of the primary coolant pipe and also the proposed propeller. The radial thickness and weight of the rim-drive motor were to be minimised as much as practically possible, to allow the rotor assembly to be mounted permanently within the primary coolant pipe.

The initial design investigated was a LSPM rim-drive motor topology but after taking into consideration the ambient temperature of the coolant and hence, the temperature the rotor was required to operate at, it was decided that PM materials were not suitable for this particular application. A line-start rim-drive induction motor topology was chosen, therefore, to be assessed as an alternative.

The first stage of the concept study was to select a conventional 200 kW industrial motor, which was a standard squirrel-cage induction motor. This motor could be modelled using Flux2D and later used as a benchmark. The predicted performance from the benchmark FE model could subsequently be compared to the test data for the 200 kW industrial motor to initially validate the FE model. Figure 4.19 shows a cross-sectional view of the 2-D FE model of the benchmark motor.
Once the FE model of the benchmark motor was completed, a number of per-unit load-point simulations were conducted. The operating performance predicted by the FEA is shown compared to the test data from the industrial motor in Table 4.3.

Table 4.3:  Per-unit Load-point FEA Predicted Results for the 200 kW Benchmark FE Model Compared to the Test Data from the Industrial Motor

<table>
<thead>
<tr>
<th>Load (per-unit)</th>
<th>Torque (% difference)</th>
<th>Speed (% difference)</th>
<th>Phase Current (% difference)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>0.68</td>
<td>-0.07</td>
<td>18.88</td>
</tr>
<tr>
<td>0.75</td>
<td>-0.01</td>
<td>-2.79</td>
<td>9.85</td>
</tr>
<tr>
<td>1</td>
<td>0.97</td>
<td>-1.57</td>
<td>6.81</td>
</tr>
<tr>
<td>1.25</td>
<td>0.93</td>
<td>-2.76</td>
<td>4.74</td>
</tr>
</tbody>
</table>

Table 4.3 shows that the predicted torque and speed at full-load from the FEA conducted on the benchmark FE model differ by less than 2% when compared to the test data from the industrial motor. The full-load phase current has a 6.81% deviation, which increases as the load is reduced. This result suggests that the magnetising current in the FE model is incorrect, which would imply that the calculated values for the inductance of the end-windings and end-rings in the linked external circuit of the FE model are inaccurate. These values are notoriously difficult to determine; therefore, the full-load results were considered acceptable.
The second stage of the concept study was to modify the benchmark FE model to meet the rim-drive motor pump specifications. Firstly, the supply voltage and frequency were altered from 400 V, 50 Hz to 440 V, 60 Hz. Secondly, the RID was increased from 125 mm to the diameter of the primary coolant pipe; a measurement of 245 mm. This resulted in an increase in the phase resistance, the phase end-winding inductance and the end-ring resistance. The operating temperature of the rotor materials were also increased to an initial value of 300 °C, as the coolant was circulating at an ambient temperature of 250 °C.

One of the key differences between the industrial motor and the rim-drive motor pump design is the air-gap. The philosophy in a conventional induction motor design is to minimise the air-gap, subject to mechanical and thermal constraints, to keep the magnetising reactance as low as practically achievable. The industrial motor had an air-gap of 1.5 mm, which was increased to 4.75 mm in the rim-drive motor pump design. This was implemented to allow a series of cans to be introduced into the air-gap (filled by coolant). The rim-drive motor pump design is novel because it uses a number of air-gap cans to provide additional torque, environmental protection and thermal shielding.

The first of the cans introduced into the air-gap was a copper-can mounted on the surface of the rotor-core. The copper-can had a radial thickness of 0.5 mm and produced torque in the same way as a squirrel-cage. This could potentially be used to allow the rotor-bars to be reduced to a simpler shape, minimising the rotor core-back depth and hence, reducing the weight of the motor.

The second can was also mounted onto the rotor but over the copper-can. This can was made of Inconel due to its suitability for use in harsh environments. The Inconel-can was also 0.5 mm thick and was added to provide environmental containment (a hermetic seal) of the rotor, to protect it from the coolant in the primary circuit of the nuclear reactor.

Following the insertion of the Inconel-can, the air-gap thickness was left at the same value of 1.25 mm as the industrial motor. The air-gap was followed by the primary coolant pipe wall, which was 2 mm thick and also made of Inconel. The pipe wall provides environmental containment protection for the stator, which is wrapped around the outside of the pipe.
Finally, between the outer surface of the pipe wall and the inner surface of the stator-core, a can/layer of ceramic material was inserted. This was included to act as a thermal barrier to minimise the heat transfer from the coolant circulating at 250 °C to the stator. A cross-sectional illustration of the air-gap topology for the rim-drive motor pump design is presented in Figure 4.20.

![Cross-sectional View of the Air-gap Configuration for the Rim-drive Motor Pump Design](image)

**Figure 4.20:** Cross-sectional View of the Air-gap Configuration for the Rim-drive Motor Pump Design

### 4.3.4 Seal-less Rim-drive Motor Pump FEA Predicted Performance

Following the modifications to the benchmark FE model described in the previous section to develop the rim-drive motor pump FE model, Flux2D was used to conduct a number of simulations to predict the torque and phase current of the rim-drive motor pump design at various speeds, which were compared to the benchmark FE model. This initial design resulted in a reduction in the torque across the torque-speed curve by approximately 33%. The general reduction in torque was mainly a result of the increased effective air-gap length, which reduced the magnetising reactance and hence, the magnitude of the air-gap magnetic flux density. The magnetising current, however, had increased by almost 94% compared to the benchmark FE model. To illustrate the effect of the increased magnetising current, a harmonic analysis of the air-gap magnetic field produced by the benchmark FE model compared to the rim-drive motor pump FE model is shown in Figure 4.21.
Figure 4.21: Fourier Harmonic Analysis of the Air-gap Magnetic Field Produced by the Benchmark Motor and the Rim-drive Motor Pump Design

Figure 4.21 shows that the magnitude of the air-gap magnetic flux density fundamental harmonic for the rim-drive motor pump design has been reduced by approximately 35% compared to the benchmark FE model.

One of the key design requirements for the rim-drive motor pump was to minimise the active radial thickness. The next stage in the design, therefore, was to modify the rotor-bar shape to a simpler design. The rotor-bars of the industrial motor were designed to take advantage of ‘skin effect’. Skin effect is a frequency-dependant phenomenon that alters the current distribution in the bars as a function of the frequency and hence, rotor speed. As the motor starts, the frequency of the bar currents are at supply frequency, which pushes the currents to the top of the bars near the air-gap and hence, increases the effective rotor resistance. As the motor accelerates, however, the frequency in the bars reduces and the currents spread back down the bars, reducing the effective rotor resistance. The larger effective rotor resistance at starting increases the starting torque and reduces the starting current, seen in Figures 4.23 and 4.24. A large starting torque, however, was not a requirement of the rim-drive motor pump design; therefore, the shape of the rotor-bars could be simplified. The rotor-bars were simplified to a basic ‘tear-drop’ shape, as illustrated in Figure 4.22.
To further reduce the required cross-sectional area of the rotor-bars, their material was changed from aluminium, used in the industrial motor, to copper. This resulted in a reduction of the rotor-core radial thickness by 36 mm, compared to the industrial motor.

Since the RID had been increased to the required value of 245 mm to accommodate the propeller, the rotor and stator teeth widths had also increased. This resulted in reduced magnetic flux density levels in the teeth and hence, sub-optimal use of the electric steel. The stator-slots and rotor-bars were therefore adjusted by reducing their radial depths, whilst increasing their widths to produce the same magnetic flux density levels in the teeth as in the industrial motor. As a result, the rotor-bars were reduced by 6.5 mm and the stator-slots by 2 mm, respectively. Although relatively small modifications, these would have the additional benefit of reducing the leakage inductance of the rotor-bars and stator-slots.

As the supply conditions were different for the rim-drive motor pump design compared to the industrial motor, the core-back magnetic flux density levels were reduced. The final modification in this initial design phase, therefore, was to reduce the radial depth of the rotor and stator core-backs until a safe magnetic flux density level was achieved under normal operating conditions. This resulted in the core-backs being reduced by approximately 10 mm each. As a result of all the modifications discussed being implemented, the overall active radial thickness of the rim-drive motor pump design
was reduced by a total of 64 mm, compared to the industrial motor. A number of FEA simulations were conducted to predict the operating performance of this final initial design stage. The predicted performance results for torque and phase current, compared to the benchmark FE model, are shown in Figures 4.23 and 4.24, respectively.

**Figure 4.23:** FEA Predicted Results of Torque vs. Slip for the Rim-drive Motor Pump (with and without the copper-can) Compared to the Benchmark Motor

**Figure 4.24:** FEA Predicted Results of Phase Current vs. Slip for the Rim-drive Motor Pump (with and without the copper-can) Compared to the Benchmark Motor
Figure 4.23 shows that the starting torque of the rim-drive motor pump design with the copper-can decreased by approximately 42% compared to the benchmark FE model, which was mainly a result of the simplification of the rotor-bar shape and reduced air-gap magnetic flux density. The peak torque, however, increased by approximately 32% mainly because of the increased mean air-gap diameter. The removal of the copper-can reduced the torque by an average of 2.5% across the torque-slip curve, with the starting torque decreasing the most by approximately 4%.

Figure 4.24 shows that the starting phase current of the rim-drive motor pump design with the copper-can increased by approximately 12% compared to the benchmark FE model, which is mostly a result of the increased rotor resistance. The magnetising current also increased by almost 150% mainly because of the increased effective air-gap length. The removal of the copper-can resulted in an average reduction across the phase current-slip curve of approximately 3%, with the starting and magnetising currents being reduced by 4% and 6%, respectively.

An image of the FE models for the benchmark motor and the final rim-drive motor pump design are presented in Figure 4.25, which illustrate the implemented design changes and reduction in the radial active thickness of the rim-drive motor pump design.

Figure 4.25: Graphical Illustration of the Benchmark Motor Design vs. the Rim-Drive Motor Pump Design
To evaluate the power factor and efficiency of the rim-drive motor pump design compared to the industrial motor, a number of simulations were conducted to determine several per-unit load-points, the results of which are presented in Table 4.4.

**Table 4.4:** Comparison of Power Factor and Efficiency for the Rim-drive Motor Pump Compared to the Industrial Motor at Various Per-unit Load-points

<table>
<thead>
<tr>
<th>Load (per-unit)</th>
<th>Power Factor</th>
<th>Efficiency (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Industrial</td>
<td>Rim-drive</td>
</tr>
<tr>
<td></td>
<td>Motor</td>
<td>Motor</td>
</tr>
<tr>
<td>0.5</td>
<td>0.820</td>
<td>0.68</td>
</tr>
<tr>
<td>0.75</td>
<td>0.884</td>
<td>0.73</td>
</tr>
<tr>
<td>1</td>
<td>0.906</td>
<td>0.77</td>
</tr>
<tr>
<td>1.25</td>
<td>0.915</td>
<td>0.80</td>
</tr>
</tbody>
</table>

Table 4.4 shows that the power factor for the rim-drive motor pump design is lower compared to the industrial motor, which is due to the increased magnetising current. The efficiency is also lower by a considerable amount. This is mainly due to the eddy current loss generated in the primary coolant pipe wall, which reacts to the full stator magnetic field.

The pipe wall is an issue that would therefore have to be considered carefully if this design was implemented, i.e. possibly use a non-conducting material in a section of the pipe where the rim-drive motor pump is situated. For illustrative purposes, however, the FE model was modified by defining the pipe wall as air and the FEA simulations were conducted again. The predicted performance results for torque and phase current, compared to the benchmark FE model, are shown in Figures 4.26 and 4.27, respectively.
Figure 4.26: FEA Predicted Results of Torque vs. Slip for the Rim-drive Motor Pump (with the primary coolant pipe wall defined as air) Compared to the Benchmark Motor

Figure 4.27: FEA Predicted Results of Phase Current vs. Slip for the Rim-drive Motor Pump (with the primary coolant pipe wall defined as air) Compared to the Benchmark Motor
Figures 4.26 and 4.27 show that there is generally little difference in the torque and phase current of the rim-drive motor pump FE model (with copper-can) with and without the pipe wall in the air-gap. The peak torque has increased by almost 5% but the starting torque has remained almost the same. The magnetising current has reduced by approximately 8% but the starting current was again practically the same value.

The power factor and efficiency for the rim-drive motor pump design compared to the industrial motor, which are presented in Table 4.5, however, show a marked difference.

Table 4.5: Comparison of Power Factor and Efficiency for the Rim-drive Motor Pump Compared to the Industrial Motor at Various Per-unit Load-points with the Primary Coolant Pipe Wall Defined as Air

<table>
<thead>
<tr>
<th>Load (per-unit)</th>
<th>Power Factor</th>
<th>Efficiency (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Industrial Motor</td>
<td>Rim-drive Motor</td>
</tr>
<tr>
<td>0.5</td>
<td>0.820</td>
<td>0.38</td>
</tr>
<tr>
<td>0.75</td>
<td>0.884</td>
<td>0.52</td>
</tr>
<tr>
<td>1</td>
<td>0.906</td>
<td>0.62</td>
</tr>
<tr>
<td>1.25</td>
<td>0.915</td>
<td>0.69</td>
</tr>
</tbody>
</table>

Table 4.5 shows that the power factor for the rim-drive motor pump design has decreased further compared to the industrial motor as expected, as the air-gap has effectively increased by an additional 2 mm. The efficiency, however, has increased and is more in line with that of the industrial motor.

The full-load results and losses for the industrial motor compared to the rim-drive motor pump design (with the pipe wall defined as air) are presented in Tables 4.6 and 4.7, respectively.

Table 4.6: Full-load Results for the Rim-drive Motor Pump Compared to the Industrial Motor

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Industrial Motor</th>
<th>Rim-drive Motor</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Speed</td>
<td>1,492</td>
<td>1,782</td>
<td>rpm</td>
</tr>
<tr>
<td>Torque</td>
<td>1,268</td>
<td>1,071</td>
<td>Nm</td>
</tr>
<tr>
<td>Current</td>
<td>189</td>
<td>253</td>
<td>A</td>
</tr>
<tr>
<td>Slip</td>
<td>0.0056</td>
<td>0.0098</td>
<td>-</td>
</tr>
<tr>
<td>Input power</td>
<td>205</td>
<td>208</td>
<td>kW</td>
</tr>
<tr>
<td>Output power</td>
<td>198</td>
<td>200</td>
<td>kW</td>
</tr>
<tr>
<td>Specific electric loading</td>
<td>28,155</td>
<td>35,538</td>
<td>A-t.m⁻¹</td>
</tr>
<tr>
<td>Specific magnetic loading</td>
<td>0.51</td>
<td>0.34</td>
<td>T</td>
</tr>
</tbody>
</table>
Table 4.7: Breakdown of the Losses for the Rim-drive Motor Pump Compared to the Industrial Motor

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Industrial Motor</th>
<th>Rim-drive Motor</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator copper</td>
<td>2,211 W</td>
<td>3,744 W</td>
<td>W</td>
</tr>
<tr>
<td>Rotor copper</td>
<td>1,296 W</td>
<td>1,972 W</td>
<td>W</td>
</tr>
<tr>
<td>Stator iron</td>
<td>1,825 W</td>
<td>1,429 W</td>
<td>W</td>
</tr>
<tr>
<td>Rotor iron</td>
<td>751 W</td>
<td>748 W</td>
<td>W</td>
</tr>
<tr>
<td>Rotor copper-can</td>
<td>-</td>
<td>114 W</td>
<td>W</td>
</tr>
<tr>
<td>Rotor Inconel-can</td>
<td>-</td>
<td>3.17 W</td>
<td>W</td>
</tr>
</tbody>
</table>

Table 4.6 shows that for the full-load operating point, the rim-drive motor pump design produces about 18% less torque and the phase current is about 34% higher compared to the industrial motor. The electric loading has also increased but this is calculated from the phase current which includes the magnetising current. The magnetising current, however, has increased because of the larger effective air-gap used to accommodate the additional cans.

Table 4.7 shows that the rim-drive motor pump design has a 70% larger stator copper loss component compared to the industrial motor, which is due to the increased phase current. The rotor copper loss has also increased by approximately 52%, which is mainly due to the increased operating temperature. A detailed specification of the rim-drive motor pump design is given in Appendix B.

4.3.5 Summary

The purpose of this concept study was to develop an induction rim-drive motor pump for use as a main coolant pump in nuclear reactors. Although the mean air-gap diameter of the rim-drive motor pump design was increased from 355 mm to 377 mm, the radial thickness of the active material was reduced by 64 mm, which resulted in an overall weight saving of approximately 62 kg compared to the industrial benchmark motor.

The predicted results showed that at full-load the rim-drive motor pump design had a 34% increase in the supply current and the power factor was reduced from 0.906 to 0.770 compared to the industrial benchmark motor. These are the direct result of the large magnetising current required because of the larger effective air-gap.
The efficiency was reduced substantially due to the presence of the coolant pipe wall in the air-gap but it was shown that the efficiency could be increased at full-load from approximately 58% to 96% if the pipe wall was made of non-conducting material, although this would have the effect of reducing the power factor further.

The pipe wall material would have to be considered carefully, therefore, in a future design if the topology was to become a commercially viable product. The data was encouraging as the weight and size savings were seen to be beneficial by Rolls-Royce for this particular application.

### 4.4 DDAT Rim-drive Propulsion Motor

#### 4.4.1 Introduction

The third potential application area investigated for the rim-driven electric machine topology was as a vertically retractable azimuth thruster, better known as a drop-down azimuth thruster (DDAT). DDATs are commonly used for the low-speed manoeuvring of large vessels or as an emergency back-up propulsion system on vessels such as submarines in the event of a complete failure of the primary propulsion system.

#### 4.4.2 Application

The propeller of a DDAT is traditionally powered externally via a shaft and gearbox coupled to an induction or PM drive motor. The thrust developed is normally adjusted in one of two ways: a CPP and a fixed-speed motor or a FPP and a variable-speed motor controlled via a converter. DDATs enable the propeller to effectively rotate 360° around a vertical axis so that a DDAT can perform both propulsion and steering functions. Figure 4.28 illustrates a conventional DDAT produced by Rolls-Royce.
The conventional DDAT shown in Figure 4.28 can be produced electrically using the rim-driven topology and still retain its functionality. One of the advantages of a direct-drive rim-driven electric thruster solution compared to a conventional DDAT is the removal of the complicated gearboxes and drive shafts arrangement – the propeller drive motor is normally inside the hull and connected to the propeller via an ‘Z drive’ or ‘L drive’ shaft arrangement, as illustrated in Figure 4.28. The removal of the gearbox pod and shaft housing, from the flow of water, increases the hydrodynamic efficiency of the DDAT. Furthermore, since the shaft passing through the hull of the vessel can be removed, reduced vibration levels can be achieved, increasing passenger comfort, for example. Moreover, removing the drive shafts, gears and hence, bearings, reduces the maintenance requirements of a DDAT.

For most marine propulsion systems, embedding the propeller drive motor directly into the DDAT usually means operating in a sea-water environment. This can be used, however, to improve cooling and increase motor torque densities from those normally achieved with natural or forced-air cooled machines. Environmental containment of the rotor (and stator) is also a key requirement to protect them from sea-water
contamination. The containment system is commonly in the form of an epoxy resin layer or sleeve/can fitted around the outside of the rotor-core and to the inside of the stator-core, for example.

### 4.4.3 DDAT Rim-drive Propulsion Motor Design

The main purpose of this concept study was to develop a paper design of an electric machine integrated directly into the structure of the thruster, specifically the housing around the propeller itself utilising the rim-driven topology. The specific technology being investigated was a DOL canned rim-drive induction motor, which offered the major advantage of removing the need for a power electronic converter to control the motor in FPP thruster applications. Removing the requirement for a converter reduces the on-board footprint within the vessel. It also reduces the system cost of a rim-drive solution in applications where conventional FFP operation is preferred.

This volume saving is especially important on vessels such as submarines. For example, if an emergency back-up propulsion system on-board a submarine is considered, it may remain inactive for long periods of time. Removing the requirements for a converter whilst still retaining the functionality of the emergency propulsion system saves valuable space and hence, weight. For example, it has been estimated that a one ton weight saving in the stern of a submarine is equivalent to a five ton weight saving in the bow. Furthermore, for every ton of weight saved, it is estimated that one million pounds of cost savings can be achieved [133]. It is clearly valuable to develop an emergency propulsion system that can achieve these benefits. The weight of the motor, therefore, was to be minimised as much as practically possible without compromising its electric and mechanical properties.

The first stage of the design study was to select an industrial induction motor that could be used as a benchmark for the DDAT rim-drive propulsion motor design. This would be used to validate the FE modelling as discussed previously. This time the industrial motor specification was chosen to have approximately the same torque rating at full-load as the required DDAT rim-drive propulsion motor. This was based on the reasonable assumption that the torque largely dictates the volume and hence, weight of a motor.
The selected industrial motor was modelled using Flux2D and a number of load-point simulations were carried out, as before, to determine the theoretical running performance of the benchmark FE model. These results, which are presented in Table 4.8, compare the per-unit performance of the benchmark FE model with the experimental test data from the industrial motor.

<table>
<thead>
<tr>
<th>Load (per-unit)</th>
<th>Torque (% difference)</th>
<th>Speed (% difference)</th>
<th>Phase Current (% difference)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>0.36</td>
<td>-0.10</td>
<td>6.85</td>
</tr>
<tr>
<td>0.75</td>
<td>0.36</td>
<td>-0.16</td>
<td>3.88</td>
</tr>
<tr>
<td>1</td>
<td>0.23</td>
<td>-0.23</td>
<td>2.46</td>
</tr>
<tr>
<td>1.25</td>
<td>0.07</td>
<td>-0.29</td>
<td>1.58</td>
</tr>
</tbody>
</table>

Table 4.8: Per-unit Load-point FEA Predicted Results for the Benchmark FE Model Compared to the Test Data from the Industrial Motor

Table 4.8 shows that the predicted full-load torque and speed are within 0.25% of the experimental test data, and the full-load phase current is within 2.5%. The current deviation increases as the load decreases, which again suggests that the magnetising current is incorrect and hence, the linked circuit inductances in the FE model are inaccurate. These full-load results, however, were deemed acceptable to validate the FE model.

The specifications for the DDAT rim-drive propulsion motor were: 440 V, 3-phase supply, 60 Hz, a rotational speed of approximately 575 rpm and a torque requirement of 3,000 Nm. The diameter of the specified propeller was 0.8 m. Furthermore, the propeller had a 20 mm thick nickel-aluminium-bronze (NAB) ring (the same material from which a propeller is usually manufactured) that would be attached to the propeller blade tips for structural support. The DDAT rim-drive propulsion motor would therefore be located on the outer surface of the NAB ring, corresponding to a RID of 0.84 m.

The benchmark FE model was subsequently modified by increasing the RID to the DDAT rim-drive propulsion motor specification of 0.84 m. In the industrial motor design, the rotor had 58 bars and a tooth width of approximately 12.5 mm; the stator had 72 slots and a tooth width of approximately 9.5 mm. After the diameter of the FE model was increased, however, the rotor and stator teeth widths had increased to approximately 52 mm and 39 mm, respectively. Effective use of the electric steel meant
that the teeth would need to be reduced in width. Due to the lower operational speed requirement of the DDAT rim-drive propulsion motor compared to the industrial motor, however, the number of poles of the motor also needed to be increased, which could potentially increase the required number of stator-slots. The first modification, therefore, was to determine the number of pole-pairs required for the specified operating speed. For a supply frequency of 60 Hz and a required operational speed of approximately 575 rpm, 6.26 pole-pairs would be required. Six pole-pairs were chosen, therefore, resulting in a synchronous speed of 600 rpm. With loaded operation, the DDAT rim-drive propulsion motor would run closer to the required operational speed.

A conventional 3-phase double-layer winding was implemented to reduce the length of the end-windings due to the large diameter of the stator. A 3-phase single-layer winding, for example, would have required an end-windings diameter span between stator-slots of approximately 260 mm. By using double-layer windings, however, this was reduced to 217 mm. The implementation of double-layer windings would also have the beneficial effects of reducing the leakage inductance of the end-windings, the phase resistance, the weight of the motor and also the material costs. The 5th and 7th MMF harmonics could also be reduced by short pitching the windings.

The air-gap was increased from 1.5 mm to 5.5 mm to again allow for a series of cans to be introduced into the air-gap. The first can added to the rotor-core surface was made of copper. This can had a radial thickness of 1 mm. The second can on the rotor, mounted on top of the copper-can, was an environmental containment-can of Inconel, which was 0.5 mm thick. The physical air-gap was increased to 3.5 mm, which was the minimum distance specified for mechanical clearance. Finally, an environmental containment-can was also added to the inner stator-core surface, which was 0.5 mm thick and made of Inconel.

Since the mean air-gap diameter of the DDAT rim-drive propulsion motor design had been approximately doubled, the torque had subsequently increased by a factor of approximately four compared to the industrial motor. The axial length of the FE model, therefore, was reduced by the same factor. In conventional induction motor design, the air-gap electromotive force (EMF) induced per phase in the stator windings is assumed to be approximately equal to the terminal phase voltage, which is then used to set the required number of turns of the phase winding for a desired air-gap magnetic flux.
density level. The large magnetising current required for the DDAT rim-drive propulsion motor design due to the increased air-gap, however, resulted in a lower specific magnetic loading using this approach, as was shown in Section 4.3.4. The required number of turns per phase, therefore, was increased to bring the specific magnetic loading back up to the desired level of approximately 0.5 T, corresponding to a peak air-gap magnetic flux density of approximately 0.8 T.

Since the DDAT rim-drive propulsion motor was to be operated in a sea-water environment, the motor would be submersed during operation and, as a result, sea-water would be present in the air-gap, in direct contact with the environmental cans on the stator and rotor surfaces. Furthermore, because of the increased diameter of the motor, the surface areas of the rotor and stator had increased, which meant that substantially improved cooling would be available when compared to a conventional air-cooled motor.

The electric loading of the DDAT rim-drive propulsion motor design was therefore increased to minimise the required cross-sectional area per turn of the stator windings, to keep the required stator-slot area as small as possible, since the required number of turns per phase had increased and improved cooling was available. The stator conductors were designed, therefore, to operate at up to 8 A/mm². This was almost three times more than the full-load current density of 2.89 A/mm² in the industrial motor. The rotor-bar load current is related directly to the stator estimated load current. The rotor-bars, however, are normally worked at a higher current density than the stator conductors because they have no insulation reducing their thermal conductivity. The rotor-bars were designed, therefore, to operate at approximately 16 A/mm².

A number of design iterations were conducted to determine the appropriate axial length, number of turns per phase, number of stator-slots and resultant slot area for the DDAT rim-drive propulsion motor design. The same procedures used in the previous design studies were carried out to reduce the core-back depths, simplify the rotor-bars and optimise the teeth widths on the rotor and stator. The implementation of these design modifications resulted in the DDAT rim-drive propulsion motor design FE model, which is compared to the benchmark FE model in Figure 4.29.
4.4.4 DDAT Rim-drive Propulsion Motor FEA Predicted Performance

The predicted torque and phase current results as a function of slip for the DDAT rim-drive propulsion motor design (with and without the copper-can), compared to the benchmark FE model, are shown in Figures 4.30 and 4.31, respectively.
Figure 4.30 shows that the copper-can increases the torque by approximately 16% across the torque-slip curve, with the peak torque increasing by almost 10% compared to the FE model without the copper-can. Furthermore, the starting torque is increased by approximately 31%. This clearly shows the additional torque that the 1 mm copper-can supplies throughout the torque-slip curve in this design, which is mainly due to the increased resistivity of the copper-can because of the short axial length. The phase current is also affected by the addition of the copper-can, with an increase across the phase current-slip curve of approximately 7%, as can be seen in Figure 4.31.

Compared to the benchmark FE model, however, there are some considerable changes. Firstly, the starting torque of the DDAT rim-drive propulsion motor design with the copper-can compared to the benchmark FE model has decreased by approximately 60%, which is again caused by the simplified rotor-bar shape. Secondly, the peak torque level was approximately 27% lower. Furthermore, there is also a reduction in the overall torque, which is caused by the change in the aspect ratio of the motor, i.e. reduced air-gap volume and also the reduced synchronous speed.
The DDAT rim-drive propulsion motor design, however, has sufficient overload capability (typically a peak torque value approximately double the full-load torque) and therefore the design was considered sufficient for the requirements of the intended application. Furthermore, Figure 4.31 shows that the starting phase current of the DDAT rim-drive propulsion motor design has reduced by approximately 65%, which is also a result of the increased rotor resistance. The magnetising current, however, has increased by approximately 94%, which is again a result of the larger air-gap.

To evaluate the power factor and efficiency of the DDAT rim-drive propulsion motor design compared to the industrial motor, a number of FEA simulations were conducted to determine several per-unit load-points. The results of these simulations are presented in Table 4.9.

Table 4.9: Comparison of Power Factor and Efficiency for the DDAT Rim-drive Propulsion Motor Compared to the Industrial Motor at Various Per-unit Load-points

<table>
<thead>
<tr>
<th>Load (per-unit)</th>
<th>Power Factor</th>
<th>Efficiency (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Industrial Motor</td>
<td>Rim-drive Motor</td>
</tr>
<tr>
<td>0.5</td>
<td>0.788</td>
<td>0.55</td>
</tr>
<tr>
<td>0.75</td>
<td>0.85</td>
<td>0.66</td>
</tr>
<tr>
<td>1</td>
<td>0.87</td>
<td>0.7</td>
</tr>
<tr>
<td>1.25</td>
<td>0.87</td>
<td>n/a</td>
</tr>
</tbody>
</table>

Table 4.9 shows that the power factor for the DDAT rim-drive propulsion motor design is lower compared to the industrial motor, which is again due to the larger magnetising current caused by the increased air-gap. The efficiency is also lower, which is mainly due to the high electric loading the motor was designed to operate at, causing increased rotor and stator joule losses. It is to be noted that the DDAT rim-drive propulsion motor design did not achieve the 1.25 per-unit load level. This was because the DDAT rim-drive propulsion motor was designed to produce a full-load torque of approximately 3,000 Nm (the same as the industrial motor), with an over-load capability of 2:1 peak-torque to full-load torque.

The full-load results and losses for the DDAT rim-drive propulsion motor design, therefore, compared to the industrial motor at the same torque output level are presented in Tables 4.10 and 4.11, respectively.
Table 4.10: Full-load Results for the DDAT Rim-drive Propulsion Motor Compared to the Industrial Motor

<table>
<thead>
<tr>
<th>Quantity</th>
<th>Industrial Motor</th>
<th>Rim-drive Motor</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Speed</td>
<td>988</td>
<td>573</td>
<td>rpm</td>
</tr>
<tr>
<td>Torque</td>
<td>3,042</td>
<td>3,042</td>
<td>Nm</td>
</tr>
<tr>
<td>Current</td>
<td>312</td>
<td>282</td>
<td>A</td>
</tr>
<tr>
<td>Slip</td>
<td>0.0121</td>
<td>0.0445</td>
<td>-</td>
</tr>
<tr>
<td>Input power</td>
<td>326</td>
<td>212</td>
<td>kW</td>
</tr>
<tr>
<td>Output power</td>
<td>315</td>
<td>183</td>
<td>kW</td>
</tr>
<tr>
<td>Specific electric loading</td>
<td>36,469</td>
<td>52,066</td>
<td>A-t.m⁻¹</td>
</tr>
<tr>
<td>Specific magnetic loading</td>
<td>0.46</td>
<td>0.5</td>
<td>T</td>
</tr>
</tbody>
</table>

Table 4.10 shows that the input and output power of the DDAT rim-drive propulsion motor design have been reduced by 34% and 42%, respectively, compared to the industrial motor although the full-load torque value is the same. Also, the phase current of the DDAT rim-drive propulsion motor design is approximately 10% lower compared to the industrial motor, although the electric loading has increased by approximately 43%.

It is clear from Table 4.11 that the DDAT rim-drive propulsion motor design has a substantial stator copper loss component, with an increase of 252% compared to the industrial motor. This loss, along with the stator containment-can loss, accounts for the main reduction in efficiency. Efficiency, however, is less critical for the intended application of this motor due to the low duty cycle of operation and, therefore, was not considered a key factor in the design process. Far more important was the line-start operation, facilitating the removal of the power electronic converter and hence, the space and weight savings.
Although the DDAT rim-drive propulsion motor was designed to operate at up to 8 A/mm² in the stator conductors and 16 A/mm² in the rotor-bars, at full-load a current density of approximately 5 A/mm² and 9 A/mm², respectively, was predicted from the FEA simulations. The lower current densities were due to mechanical constraints on the leading dimensions of the design. The diameter of the DDAT rim-drive propulsion motor was fixed by the size of the propeller and therefore, the required torque was achieved by shortening the axial length of the motor. Structural rigidity of the motor, however, set a minimum axial length which in turn resulted in a lower electric loading for the rated torque. The resultant electric loading nevertheless was still significantly higher than the industrial motor. A detailed specification of the DDAT rim-drive propulsion motor design is given in Appendix C.

4.4.5 Summary

The DDAT rim-drive propulsion motor was designed to produce the same full-load torque as an appropriately selected industrial motor. This was achieved with a lower input power level due to the increased diameter of the design. The overall efficiency and power factor of the DDAT rim-drive propulsion motor design, however, were lower and the full-load current was also lower compared to the industrial motor.

The higher magnetising current because of the reduced air-gap reluctance, reduced the air-gap magnetic flux density, which was adjusted by increasing the required number of turns per phase. The electric loading was also increased and although there would normally be concerns about cooling, this motor is assumed to have considerably better cooling because of the significantly increased surface area of the rotor and stator, which are in direct contact with sea-water.

The starting torque of the DDAT rim-drive propulsion motor design was not as high as the industrial motor (approximately 60% lower) but was regarded as sufficient for the intended application, considering the reduction in complexity of the rotor that had been achieved. The simplified rotor design resulted in the radial active material length being reduced by approximately 28%. The weight of the DDAT rim-drive propulsion motor design increased by approximately 26% compared to the industrial motor but
considering the fact that the mean air-gap diameter had increased by approximately 100%, the weight increase was regarded as modest.

### 4.5 Conclusions

This chapter presented the work conducted on the design and development of several concept studies of electric machines for different industrial applications utilising the rim-drive motor topology.

The first section described the LSPM motor topology and gave brief examples of typical rotor configurations including buried PM, surface-mount PM and canned PM, along with the advantages and disadvantages of each configuration. The key aspect to the successful implementation of this type of motor, i.e. the synchronisation process, was also examined. This was followed with the development and preliminary analysis of a benchmark PM rim-drive FE model of a motor produced by Rolls-Royce Marine, which was subsequently modified to produce a canned LSPM rim-drive motor FE model. The effects on the line-start and synchronisation capabilities of the motor were then analysed by altering the frequency, conducting-can thickness and position of the PMs on the rotor.

The second concept study was an in-line seal-less motor pump for use in a nuclear reactor as a main coolant pump, and the third concept study was focused on an drop-down azimuth thruster rim-drive motor for use as an emergency back-up propulsion system on-board a submarine. An overview of the key design parameters for each concept design was given, along with the FEA predicted performance results using the Flux2D software package.

The use of a hybrid ‘canned’ induction rotor design, in which a conventional induction motor ‘deep-bar’ cage was replaced by a simple cage with a low radial depth, along with a series of air-gap cans to provide environmental shielding, and a conducting-can on the rotor to simplify the rotor design, has been shown to produce operating performances suitable for the intended industrial applications of each rim-drive motor.
It was found, however, that the efficiency and power factor of the rim-drive motor designs are reduced compared to their benchmark equivalent motors, which is due to the increased magnetising current caused by the larger air-gap implemented to accommodate the various cans. These factors, however, are considered less important when compared to the advantages the rim-drive motor designs offer in their intended applications.

The concept studies presented in this chapter demonstrate that there is potential for the canned rim-drive motor topology to be implemented in several industrial applications. The FEA predicted results are encouraging; however, all of the concept studies are too large in terms of power rating and torque output for a prototype rim-drive motor to be built and tested at the university to validate the FE models and design process. The next chapter will therefore present the design and analysis of a prototype rim-drive motor of suitable power rating for an industrial application that is appropriate to be manufactured and tested at the university.
Chapter 5

Prototype Rim-drive Motor for Tidal Stream Turbine Positioning Thruster

5.1 Introduction

The previous chapter described the work conducted on the design and analysis of three concept studies of electric motors, utilising the rim-driven electric machine topology. These included a line-start permanent-magnet rim-drive motor for ship propulsion, a seal-less rim-drive motor pump for the nuclear industry and a rim-drive marine propulsion motor for use as an emergency back-up propulsion system on-board a submarine.

This chapter will present the work conducted on the design, analysis and development of a prototype rim-drive motor. The intended industrial application for this motor and the reasons for its requirement will first be discussed, which will be followed by the considerations governing the design. The issue of marine bio-fouling is mentioned before the development of the motor is presented. Key analytical calculations are given, covering all the main aspects of the design. Work is then presented on the modification of the 2-D FE model to take account of 3-D effects in the rotor conducting-can in order to improve the accuracy of the results predicted by the FE model. Analysis results are then presented showing how the conducting-can can be modified to affect the starting torque and current, the peak torque and the full-load operating point and hence, the
efficiency of the motor to achieve the required operational requirements. Finally, the issue of losses, which generate heat in electric machines is discussed, along with the typical modes of heat transfer. This is followed by an introduction to the thermal analysis software package that was used to conduct thermal analyses on the motor design. The results from these analyses are presented and used to optimise the design for its intended industrial application.

5.1.1 Background

The Low Carbon Team at Rolls-Royce, along with Tidal Generation Ltd. (TGL), is developing a tidal stream turbine to generate power from tidal currents, as illustrated in Figure 5.1.

Figure 5.1: Tidal Stream Turbine Developed by Rolls-Royce [134]

The tidal stream turbine shown in Figure 5.1 is a fully submersible machine designed to operate at sea depths typically between 15 and 30 metres, although there is the possibility that it could be implemented at depths up to 80 metres. A prototype turbine capable of generating up to 500 kW is installed at the European Marine Energy Centre (EMEC) in Orkney, Scotland, which is presently undergoing evaluation testing. If this proves successful, commercial tidal stream turbines are envisaged to be designed and manufactured by Rolls-Royce for an operating rating of 1 MW.
5.1.2 Requirement for a Bi-directional Thruster

The tidal stream turbine is affixed to the sea floor on a fixed tripod base, which supports the ‘nacelle’, as illustrated in Figure 5.1. The nacelle houses the generator, gearbox and control system of the turbine. To maximise the capture of useful energy, it is essential that the uni-directional blades of the turbine are steered (yawed) into the direction of the tide. A bi-directional thruster mounted at the rear of the nacelle, as illustrated in Figure 5.2, is therefore required to yaw the nacelle when the tide reverses.

![Figure 5.2: Position of the Required Bi-directional Thruster within the Nacelle of the Tidal Stream Turbine [134]](image)

On-board the prototype tidal stream turbine, yawing is achieved by a hydraulically-driven thruster. Hydraulic thrusters have a number of disadvantages, however, including the need for an appropriately rated hydraulic power pack (pump and accumulators) and the requirement to pass high pressure (25 MPa) hoses through the nacelle (which also has several sections that can be pressurised to expel water, to increase the buoyancy of the nacelle for maintenance purposes). An equivalent electric thruster capable of performing the yawing duty would enhance the turbine by increasing its safety and reliability, reducing maintenance costs and saving valuable space within the nacelle.

5.1.3 Operational Requirements for the Bi-directional Thruster

The mode of operation for the bi-directional thruster is to carry out yawing during the slack period between the end of one tidal phase and the start of the subsequent phase, i.e. when the tidal stream velocities are low. The bi-directional thruster is not required to
attempt to yaw the turbine nacelle against the tidal flow when the velocities are high. Approximately two minutes of operation of the bi-directional thruster is sufficient to rotate the turbine nacelle through 180° during the tidal slack period. Since the tide changes direction every six and one quarter hours, the bi-directional thruster only operates approximately four times per day. Due to this low duty cycle operational requirement, therefore, the electric thruster can be designed to make use of intermittent ratings, minimising the size and cost of the motor. Furthermore, high operating efficiencies are not considered essential.

5.1.4 Electric Bi-directional Thruster Design Considerations

The electric bi-directional thruster (and the tidal stream turbine) is intended for operation in moderately deep coastal locations where strong tides and currents exist. It is to be assumed that the electric thruster will be permanently and fully submersed in sea-water. Its design, therefore, should include full hermetic sealing to prevent the ingress of sea-water into any part of the electric machine. Furthermore, it is to be designed to operate at a minimum depth of 15 metres but up to a maximum depth of 80 metres. As a result, associated pressures (approximately 250 kPa to 900 kPa) should also be considered in the design. Finally, the ambient temperature of the operating environment is considered to be in the range of 0 °C to 30 °C.

The overall diameter of the electric thruster should not exceed 1 metre and the weight should not exceed 400 kg. In addition, the electric thruster is to be designed for a 20 year life, with a two year period between inspection/overhaul. It should also remain maintenance free for periods of two years over the estimated 20 year life of the turbine.

One of the final design considerations is to minimise the impact that the electric thruster would have on the auxiliary electric supply of the tidal stream turbine. The auxiliary supply has relatively low fault level requirements, which are due to two features. Firstly, the turbine is connected to the shore by a long sub-sea cable spanning several kilometres. This cable limits the calculated fault level to around 10 MVA. Secondly, the electric thruster (along with other components within the nacelle) is supplied from a relatively small capacity auxiliary transformer, in relation to the load capacity of the electric thruster. The low fault level on the 400 V internal bus-bar within the nacelle is
such that, if it was subjected to a typical 600-800% starting current for an induction motor, the supply would experience a pronounced voltage dip in excess of 20%. Strengthening the bus-bar by using a larger auxiliary transformer is not economically viable due to the low duty cycle requirements of the electric thruster. The starting current of the electric thruster, therefore, is to be minimised as much as practically possible and not to exceed 200% full-load current.

5.2 Marine Bio-fouling

Although it is outside the scope of this thesis, if the electric thruster was to become a commercial product permanently submerged in a marine environment, consideration would have to be given to the issue of marine bio-fouling, as this may effect the operation and performance through the life of the machine.

Marine bio-fouling is the unwanted growth of various marine life forms such as micro-organisms, algae and barnacles on structures underwater. These growths reduce the hydrodynamic efficiency of sea vessels, for example, which in turn increases operating costs such as fuel, cleaning and painting [135]. In extreme cases, bio-fouling can lead to corrosion, which can weaken a structure, therefore requiring replacement [136].

Bio-fouling organisms produce high-strength adhesives with the ability to bind to structures underwater whilst the surface is wet. Current research in the literature is focused on understanding the properties of these adhesives, so that systems or products can be developed to inhibit bio-fouling [137]. There are currently several products aimed at reducing or inhibiting bio-fouling including anti-fouling paints (with the active ingredient being copper or tributyltin [135, 138]) and fouling-release coatings [137], i.e. silicone elastomeric coatings, which are non-toxic, as apposed to toxic releasing paints.

A study by The Technical Co-operation Program (TTCP) - Materials Technology and Processes Group, was aimed at developing flexible non-toxic coatings for the prevention of bio-fouling on naval vessels. They concluded that they were successful in identifying an environmentally acceptable bio-fouling release coating for use with flexible substrates on the underwater hulls of naval vessels (although the specific product was not mentioned) [139]. Other examples of anti-fouling techniques include an
The electrochemical method, using a carbon-chloroprene electrode [140]; pulse laser irradiation [141]; and electrical discharge to generate acoustic waves [142].

The electric thruster will be driven by a rim-drive motor, which will utilise sea-water in the air-gap and around the periphery of the rotor and stator, to cool it during operation. When the motor is not in use, however, which is approximately 98% of the time, there will be sea-water in the air-gap that is not in motion and may therefore lead to bio-fouling on the rotor-core outer surface and the stator-core inner surface. These surfaces are important to the performance of the motor because any bio-fouling may reduce the air-gap sufficiently to restrict the flow of sea-water and hence, the cooling of the motor would be diminished. Furthermore, if bio-fouling occurred on the other outer surfaces of the motor, the thermal conductivity would be altered and again the cooling may be compromised.

The techniques to reduce or inhibit bio-fouling discussed above are relevant to the tidal stream turbine and also the electric thruster. They should be considered, therefore, if the electric thruster was to be developed for full commercial application.

5.3 Analytical Calculation of the Key Design Parameters

5.3.1 Motor Design Loadings

Electric machines are generally designed to provide a specified level of power for a sustained period of time. There are two fundamental factors, however, that typically limit the output power of a motor. The designer has to pay particular attention to these two factors, which are the electric loading and the magnetic loading.

5.3.1.1 Calculation of Electric Loading

The electric loading affects the internal losses of a motor and is required to be maintained below a defined level before they start to affect the insulation properties of the stator conductors. The losses are generally limited by the thermal design of the motor, which results in a maximum current density that can be sustained in the
conductors. The utilisation of an external cooling source such as water, which is a better thermal conductor than air, can improve the overall efficiency of the cooling system and therefore, provide a benefit by facilitating the maximum permissible current density in the conductors to be increased. This results in further benefits including reduced motor volume, weight and material costs. The total electric loading of a motor is the sum of ampere-conductors around the stator periphery and is defined as [3]:

\[
Total \text{ electric loading} = 6I_c N_{ph}
\]  

(5.1)

where:

\[ I_c = \text{Conductor current [A]} \]
\[ N_{ph} = \text{Number of stator turns per phase} \]

Of more practical significance than the total electric loading, however, is the ‘specific electric loading’. The specific electric loading considers a perfectly sinusoidal current distribution on the inside surface of the stator. The magnitude of this current distribution generates the air-gap MMF distribution. Therefore, the specific electric loading is the ampere-conductor per metre of the periphery of the air-gap, which is calculated from:

\[
Specific \text{ electric loading} = ac = \frac{6I_c N_{ph}}{\pi D}
\]  

(5.2)

where:

\[ D = \text{Mean air-gap diameter [m]} \]

5.3.1.2 Calculation of Magnetic Loading

The magnetic loading is limited by the saturation that the electric steel in the motor can tolerate before the magnetising current starts to increase rapidly. The total magnetic flux in the motor is usually referred to as the magnetic loading, which is determined from [3]:

\[
Total \text{ magnetic loading} = 2p \phi_{pole}
\]  

(5.3)
where:
\[ \phi_{pole} = \text{Magnetic flux per pole [Wb]} \]

Again, of more practical significance is the ‘specific magnetic loading’, which is the average magnetic flux per pole. The specific magnetic loading can therefore be calculated from:

\[
\text{Specific magnetic loading} = \overline{B} = \frac{2 p \phi_{pole}}{\pi D L} \tag{5.4}
\]

where:
\[ L = \text{Axial length [m]} \]

The initial design process of an electric motor normally begins, therefore, by estimating sensible values for the specific electric loading and the specific magnetic loading.

### 5.3.2 Calculation of the Induced Voltage per Phase

If the stator of an electric motor contains a distributed 3-phase AC winding and a voltage is applied to these windings, a sinusoidally-distributed rotating magnetic field is produced in the air-gap, rotating at synchronous speed. If we initially consider the windings to be perfect, i.e. they have no resistance and the winding factors are zero for all the harmonics except for the fundamental, then the air-gap magnetic field can be expressed as:

\[
B_g = \hat{B}_g \cos(\omega t - p \theta) \tag{5.5}
\]

where:
\[ B_g = \text{Air-gap magnetic flux density [T]} \]
\[ \hat{B}_g = \text{Peak air-gap magnetic flux density [T]} \]

The air-gap magnetic field induces voltages in the rotor conductors, which will then drive rotor currents in any conducting closed circuit on the rotor. The induced voltage,
or electromotive force (EMF), can be calculated using Faraday’s Law. If a single loop of wire is initially considered, i.e. one turn of a winding coil, then the EMF induced is:

$$E = -\frac{d\Phi_B}{dt} \tag{5.6}$$

where:

- $E$ = Induced voltage [V]
- $\Phi_B$ = Magnetic flux [Wb]

The magnetic flux linking one turn of a coil that is fully pitched over one pole of a motor is:

$$\Phi_{coil}^B = A_{coil} B_{pole}^{av} \tag{5.7}$$

where:

- $\Phi_{coil}^B$ = Magnetic flux linking the coil [Wb]
- $A_{coil}$ = Cross-sectional area of the coil [m$^2$]
- $B_{pole}^{av}$ = Average magnetic flux density per pole [T]

The average magnetic flux density per pole is:

$$B_{pole}^{av} = \frac{2\hat{B}_g}{\pi} \cos(\omega t) \tag{5.8}$$

Therefore, the peak EMF induced in the coil is:

$$\hat{E}_{coil} = -\frac{d\Phi_{coil}^B}{dt} \Rightarrow \frac{LDB_g \omega}{p} \tag{5.9}$$

And hence, the root mean square (RMS) EMF induced in the coil is:

$$E_{coil}^{rms} = \frac{\sqrt{2\pi LDB_g}}{p} \tag{5.10}$$
where:

\[ E_{\text{coil}}^{\text{rms}} = \text{RMS induced voltage per coil [V}] \]
\[ f = \text{Supply frequency [Hz]} \]

### 5.3.3 Calculation of the Stator Winding Factors per Phase

If one phase of a 3-phase stator winding is initially considered, which is made up of \( N_c \) coils per phase distributed in discrete slots in the stator, there will be an EMF induced in each coil. The EMF induced in each coil, however, will be out of phase to the adjacent coil, reducing the total EMF induced per phase. The reduction in the total EMF induced per phase is known as the distribution factor, \( K_d \), and can be calculated from [143]:

\[
K_{d(n)} = \frac{\sin\left(\frac{mnp\beta_d}{2}\right)}{m \sin\left(\frac{n\beta_d}{2}\right)}
\]  
(5.11)

where:

\( m = \) Number of coils in a phase band
\( \beta_d = \) Angle between coils in a phase band [radians]

Another factor that can reduce the total EMF induced per phase is if the slot pitch is smaller than the pole pitch, i.e. the windings are short pitched. This factor is known as the pitch (or chording) factor, \( K_p \), and is calculated from [143]:

\[
K_{p(n)} = \cos\left(\frac{n\alpha_p}{2}\right)
\]  
(5.12)

where:

\( \alpha_p = \) Angle of short pitching [radians]

The EMF induced in stator windings per phase, which are composed of a number of tightly wound turns per coil, \( N_t \), and are all linked by the same magnetic flux therefore is:
Chapter 5  
Prototype Rim-drive Motor for Tidal Stream Turbine Positioning Thruster

\[ E_{ph}^{rms} = \frac{\sqrt{2}\pi LDN_{ph}K_w\hat{B}_g}{p} \]  \hspace{1cm} (5.13)

where:

\[ K_w = K_dK_p \]  \hspace{1cm} (5.14)

and is known as the winding factor, and:

\[ N_{ph} = N_cN_t \]  \hspace{1cm} (5.15)

is the total number of series connected turns per phase.

5.3.4 Calculation of the Required Number of Turns per Phase

The total number of turns per phase (assuming all turns are connected in series) can be calculated by initially assuming that \( E_{ph}^{rms} \approx V_{ph}^{rms} \) and rearranging Equation 5.13 to yield:

\[ N_{ph} = \frac{V_{ph}^{rms} p}{\sqrt{2}\pi LDK_w\hat{B}_g} \]  \hspace{1cm} (5.16)

5.3.5 Calculation of the Required Stator/Rotor Tooth Width

There are several approaches to calculating the required stator/rotor tooth widths, which include using the average magnetic flux per pole or the RMS magnetic flux per pole. Commonly, however, the approach is to use the \( B_{60} \) point. This approach assumes some saturation of the teeth and allows the electric steel to be pushed further into the saturation region during operation. The required tooth width \( w_t \) can be estimated, assuming a parallel sided tooth, therefore, as follows:

\[ \hat{\phi}_t = (w_t + w_i)B_{60}L \]  \hspace{1cm} (5.17)
where:
\[ \phi_\hat{t} = \text{Peak tooth magnetic flux [Wb]} \]
\[ w_s = \text{Width of slot [m]} \]
\[ w_t = \text{Width of tooth [m]} \]

and:
\[ B_{60} = \frac{\sqrt{3}}{2} B_s \quad (5.18) \]

Therefore, the peak tooth magnetic flux is:
\[ \phi_\hat{t} = (w_t + w_s) \frac{\sqrt{3}}{2} B_s L \quad (5.19) \]

and the peak tooth magnetic flux density is:
\[ \hat{B}_t = \frac{\phi_\hat{t}}{w_s L} = \left( \frac{w_t + w_s}{w_t} \right) \frac{\sqrt{3}}{2} B_s \quad (5.20) \]

where:
\[ \hat{B}_t = \text{Peak tooth magnetic flux density [T]} \]

Noting that:
\[ w_s + w_t = \frac{\pi \text{SID}}{N_s} \quad (5.21) \]

where:
\[ N_s = \text{Number of stator-slots} \]
\[ \text{SID} = \text{Stator inside diameter [m]} \]
The required minimum tooth width can be determined, therefore, by combining Equations 5.20 and 5.21 to yield:

\[ w_t = \frac{\sqrt{3} \hat{B}_g \pi S I D}{2 \hat{B}_g N_s} \]  
(5.22)

5.3.6 Calculation of the Required Core-back Depth

An increase in the number of magnetic poles of a 3-phase winding directly reduces the required core-back thickness, i.e. the part of the magnetic circuit that carries magnetic flux between the poles and hence, the weight and size of a motor. The core-back thickness is chosen carefully so as to not over-saturate under normal operating conditions. The peak core-back magnetic flux density can be calculated from the magnetic flux per pole as follows:

\[ \phi_{pole} = B_{pole} A_{pole} = \frac{2 \hat{B}_g L \pi D}{2p} = \frac{\hat{B}_g LD}{p} \]  
(5.23)

where:

\[ A_{pole} = \text{Cross-sectional area of the pole} [m^2] \]

The magnetic flux in the core-back is half the magnetic flux per pole because the magnetic flux divides equally around the outer stator-core (or inner rotor-core), such that:

\[ \phi_{core} = \frac{\phi_{pole}}{2} \]  
(5.24)

Therefore, the peak core-back magnetic flux density is:

\[ \hat{B}_c = \frac{\phi_{core}}{A_{core}} = \frac{\phi_{core}}{Ly_c} = \frac{\hat{B}_g D}{2py_c} \]  
(5.25)
where:

\[ \hat{B}_c = \text{Peak core-back magnetic flux density [T]} \]
\[ y_c = \text{Core-back radial thickness [m]} \]

Rearranging Equation 5.25, the minimum required core-back radial depth therefore is:

\[ y_c = \frac{\hat{B}_c D}{2pB_c} \tag{5.26} \]

Equations 5.22 and 5.26 are approximate because they do not take account of the non-linear saturation effects in the electric steel. FEA, however, can be used to conduct a sensitivity analysis to identify the minimum tooth width and depth of the core-back before saturation begins to have detrimental effects in a motor design. As a result, the electromagnetic utilisation of the electric steel in the magnetic circuit is maximised.

### 5.3.7 Calculation of the Volt-ampere Rating

As a first approximation, the volt-ampere (VA) rating of a motor is [103]:

\[ S = \frac{P}{\eta \cos \varphi} \tag{5.27} \]

where:

\[ S = \text{Input apparent power (VA)} \]
\[ \cos \varphi = \text{Power factor} \]

### 5.3.8 Calculation of the Stator Current per Phase

The current per phase in the stator windings can be calculated using Equation 5.27 as follows:

\[ I_{ph}^{\text{rms}} = \frac{S}{3V_{ph}^{\text{rms}}} \tag{5.28} \]
5.3.9 Calculation of the Stator Windings Current Density per Turn

The current density per turn in the stator windings can be calculated using Equation 5.28 as follows:

\[
J_{\text{turn}} = \frac{I_{\text{ph}}}{A_{\text{turn}}} 
\]  

(5.29)

where:

\( J_{\text{turn}} = \) Current density per turn (A.mm\(^2\))

\( A_{\text{turn}} = \) Cross-sectional area per turn (mm\(^2\))

The current density per turn, \( J_{\text{turn}} \), is chosen by adjusting the cross-sectional area per turn, \( A_{\text{turn}} \), until an appropriate current density is achieved that can be maintained by the cooling system available to the motor. An estimation of the required stator-slot area can then be calculated from the value determined for \( A_{\text{turn}} \) as follows:

\[
A_{\text{slot}} = \frac{A_{\text{turn}}N_s\chi}{\lambda_s} 
\]  

(5.30)

where:

\( A_{\text{slot}} = \) Stator-slot area (mm\(^2\))

\( \chi = \) Winding type (single-layer = 1, double-layer = 2)

\( \lambda_s = \) Packing factor

5.3.10 Calculation of the Core Losses

The electric steel used for the stator- and rotor-core generates losses that can be split into two main components: the eddy current loss and the hysteresis loss. The eddy current loss can be calculated from [103]:

\[
P_e = \frac{(\pi B_t f)^2}{\rho B_e} 
\]  

(5.31)
where:

\[ P_e = \text{Eddy current loss [W.m}^{-3}\text{]} \]
\[ \hat{B} = \text{Maximum magnetic flux density [T]} \]
\[ t_l = \text{Lamination thickness [m]} \]
\[ \beta_e = \text{Geometric coefficient} \]

and the hysteresis loss can be calculated from [103]:

\[ P_h = \eta_h \hat{B}^{n_h} f \quad (5.32) \]

where:

\[ P_h = \text{Hysteresis loss [W.m}^{-3}\text{]} \]
\[ \eta_h = \text{Material constant} \]
\[ n_h = \text{Exponent with a value between 1.6 and 2} \]

There is another electric steel loss component, known as ‘rotational loss’, which is caused because of the variation of the magnetic flux lines in the lamination plane. There has been no method established as yet, however, for the calculation of this loss [144].

### 5.3.11 Calculation of the Parameters of the Stator Windings

The design of stator windings for a motor typically focuses on two main objectives. Firstly, the windings are designed to produce as nearly a sinusoidal current distribution as practical, given the mechanical and electric constraints. Secondly, they are designed to minimise the phase resistance and leakage reactance. The end-turns of the windings have a detrimental effect on the performance of the motor but are required for connection of the stator conductors. They also increase the resistance of the windings and hence, the stator joule losses. It is beneficial, therefore, to design the turns of the end-windings to be as short as practically possible.

Large motors are therefore usually designed with double-layer windings. This is implemented to promote greater flexibility and hence, achieve the objectives mentioned. Although double-layer windings reduce the fundamental harmonic of the air-gap
magnetic field, they also reduce the higher order winding harmonics, which is beneficial because these typically reduce the output torque of a motor. Generally, double-layer windings are short pitched by $1/6^{th}$ of the number of stator-slots per pole, in order to achieve a balance between minimising both the $5^{th}$ and $7^{th}$ winding harmonics, which are usually the most troublesome. As an example, the effect on the air-gap harmonic fields of short pitching the double-layer windings of the 200 kW benchmark motor used in Chapter 4, Section 4.3.3, which has 18 slots per pole, is presented in Table 5.1.

**Table 5.1:** Winding Factors for the First Five Air-gap Magnetic Field Harmonics Produced by the Double-layer Windings for the 200 kW Industrial Motor that are Short Pitched by Several Stator-slots

<table>
<thead>
<tr>
<th>Number of Stator-slots Short Pitched</th>
<th>$n$</th>
<th>0</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
</tr>
</thead>
<tbody>
<tr>
<td>1$^{st}$</td>
<td></td>
<td>0.956</td>
<td>0.953</td>
<td>0.942</td>
<td>0.924</td>
<td>0.898</td>
<td>0.867</td>
</tr>
<tr>
<td>5$^{th}$</td>
<td></td>
<td>0.197</td>
<td>0.179</td>
<td>0.127</td>
<td>0.031</td>
<td>-0.034</td>
<td>-0.113</td>
</tr>
<tr>
<td>7$^{th}$</td>
<td></td>
<td>-0.145</td>
<td>-0.119</td>
<td>-0.05</td>
<td>0.038</td>
<td>0.111</td>
<td>0.145</td>
</tr>
<tr>
<td>11$^{th}$</td>
<td></td>
<td>-0.102</td>
<td>-0.058</td>
<td>-0.035</td>
<td>0.098</td>
<td>0.078</td>
<td>-0.009</td>
</tr>
<tr>
<td>13$^{th}$</td>
<td></td>
<td>0.092</td>
<td>0.039</td>
<td>-0.059</td>
<td>-0.089</td>
<td>-0.016</td>
<td>0.075</td>
</tr>
</tbody>
</table>

Table 5.1 shows that by short pitching the windings by three slots of the 18 slots per pole, i.e. $1/6^{th}$, the $5^{th}$ and $7^{th}$ winding harmonics are at their minimum in terms of magnitude and were reduced by 14.6% and 10.7%, respectively, compared to a single-layer, fully pitched winding configuration. As mentioned, the magnitude of the fundamental harmonic will also be reduced and in this example, it was reduced by only 3.2%. These results clearly show the benefit of short pitching windings to reduce the higher order harmonics.

### 5.3.11.1 Calculation of the Stator End-windings Leakage Inductance

The leakage inductance of the stator end-windings is notoriously difficult to calculate because of the magnetic coupling between the end-windings and the rotor end-rings, along with the coil-to-coil coupling. As a first approximation, however, the calculation of end-windings leakage inductance for random wound coils by Lipo can be used [145]. Since these coils are more flexible, the shape of the end-w windings tends to approximate a rectangular shape with rounded edges, as illustrated in Figure 5.3.
The total leakage inductance of the end-windings per phase was estimated as follows:

\[
L_{ew} = \frac{4K_2^2N_{ph}^2}{p} (L_{ew1} + L_{ew2} + L_{ew3} + L_{ew4}) \tag{5.33}
\]

where:

\[ L_{ew} = \text{End-windings leakage inductance per phase [H]} \]

and:

\[
L_{ew1} = \mu_0 \left(\frac{l_{ew} - P\tau_{pl}}{2\pi}\right) \log_e \left(\frac{P\tau_{pl}}{\Delta R}\right) \tag{5.34}
\]

\[
L_{ew2} = 0 \tag{5.35}
\]

\[
L_{ew3} = \frac{\mu_0}{\pi} \left(\frac{P\tau_{pl}}{2}\right) \log_e \left(\frac{l_{ew2}}{2\Delta R}\right) \tag{5.36}
\]

\[
L_{ew4} = \frac{\mu_0 l_{ew}}{8\pi} \tag{5.37}
\]

The length of the end-windings, \(l_{ew}\), can be calculated by initially assuming semi-circular coils when formed. Therefore:
\[ l_{ew} = \pi r_c \sin\left(\frac{\pi p}{N_s}\right) \] 

(5.38)

where:

\[ r_c = \text{Radius to centre of a stator-slot [m]} \]

The coil span, \( P r_{p1} \), can be calculated from:

\[ P r_{p1} = \frac{2\pi r_c \lambda_c}{N_s} \] 

(5.39)

where \( \lambda_c \) is the pitch of the coils in number of stator-slots. Finally, \( \Delta R \), can be calculated from:

\[ \Delta R = \sqrt{\frac{A_{\text{coil}}}{\pi}} \] 

(5.40)

where:

\[ \Delta R = \text{Radius of an equivalent circle of the same cross-sectional area as a coil [m]} \]

### 5.3.11.2 Calculation of the Stator Windings Phase Resistance

The DC resistance of the stator windings per phase can be calculated by estimating the average length of one turn and multiplying it by the total number of series turns per phase. The resistance per phase is therefore calculated as follows:

\[ R_{ph} = \frac{\rho l_w}{A_{\text{turn}}} \] 

(5.41)

where:

\[ \rho = \text{Resistivity of the conductor material [\Omega.m]} \]

\[ l_w = \text{Total length of the series turns per phase [m]} \]
The total length of series turns per phase, $l_w$, can be estimated as follows:

$$l_w = (2L + \pi P \tau_0) N_{ph}$$  \hspace{1cm} (5.42)

The resistivity of the conductor material should be corrected for the full-load operating temperature of the motor as follows:

$$\rho = \rho_0[1 + \alpha \tau (T - T_0)]$$  \hspace{1cm} (5.43)

where:

- $\rho_0 = \text{Resistivity of conductor material at specified temperature (Ω.m)}$
- $\alpha$ = Temperature coefficient of resistivity ($^\circ\text{C}^{-1}$)
- $T$ = New operating temperature of material ($^\circ\text{C}$)
- $T_0$ = Temperature of material at specified resistivity ($^\circ\text{C}$)

## 5.4 Prototype Rim-drive Motor Design for Electric Thruster

### 5.4.1 Specifications for the Prototype Rim-drive Motor

The key specifications for the bi-directional electric thruster were provided by Rolls-Royce so that a rim-drive motor solution could be developed, which could subsequently be compared to the hydraulic thruster system. The specifications were: 30 kW, 400 V, 3-phase supply, 50 Hz, an operating speed of approximately 300 rpm and a propeller diameter of 0.7 m. These requirements were estimated to be capable of generating a propeller thrust of 4.3 kN.

The initial design investigated for the electric thruster was a line-start permanent-magnet (LSPM) motor topology following on from the work presented in Chapter 4, Section 4.2.6. After discussions with Rolls-Royce Marine, however, an induction motor topology was selected as an alternative. This was due to the cost of the PM material and additional assembly expense of the LSPM topology. A ‘canned’ rim-drive induction motor topology design was therefore developed.
Due to the relatively large diameter of the rim-drive motor compared to the low power requirement, the design of the rotor was simplified by completely removing the squirrel-cage and using only a conducting-can on the surface of the rotor to produce torque. This resulted in the active material and weight of the motor being reduced whilst retaining its functionality.

The canned rim-drive induction motor topology is novel in several aspects, therefore, as it uses a series of air-gap cans to provide both motor operation and environmental protection whilst eliminating the requirement for a traditional squirrel-cage. An illustration of the topology for the prototype rim-drive motor is presented in Figure 5.4.

![Figure 5.4: Topology of the Prototype Rim-Drive Motor for the Electric Thruster](image)

Both the rotor- and stator-core are normally laminated in electric machines to reduce the eddy current loss and improve efficiency. If costs were required to be minimised for the electric thruster system, however, the rotor-core of the rim-drive motor could be manufactured from solid steel due to the low rotor frequency and low duty cycle operation. A solid rotor-core would also increase its mechanical rigidity and thermal attributes [103], which may be beneficial in this application due to the aspect ratio of the motor.
5.4.2 Development of the Prototype Rim-drive Motor

The first stage in the development of the prototype rim-drive motor was to select a 30 kW industrial induction motor, as was done with the concept studies in the previous chapter. This was modelled using Flux2D and used as a benchmark to compare the performance of the prototype rim-drive motor design. The FEA predicted torque, speed and phase current (at full-load) were within 1% compared to the experimental test data from the industrial motor and were therefore considered acceptable.

The second stage of the design was to modify the benchmark FE model to the specifications for the electric thruster defined previously. In the case of rim-drive motors for use as thrusters, the diameter is usually the first modification since it is constrained by the dimensions of the propeller (or the mounting pod), for example. The inside diameter of the rotor-core, therefore, was increased to the outside diameter of the propeller tips, plus an allowance for a 20 mm thick mounting ring, which would be used to mount the propeller assembly to the rotor-core of the prototype rim-drive motor. This produced a rotor inside diameter of 0.74 m.

The mean air-gap diameter had increased considerably compared to the industrial motor (approximately 800%) and since torque is closely proportional to $D^2L$, the increased diameter resulted in a substantial increase in torque produced by the prototype rim-drive motor design. To reduce the torque, therefore, the axial length of the motor was subsequently decreased, resulting in the distinctive rim-drive motor topology, (as illustrated in Figure 1.2).

The axial length was adjusted until the motor was capable of producing 30 kW at full-load, with an overload torque capability of 2:1 (peak torque to full-load torque). It was determined that this resulted in a required axial length of approximately 50 mm. This axial length was considered too short in relation to the diameter of the motor to achieve mechanical rigidity. The axial length, therefore, was set at a minimum length of 100 mm for structural reasons.
5.4.3 Calculation of the Required Number of Pole-pairs

One of the key differences between the industrial motor and the prototype rim-drive motor was a reduction in the required operating speed. The prototype rim-drive motor had a speed requirement of approximately 300 rpm, which increased the required number of poles of the motor. An increased pole number can be beneficial in a large diameter motor, however, as the length of the end-windings can be reduced, although increasing the pole number will reduce the power factor. The required number of pole-pairs can be calculated from [3]:

\[
\omega_s = \frac{\omega}{p} = \frac{2\pi f}{p}
\]  

(5.44)

where:

\( \omega_s \) = Synchronous angular frequency [radians.s\(^{-1}\)]

or in terms of rpm:

\[
S_{rpm} = \frac{60f}{p}
\]  

(5.45)

where:

\( S_{rpm} \) = Synchronous speed [rpm]

Rearranging Equation 5.45 to determine the required number of pole-pairs therefore, yields:

\[
p = \frac{60f}{S_{rpm}}
\]  

(5.46)

Using Equation 5.46, ten pole-pairs would be necessary for a supply frequency of 50 Hz and a speed requirement of approximately 300 rpm. Once the prototype rim-drive motor was operating under load, however, the slip would increase and the operating speed would reduce. Nine pole-pairs were chosen, therefore, which resulted in a synchronous speed of approximately 333 rpm.
5.4.4 Calculation of the Required Number of Stator-slots

The required number of stator-slots is determined as a compromise between minimising the slot leakage inductance (by keeping the ratio of the slot depth to slot width to a minimum [146]) and maximising the number of slots per pole per phase, which reduces the harmonics of the air-gap magnetic field [103]. Considering the prototype rim-drive motor design, therefore, the minimum number of slots would be 54 for one slot per pole per phase.

This resulted in a tooth width of approximately 20 mm, which was considered excessive. The number of slots per pole per phase was increased to two, resulting in a tooth width requirement of approximately 10 mm. A tooth width of 10 mm, however, was considered the minimum tooth width to achieve mechanical rigidity given the diameter of the motor. Therefore, two slots per pole per phase were chosen as the optimum number for the prototype rim-drive motor design.

5.4.5 Design of the Air-gap

The air-gap (filled with sea-water) of the prototype rim-drive motor design was increased from 1.25 mm to 3.5 mm. This was to accommodate two cans on the outer surface of the rotor and one can on the inner surface of the stator. The first can on the surface of the rotor was a 0.5 mm thick copper-can. This is the conducting-can used to develop the motor torque. A second can of epoxy resin, 0.5 mm thick, was implemented on top of the copper-can and was used for containment/hermetrical sealing purposes.

The inner surface of the stator also had an environmental can, 0.5 mm thick, of epoxy resin to protect the stator-core. The remainder of the space, i.e. 2 mm, was the resulting physical air-gap and was considered to be the minimum mechanical clearance allowable, given the diameter of the motor design. The configuration of the air-gap for the prototype rim-drive motor design is illustrated in Figure 5.5.
5.4.6 Design of the Core-back

The next stage in the design of the prototype rim-drive motor was to modify the radial depth of the rotor and stator core-backs to ensure a safe magnetic flux density operating level. The core-back depth was chosen, therefore, to avoid saturation under normal operating conditions. Using Equation 5.26, the initial required core-back depth was calculated to be approximately 25 mm. The depth of the core-back, along with the tooth widths, however, would be optimised during FE sensitivity analyses.

5.4.7 Design of the Stator Windings

A 3-phase double-layer winding, short pitched by 1/6th of a pole, was used to achieve the benefits described in Section 5.3.1. Short pitching the windings had the advantage of reducing the size of the end-windings basket, which would also reduce the leakage inductance. Figure 5.6 illustrates the FE model of the benchmark motor and the initial prototype rim-drive design.
5.4.8 Simple 2-D Calculation of the Power Loss in a Conducting-can

The total power loss in a conducting-can can be estimated from the analytical expression derived by Robinson [147]. Therefore, initially considering a finite elemental strip in a conducting-can with a thickness of $dx$, as shown in Figure 5.7.

The EMF induced in the finite elemental strip because of the stator magnetic field, Equation 5.5, is:

$$\hat{e}_{dc} = \hat{B}_g \left( \frac{s \omega}{2p} \right) \frac{LD}{2}$$  \hspace{1cm} (5.47)
where:

\[ \hat{e}_{ds} = \text{Peak EMF induced in the finite elemental strip [V]} \]

\[ s = \text{Slip} \]

The DC resistance of the finite elemental strip is:

\[ R_{ds} = \frac{\rho L_{ds}}{A_{dx}} = \frac{\rho L_{dx}}{h dx} \quad (5.48) \]

where:

\[ R_{ds} = \text{Resistance of the finite elemental strip [}\Omega]\]

\[ L_{ds} = \text{Axial length of the finite elemental strip [m]} \]

\[ A_{dx} = \text{Cross-sectional area of the finite elemental strip [m}^2] \]

\[ h = \text{Radial thickness of the conducting-can [m]} \]

The power loss in the finite elemental strip can be calculated as follows:

\[ P_{ds} = \frac{\hat{e}_{ds}^2}{2R_{ds}} = \frac{\left(\hat{B}_g D s \omega\right)^2 L h dx}{\rho 32 \pi^2} \quad (5.49) \]

The total power loss, \( P_o \), in the conducting-can, therefore, is:

\[ P_o = P_{ds} \times \pi D = \frac{\left(\hat{B}_g s \omega\right)^2 D^3 L h \pi}{\rho 32 \pi^2} \quad (5.50) \]

### 5.4.9 2-D Model of the Current Distribution in a Conducting-can

During operation of the prototype rim-drive motor, the stator air-gap magnetic field will induce an EMF in any material that possesses conductivity. Eddy currents, therefore, flow in the rotor copper-can. One of the disadvantages of a 2-D FE model, however, is that it assumes that all field quantities do not change along the axial length of the motor. For a squirrel-cage rotor this is a reasonable assumption if the end-ring impedance is added externally via a linked circuit. The approximation is less reasonable in the case of
a conducting-can because the currents are not constrained to flow axially in the solid material, as illustrated in Figure 5.8.

![Figure 5.8: Eddy Current Distribution in a Conducting-can Over One Pole-pair](image)

Figure 5.8 shows clearly that the air-gap magnetic field produces roughly circular current paths in the conducting-can, which were determined by implementing the equations presented in [148] in Matlab. Currents in a caged rotor, however, are constrained to flow axially down the rotor-bars and around the end-rings. As a consequence, the 2-D FE model underestimates the eddy current loss that is produced in the rotor conducting-can and hence, the torque that is developed by the prototype rim-drive motor design.

### 5.4.10 Pseudo 3-D FE Model of the Power Loss in a Conducting-can

Since the rotor currents do not just flow axially in the conducting-can, it cannot be identified as a circuit in the FE model. It is not possible, therefore, to add an effective external end-ring impedance to the FE model, as is the case with a squirrel-cage rotor. The conductivity of the conducting-can can be modified in the FE model, however, to
produce the same fundamental rotor loss by implementation of the analytical expressions that were developed in the late 1950s by Russell and Norsworthy [148]. These analytical expressions were initially developed to predict the eddy current loss in a stationary conducting shell (pipe wall) of electric motors used in the nuclear industry (similar to the concept presented in Chapter 4, Section 4.2.4.3). The analytical expressions can also be used in the case of the conducting-can on the rotor of the prototype rim-drive motor to effectively create a pseudo 3-D FE model.

Russell and Norsworthy made several assumptions to reduce the problem to its simplest terms, i.e. one of relative motion between a magnetic field and a thin conducting sheet. Firstly, they assumed that the magnetic flux density in the air-gap was radial and produced a travelling sinusoidal waveform varying both in space and time. Secondly, they assumed that the magnetic flux did not vary with axial position inside the air-gap and that it was zero elsewhere, i.e. fringing was disregarded. Finally, they assumed that the currents in the conducting sheet did not alter the air-gap magnetic field. An illustration of the problem is presented in Figure 5.9.

\[ B_{(r=a)} = B_g \cos (\omega t - p\theta) \]

*Figure 5.9: Open-ended Shell (conducting-can) with Symmetrical Overhang [6]*

In Figure 5.9, the active part of the motor is region 1, i.e. \( |x| \leq L \). For region 2, i.e. \( |x| \geq L \), where the conducting-can extends beyond the active part of the motor, the air-gap magnetic field is assumed to be zero. The radial thickness of the conducting-can, \( h \), was
assumed to be negligible with regards to the mean radius of the conducting-can, \( a \). The radial variation of all quantities could therefore be neglected. These assumptions allowed the problem to be reduced to one in two variables. As it does not have circular symmetry, the problem can be expressed in two co-ordinate systems:

1. Cylindrical polar co-ordinates in reference to a cylinder of fixed radius, \( a \), or
2. Cartesian co-ordinates in reference to an equivalent plane rectangular sheet, as shown in Figure 5.10.

Transformation between the two co-ordinate systems can be accomplished by using the following relationships [148]:

\[
\pi a = b \tag{5.51}
\]

and:

\[
y = -a \theta \tag{5.52}
\]

The eddy current loss generated in a conducting-can of uniform thickness and conductivity can be modified by the analytical expression, \( K_S \), developed by Russell and Norsworthy [148], which is:
\[ K_S = \left[ 1 - \frac{\tanh\left( \frac{pL}{2a} \right)}{\left( \frac{pL}{2a} \right) \left( 1 + \lambda_o \right)} \right] \]  
(5.53)

where, \( \lambda_o \), is the overhang coefficient, which can be calculated from:

\[ \lambda_o = \tanh\left( \frac{pL}{2a} \right) \tanh\left( \frac{\alpha pL}{2a} \right) \]  
(5.54)

In the case of the prototype rim-drive motor design, however, there is no overhang of the rotor conducting-can past the end of the active length of the stator. The overhang length, \( \alpha \), is zero and, therefore, the overhang coefficient, \( \lambda_o \), is also zero. As a result, Equation 5.53 simplifies to:

\[ K_S = \left[ 1 - \frac{\tanh\left( \frac{pL}{2a} \right)}{\left( \frac{pL}{2a} \right)} \right] \]  
(5.55)

The total eddy current loss, \( P_c \), in the rotor conducting-can of the prototype rim-drive motor design, therefore, can be calculated by multiplying Equation 5.50 by Equation 5.55:

\[ P_c = P_o K_S \]  
(5.56)

To illustrate the effect of the correction factor, \( K_S \), a sensitivity analysis was conducted by modifying the aspect ratio dimensions of the prototype rim-drive motor design. Firstly, Figure 5.11 illustrates \( K_S \) as a function of mean air-gap diameter for a fixed axial length.
Figure 5.11: $K_S$ as a Function of Mean Air-gap Diameter for the Prototype Rim-drive Motor Design with a Fixed Axial Length of 0.1 m

Figure 5.11 shows that as the mean air-gap diameter is increased, the correction factor $K_S$ reduces. The reduction in conductivity of the conducting-can is to be expected, however, because an increase in the diameter of the motor increases the effective path length of the end-turns, increasing the resistance of the eddy current paths. This effect is illustrated in Figure 5.12.

Figure 5.12: Illustration of Path Lengths of Eddy Currents for Two Different Diameter Conducting-cans Over One Pole-pair
A second sensitivity analysis was conducted by modifying the axial length, whilst maintaining a fixed mean air-gap diameter, to determine the effect on the correction factor, $K_S$. The results of this analysis are presented in Figure 5.13.

![Graph showing $K_S$ as a Function of Axial Length for the Prototype Rim-drive Motor Design with a Fixed Mean Air-gap Diameter of 0.79 m](image)

**Figure 5.13:** $K_S$ as a Function of Axial Length for the Prototype Rim-drive Motor Design with a Fixed Mean Air-gap Diameter of 0.79 m

Figure 5.13 shows that decreasing the axial length of the prototype rim-drive motor again results in a reduction in the correction factor $K_S$. This reduction in the conductivity of the conducting-can can again be expected because the resistance of the end-turns now becomes a dominant factor in the total path length of the eddy currents, as is illustrated in Figure 5.14.
The RID of the prototype rim-drive motor was fixed by the diameter of the specified propeller, therefore, only the axial length could be modified to adjust the torque developed. As the axial length was decreased, however, $K_S$ also reduced. For the initial prototype rim-drive motor design, with a mean air-gap diameter of 0.79 m and an axial length of 0.1 m, $K_S$ was calculated to be approximately 0.28. This resulted in a reduction in the conductivity of the rotor conducting-can by approximately 70%.

Clearly, this factor cannot be ignored in the design of canned rim-drive motors. Modifying the total power loss of the rotor conducting-can in the FE model, therefore, was implemented by adjusting the electric properties of its material. The resistivity, $\rho$, of the conducting-can material was modified with the reciprocal of $K_S$ to produce a pseudo 3-D FE model. The effect of changing the resistivity of the rotor copper-can with $K_S$ on the torque and phase current of the prototype rim-drive motor design, is illustrated in Figures 5.15 and 5.16, respectively.
Figure 5.15: Effect of $K_S$ on the FEA Predicted Torque Produced by the Prototype Rim-drive Motor Design

Figure 5.16: Effect of $K_S$ on the FEA Predicted Phase Current of the Prototype Rim-drive Motor Design
Figure 5.15 shows that implementing $K_S$ in the FE model has a substantial effect on the torque developed by the prototype rim-drive motor design for a given slip value. For example, the starting torque is overestimated in the 2-D FE model by approximately 47% compared to the pseudo 3-D FE model. Figure 5.16 shows the effect $K_S$ has on the phase current, which is also substantial. The starting current is also overestimated, in this case, by approximately 54% compared to the pseudo 3-D FE model. These results further demonstrate why the correction factor, $K_S$, cannot be ignored in the design and development of the canned rim-drive motor topology.

5.4.11 Effects on Motor Performance by Modifying the Conducting-can Properties

By adjusting the resistance of the rotor conducting-can, various operating parameters of the prototype rim-drive motor design can be altered to comply with the specification requirements of the electric thruster such as the starting torque and starting current, the peak torque speed and the full-load operating point.

One of the key design requirements of the electric thruster was to have a low starting current, to reduce the impact on the auxiliary power supply of the tidal stream turbine. The reduction in starting current can be achieved by increasing the rotor resistance of the prototype rim-drive motor, which is accomplished by modifying the thickness of the rotor conducting-can or its material composition. For example, reducing the radial thickness of the conducting-can (the axial length is fixed by the torque requirement of the motor) increases the rotor resistance and hence, reduces the rotor current. Increasing the rotor resistance also increases the starting torque, which is beneficial for the prototype rim-drive motor, due to the large inertia caused by the relatively large diameter of the rotor.

5.4.11.1 Copper Conducting-can

A FEA sensitivity analysis was conducted on the FE model of the prototype rim-drive motor design to determine the influence the rotor conducting-can thickness has on the torque and phase current produced by the prototype motor design, as illustrated in Figures 5.17 and 5.18, respectively.
Chapter 5  
Prototype Rim-drive Motor for Tidal Stream Turbine Positioning Thruster

Figure 5.17: FEA Predicted Results of Torque vs. Slip for the Prototype Rim-drive Motor with a Copper-can Composed of Several Different Thicknesses

Figure 5.18: FEA Predicted Results of Phase Current vs. Slip for the Prototype Rim-drive Motor with a Copper-can Composed of Several Different Thicknesses
Figure 5.17 shows, as expected, that as the copper-can is increased in thickness, reducing its resistance, the slip for peak torque reduces. For a copper-can thickness of 4 mm, peak torque occurs at a slip of 0.6, whereas for a copper-can thickness of 2 mm, peak torque occurs at a slip of 1. Below a thickness of 2 mm, the copper-can is not producing maximum torque. For maximum starting torque, therefore, a copper-can thickness of 2 mm would be the optimal thickness.

Figure 5.18 shows that as the copper-can is increased in thickness, the phase current across the slip curve also increases. Obviously, for minimum starting current, a copper-can thickness of 0.5 mm would be the best option. As mentioned, however, this would be at the expense of maximum starting torque and, therefore, would not be making maximum use of the copper-can.

5.4.11.2 Aluminium Conducting-can

The FE model was then modified by replacing the rotor copper-can with an aluminium-can. The FEA sensitivity analysis was conducted again to determine the effect of changing the thickness of the aluminium-can on the torque and phase current produced by the prototype rim-drive motor design. The results for torque and phase current are illustrated in Figures 5.19 and 5.20, respectively.

Figure 5.19: FEA Predicted Results of Torque vs. Slip for the Prototype Rim-drive Motor with an Aluminium-can Composed of Several Different Thicknesses
Figure 5.20: FEA Predicted Results of Phase Current vs. Slip for the Prototype Rim-drive Motor with an Aluminium-can Composed of Several Different Thicknesses

Figure 5.19 also shows that as the aluminium-can is increased in thickness, the starting torque increases. Maximum starting torque, however, is not achieved until the aluminium-can has a thickness of 4 mm. This would be at the expense of the magnetising current, which would be increased if an aluminium-can was used to produce maximum starting torque compared to a 2 mm copper-can, as the required effective air-gap length would now be an additional 2 mm thick.

One of the key design requirements of the electric thruster was to minimise the starting current of the motor and not to exceed 200% full-load current. Although an aluminium-can thickness of 0.5 mm could be used to minimise the starting current the most (as shown in Figure 5.20), this would not be utilising the maximum torque available from the prototype rim-drive motor design. A 2 mm thick copper-can was therefore chosen as the optimum thickness and material for the prototype rim-drive motor.
5.5 Thermal Analysis of the Prototype Rim-drive Motor Design

5.5.1 Introduction

During the operation of an electric machine losses are generated, producing heat. These include copper (or joule) losses in the stator and rotor winding circuits; iron (or core) losses in the electric steel and also mechanical losses including bearing friction and windage loss. The removal of this heat is as significant as the electromagnetic design of the machine because the materials used in its manufacture need to be maintained within safe operating temperatures, defined by their electric and mechanical characteristics.

If these materials are worked above their designed operating temperatures, the service life of these materials will generally be reduced. For example, it has been estimated that 37% of electric machines fail prematurely due to the deterioration of their insulating system [149]. The service life of conductor insulating materials used in windings can be calculated by [103]:

\[
Y = Ge^{-\delta T_0}
\]

where:

- \( Y \) = Service life in years
- \( T_0 \) = Operating temperature [°C]
- \( G \) = Material constant
- \( \delta \) = Material constant

As an example, for a class A insulating material, \( G \) and \( \delta \) are typically \( 7.15 \times 10^4 \) and 0.08, respectively [103]. Therefore, if a class A insulating material was operated at 100 °C, the expected service life would be 24 years. If the operating temperature was increased by 20% to 120 °C, however, the service life would reduce almost 80% to five years. Excessive heat may also cause mechanical failures such as soldered joints to separate or fractures in materials due to different thermal expansion rates. It is important, therefore, that the temperature rise and steady-state operating temperatures of
various parts of a machine are accurately predicted during the design stage because it is a key factor in determining the machine output power and reliability.

5.5.2 Modes of Heat Transfer

There are three modes of heat transfer in an electric machine: conduction, convection and radiation. Conduction is the transfer of thermal energy (heat) in a material from a region of higher temperature to a region of lower temperature. This takes place in the solid materials of the machine, i.e. the copper, insulation and electric steel. The rate of heat conduction can be calculated from Fourier’s Law, as follows [150]:

$$Q = -kA_d \frac{dT}{dx}$$  \hspace{1cm} (5.58)

where:

- $Q$ = Heat transfer [W]
- $k$ = Thermal conductivity of the material [W.K$^{-1}$.m$^{-1}$]
- $A_d$ = Cross-sectional area of conduction [m$^2$]
- $\frac{dT}{dx}$ = Temperature gradient [K.m$^{-1}$]

Convection is the transfer of heat between a surface and a fluid, and can be separated into two classes: natural convection and forced convection. Natural convection causes a fluid such as air or water to become less dense as it absorbs energy. This will result in the fluid rising from the source of heat and being replaced by cooler fluid resulting in a convection current cycle.

Forced convection, however, is the transfer of heat in a fluid in motion due to an external force such as a fan. It is dependent on the velocity at which the fluid is in motion and results in two types of flow: laminar flow and turbulent flow. Laminar flow occurs at low velocities parallel to the surface when a fluid flows in parallel layers, which do not disturb adjacent layers. Turbulent flow, however, does not flow in parallel layers but gives rise to eddy currents forming around the surface due to higher velocities in the fluid. The heat transfer by convection can be calculated from Newton’s Law, as follows [150]:
\( Q = h_c A_v (T_s - T_a) \) \hspace{1cm} (5.59)

where:

- \( h_c \) = Convection heat transfer coefficient [W.K\(^{-1}\).m\(^{-2}\)]
- \( A_v \) = Cross-sectional area of convection [m\(^2\)]
- \( T_s \) = Temperature of the emitting surface [K]
- \( T_a \) = Ambient temperature [K]

Finally, radiation is the transfer of heat between two (or more) bodies due to the emission of electromagnetic waves. These waves are produced by vibrating electrons in the molecules of the material at the surface of the body. Radiation requires no medium to exist between the bodies for heat transfer to take place. This mode of heat transfer, however, is considered to generally be insignificant in electric machines [103]. For completeness, the heat transfer by radiation can be calculated from Stefan-Boltzmann Law, as follows [150]:

\[ Q = \sigma_s A_r \varepsilon_r (T_1^4 - T_2^4) \] \hspace{1cm} (5.60)

where:

- \( \sigma_s \) = Stefan-Boltzmann constant \((5.6704 \times 10^{-8})\) [W.m\(^{-2}\)K\(^{-4}\)]
- \( A_r \) = Cross-sectional area of radiating surface [m\(^2\)]
- \( \varepsilon_r \) = Emissivity of radiating surface
- \( T_1 \) = Absolute temperature of radiating surface [K]
- \( T_2 \) = Absolute temperature of absorbing surface [K]

### 5.5.3 Motor-CAD Thermal Analysis Software

To determine the steady-state operating temperatures in the various parts of the prototype rim-drive motor design such as the stator windings and the rotor conducting-can, a thermal analysis model was developed. The Motor-CAD software package (version 5) from Motor-Design Ltd. was chosen to conduct the thermal analysis of the prototype rim-drive motor design. Motor-CAD uses a network (lumped circuit) analysis approach, as illustrated in Figure 5.21.
The lumped circuit approach is generally the most common approach used in the thermal analysis of electric machines [151-155]. Nodes are located at significant material intersections in the machine geometry such as the slot insulation to the stator-core electric steel, stator-core to the machine housing, etc. These nodes form a thermal resistance network comparable to an electric resistance network, where temperature is the equivalent of voltage; power is the equivalent of current; and, thermal resistance is the equivalent of electric resistance. For conduction, the thermal resistance is [150]:

$$ R_d = \frac{L_d}{kA_d} \quad (5.61) $$

where:

- \( R_d \) = Conduction thermal resistance \([K.W^{-1}]\)
- \( L_d \) = Path length of conduction \([m]\)

For convection, the thermal resistance is [150]:
\[ R_v = \frac{1}{h_c A_v} \]  

where:

\[ R_v = \text{Convection thermal resistance [K.W}^{-1}] \]
\[ h_c = \text{Convection heat transfer coefficient [W.K}^{-1}\text{m}^{-2}] \]

Finally, for radiation, the thermal resistance is [150]:

\[ R_r = \frac{1}{h_r A_r} \]  

where:

\[ R_r = \text{Radiation thermal resistance [K.W}^{-1}] \]
\[ h_r = \text{Radiation heat transfer coefficient [W.K}^{-1}\text{m}^{-2}] \]

If a transient thermal analysis is required, thermal capacitances are also added to the lumped circuit, which are calculated from [156]:

\[ c_{th} = m_t c_p \]  

where:

\[ c_{th} = \text{Thermal capacitance [J.K}^{-1}] \]
\[ m_t = \text{Mass [kg]} \]
\[ c_p = \text{Specific heat capacity [J.kg}^{-1}\text{K}^{-1}] \]

Parameterised data to construct the thermal model is entered into Motor-CAD by means of editors. For example, Figures 5.22 and 5.23 illustrate the radial and axial cross-sectional editors, respectively. Motor-CAD also provides material databases, which help to select appropriate values for conductivity, specific heat coefficients and fluid viscosities, etc. These editors and databases simplify data input but produce a detailed thermal model capable of both steady-state and transient analyses. Sensitivity investigations can also be conducted by modifying parameters such as fluid flow rates to determine the effects on the temperatures within a machine design.
Figure 5.22: Motor-CAD Radial Cross-sectional Editor

Figure 5.23: Motor-CAD Axial Cross-sectional Editor
5.5.4 Heat Transfer Model of the Prototype Rim-drive Motor Design

The geometric parameters of the prototype rim-drive motor design were entered into the radial and axial cross-section editors shown in Figures 5.22 and 5.23, respectively. The losses calculated from the Flux2D FE model of the prototype rim-drive motor design predicted approximately 3.3 kW of power was being generated in the rotor copper-can and 10.2 kW in the stator windings at full-load. These values were entered into the input data editor, along with values for the rotor and stator iron losses. The windage loss is automatically calculated in the software but no value for the friction was entered at this stage.

The prototype rim-drive motor is designed to be permanently submersed and operated in sea-water. A cooling jacket was therefore added to the thermal model in Motor-CAD, with sea-water chosen as the fluid medium. The specifications for the electric thruster defined the ambient operating temperature of the environment to be between 0 °C and 30 °C. 30 °C was therefore chosen as the worst case ambient temperature of the sea-water for the thermal model.

The sea-water was allowed to flow through the cooling jacket, the air-gap and also through the inside of the rotor assembly. The velocity of sea-water through the inside of the rotor assembly at operating speed was calculated to be 0.175 m.s\(^{-1}\). A sensitivity investigation, therefore, was also conducted to determine the effect the sea-water velocity would have on the internal temperatures of the prototype rim-drive motor design.

5.5.5 Thermal Analysis Predicted Results

A transient thermal analysis was conducted to determine the temperature distribution inside the prototype rim-drive motor design at full-load until steady-state was reached. The predicted results from the thermal analysis are presented in Figure 5.24.
From Figure 5.24, it can be seen that the rotor copper-can reaches a steady-state temperature of 47 °C after 24 minutes. The stator windings reach an average steady-state temperature of 170 °C after approximately 32 minutes, with a hot spot temperature of 208 °C. The hot spot temperature is at the upper limit of the best insulating materials presently available and would result in a reduction of its service life, as discussed in Section 5.5.1.

The prototype rim-drive motor, however, is designed to operate for only a few minutes per approximate six hour tidal cycle, to take advantage of intermittent ratings. After approximately two minutes of operation, therefore, the windings reach an average temperature of 80 °C, with a hot spot temperature of 104 °C. These values are now within the safe operating temperature of the insulation material.

As expected, the windings experienced the highest predicted temperatures. As the windings are a critical part of the prototype rim-drive motor, a sensitivity investigation was performed, therefore, to determine the effect that the sea-water velocity had on the hot spot temperature of the windings. The predicted results are presented in Figure 5.25.
Figure 5.25, shows that, as expected, the temperature of the stator windings increases as the sea-water flow velocity reduces. In a worse case scenario, i.e. with zero velocity, the stator windings reach a steady-state temperature of 384 °C after approximately 80 minutes. This is clearly excessive for the insulation material. However, the results also show that after two minutes of operation, the stator windings reach a temperature of 80 °C regardless of the velocity of sea-water. Therefore, as long as the prototype rim-drive motor is not operated for more than 6.5 minutes (which relates to a peak temperature of 180 °C) with no flow of sea-water (which could result from a jam to the propeller, for example) and the ambient temperature does not exceed 30 °C, the insulation material should remain intact for the required service life of the prototype rim-drive motor in the electric thruster for the tidal stream turbine.

5.5.6 Summary

Of significant importance to the operational life of an electric machine is the temperatures at which the various components and materials operate. Understanding the effect of temperature and designing the parts to dissipate heat appropriately is essential in maximising the life of a machine.
The design of the prototype rim-drive motor pushes the electric loading to the maximum permissible level with the utilisation of the sea-water cooling that is available. It was shown, however, that for the intended operational duty cycle of approximately two minutes, every six and one quarter hours, the prototype rim-drive design was well within the safe operating temperature of the windings insulation material. It was also shown that as a worst case scenario, i.e. no flow of sea-water, the prototype rim-drive motor would still be considered safe, as long as it did not operate for more than 6.5 minutes.

5.6 Conclusions

This chapter presented the work conducted on the design, analysis and development of a prototype canned rim-drive motor for use as a bi-directional thruster on-board a tidal stream turbine. The key operational requirements and design considerations were discussed, along with the issue of marine bio-fouling. The analytical calculations that govern the key design parameters of a motor were then presented, which were subsequently used to determine the initial design of the prototype rim-drive motor. A novel induction motor topology was created using only a conducting-can on the rotor, which eliminated the need for a traditional squirrel-cage due to the relatively large diameter to small output power requirement.

The limitations of using 2-D FEA with this topology were highlighted. Modifications to the 2-D FE models were implemented using analytical expressions developed by Russell and Norsworthy, to take account of the 3-D current distribution in the conducting-can. This modification to the 2-D FE model effectively increased the resistivity of the conducting-can, which improved the accuracy of the FEA predicted results. It was shown that this 3-D effect was significant in the prototype rim-drive motor due to the aspect ratio of the design and therefore could not be ignored. A sensitivity analysis was presented showing how the thickness and material of the rotor conducting-can could be altered, to affect the starting torque and current, the peak torque and the full-load operating point of the prototype rim-drive motor design, to achieve the required operational requirements.
Finally, the issue of losses generating heat in electric machines was discussed, along with the work conducted on the thermal analysis of the prototype rim-drive motor design using the Motor-CAD thermal analysis software package. The results from these analyses were used to optimise the design for its intended application by increasing the electric loading to an acceptable but safe operating level.

The next chapter will describe the manufacture of the prototype rim-drive motor, the support structures and the test-rig. This will be followed by the results obtained from the experimental testing of the prototype motor, which are compared with the FEA predicted results to validate the FE model and design process. Finally, a discussion of the results is given, highlighting potential sources of error.
Chapter 6

Manufacture and Experimental Validation of the Prototype Rim-drive Motor

6.1 Introduction

The previous chapter described the work conducted on the design of a prototype rim-drive motor for use as a bi-directional thruster on-board a tidal stream turbine. This chapter will present the final design specifications of the prototype rim-drive motor and describe the process undertaken to manufacture this motor, along with the support structures for the rotor and stator assemblies and the test-rig. Results from the characteristic performance testing of the motor will then be presented and compared to the FEA predicted results. This will be followed by a discussion of the results, sources of potential error and finally, conclusions will be drawn.

6.2 Final Dimensions of the Prototype Rim-drive Motor Design

The work conducted and described in Chapter 5 finalised the design of the prototype canned rim-drive induction motor. The motor was manufactured to the specifications presented in Tables 6.1 to 6.6, respectively. The configuration of its stator windings is presented in Appendix D.
### Table 6.1: Prototype Rim-drive Motor Service Conditions

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Output power</td>
<td>30</td>
<td>kW</td>
</tr>
<tr>
<td>Voltage</td>
<td>400</td>
<td>Vrms</td>
</tr>
<tr>
<td>Winding connection</td>
<td>Delta</td>
<td>-</td>
</tr>
<tr>
<td>Phases</td>
<td>3</td>
<td>-</td>
</tr>
<tr>
<td>Frequency</td>
<td>50</td>
<td>Hz</td>
</tr>
<tr>
<td>Poles</td>
<td>18</td>
<td>-</td>
</tr>
<tr>
<td>Synchronous speed</td>
<td>333.33</td>
<td>rpm</td>
</tr>
<tr>
<td>Stator temperature</td>
<td>104</td>
<td>°C</td>
</tr>
<tr>
<td>Rotor temperature</td>
<td>47</td>
<td>°C</td>
</tr>
<tr>
<td>Specific electric loading</td>
<td>50,470</td>
<td>A-t.m⁻¹</td>
</tr>
<tr>
<td>specific magnetic loading</td>
<td>0.43</td>
<td>T</td>
</tr>
</tbody>
</table>

### Table 6.2: Prototype Rim-drive Motor Rotor-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotor I/D</td>
<td>740</td>
<td>mm</td>
</tr>
<tr>
<td>Rotor O/D</td>
<td>788.80</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>24.40</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M1000-65A</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>100</td>
<td>mm</td>
</tr>
<tr>
<td>Air-gap</td>
<td>3</td>
<td>mm</td>
</tr>
<tr>
<td>Can material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Can thickness</td>
<td>2</td>
<td>mm</td>
</tr>
</tbody>
</table>

### Table 6.3: Prototype Rim-drive Motor Stator-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator I/D</td>
<td>798.80</td>
<td>mm</td>
</tr>
<tr>
<td>Stator O/D</td>
<td>895.36</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>24.40</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M1000-65A</td>
<td>-</td>
</tr>
<tr>
<td>Number of slots</td>
<td>108</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>100</td>
<td>mm</td>
</tr>
<tr>
<td>Stator-slot area</td>
<td>300</td>
<td>mm²</td>
</tr>
</tbody>
</table>
Table 6.4: Prototype Rim-drive Motor Windings Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coils per slot</td>
<td>2</td>
<td>-</td>
</tr>
<tr>
<td>Coils per group</td>
<td>36</td>
<td>-</td>
</tr>
<tr>
<td>Turns per coil</td>
<td>7</td>
<td>-</td>
</tr>
<tr>
<td>Number of turns per phase (in series)</td>
<td>252</td>
<td>-</td>
</tr>
<tr>
<td>Conductor diameter</td>
<td>2.50</td>
<td>mm</td>
</tr>
<tr>
<td>Number of conductors per turn</td>
<td>2</td>
<td>-</td>
</tr>
<tr>
<td>Total cross-sectional area per turn</td>
<td>9.82</td>
<td>mm²</td>
</tr>
<tr>
<td>Conductor material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Packing factor</td>
<td>0.45</td>
<td>-</td>
</tr>
<tr>
<td>Mean turn length</td>
<td>572</td>
<td>mm</td>
</tr>
<tr>
<td>Resistance per phase</td>
<td>0.335</td>
<td>Ω</td>
</tr>
<tr>
<td>End-winding inductance per phase</td>
<td>1.454</td>
<td>mH</td>
</tr>
</tbody>
</table>

Table 6.5: Prototype Rim-drive Motor Densities and Mass

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper density</td>
<td>8,920</td>
<td>kg.m⁻³</td>
</tr>
<tr>
<td>Lamination material density</td>
<td>7,650</td>
<td>kg.m⁻³</td>
</tr>
<tr>
<td>Copper-can mass</td>
<td>4.39</td>
<td>kg</td>
</tr>
<tr>
<td>Winding mass</td>
<td>37.84</td>
<td>kg</td>
</tr>
<tr>
<td>Stator- and rotor-core mass</td>
<td>119.35</td>
<td>kg</td>
</tr>
<tr>
<td>Total motor mass</td>
<td>161.58</td>
<td>kg</td>
</tr>
<tr>
<td>Rotor inertia</td>
<td>7.26</td>
<td>kg.m⁻²</td>
</tr>
</tbody>
</table>

Table 6.6: Prototype Rim-drive Motor Stator-slot Dimensions

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Slot bottom width ($S_{BW}$)</td>
<td>14.16</td>
<td>mm</td>
</tr>
<tr>
<td>Slot top width ($S_{TW}$)</td>
<td>13.40</td>
<td>mm</td>
</tr>
<tr>
<td>Slot depth ($S_D$)</td>
<td>22.88</td>
<td>mm</td>
</tr>
<tr>
<td>Slot opening ($S_o$)</td>
<td>4</td>
<td>mm</td>
</tr>
<tr>
<td>Tooth tip depth ($T_{TD}$)</td>
<td>1</td>
<td>mm</td>
</tr>
<tr>
<td>Tooth width ($w_t$)</td>
<td>10</td>
<td>mm</td>
</tr>
<tr>
<td>Slot tooth angle ($S_a$)</td>
<td>28.95</td>
<td>°</td>
</tr>
</tbody>
</table>
An illustration of the final stator-slot dimensions for the prototype rim-drive motor design are shown in Figure 6.1, and the dimensions are presented in Table 6.6.

![Figure 6.1: Stator-slot Dimensions of the Prototype Rim-drive Motor](image1.png)

Finally, an equipotential magnetic field lines plot and flux density plot of the prototype rim-drive motor over two of its 18 magnetic poles are presented in Figures 6.2 and 6.3, respectively.

![Figure 6.2: Equipotential Field Lines Plot of the Prototype Rim-drive Motor over Two Magnetic Poles](image2.png)
6.3 Manufacture of the Prototype Rim-drive Motor

The prototype rim-drive motor was manufactured by Tesla Engineering Ltd. Tesla Engineering was selected due to its ability to assemble the complete motor design all in-house. The company was capable of producing the lamination stampings, assembling the rotor- and stator-core stacks, winding the stator and encapsulating both the rotor and stator assemblies with epoxy resin, using vacuum impregnation.

6.3.1 Manufacture of the Stator- and Rotor-core Stacks

The electric steel used for both the stator- and rotor-core stacks was supplied in 1 m² sheets. Although the cores could be manufactured from two single pieces per lamination, this would have wasted a large portion of each sheet (approximately 80%). To reduce this wastage without compromising the design of the prototype rim-drive motor too much, the cores were manufactured in segments spanning 60°, as shown in Figure 6.4.
Manufacturing the cores from the segments shown in Figure 6.4 considerably reduced the amount of wasted electric steel. Six segments each per rotor- and stator-core would complete one lamination. This would obviously introduce small air-gaps into the cores but these were deemed to be acceptable, considering the amount of electric steel that was not wasted and hence, the manufacturing costs that were saved. A picture of one complete lamination for the stator- and rotor-core is shown in Figure 6.5.

The stator- and rotor-core were then built-up to the required axial length of the motor design by off-setting each additional lamination section segment by 15°, to increase the mechanical strength of the stator- and rotor-core stacks. The resultant stacks are shown in Figure 6.6.
6.3.2 Manufacture of the Rotor Copper-can

Once the rotor-core stack was completed, the rotor copper-can was added to the outer surface of the stack in two 180° segments, as shown in Figure 6.7.

6.3.3 Manufacture of the Stator Windings

The stator is composed of double-layer windings with 36 coils per phase and seven turns per coil. There are therefore 108 coils in the completed 3-phase winding. Each turn comprises two parallel strands of 2.50 mm diameter nominal conductor wire. The
Stator-core stack was labelled to keep track of the coils during winding to ensure the correct coils were inserted into the correct slots, as illustrated in Figure 6.8.

Each coil was inserted into the appropriate stator-slot previously lined with a slot-liner to protect the insulation on the conductors, as shown in Figure 6.9. A picture of the stator with the completed wound 3-phase windings is shown in Figure 6.10.
6.3.4 Encapsulation of the Stator and Rotor Assemblies

The prototype rim-drive motor was designed to be permanently submersed in sea-water and would therefore require encapsulating to protect the stator- and rotor-core, the rotor copper-can and the stator windings. There are many types of resins in use today for the purpose of impregnating (or encapsulating) the windings of an electric machine. They can generally be categorised, however, as either thermoplastic or thermoset resins [157]. A thermoplastic resin is one that has the potential to be re-used, by either re-moulding or recycling some of the material, whereas a thermoset resin can only be used once and typically has a higher curing temperature. Impregnating resins are further sub-categorised into epoxy resins and polyester resins. Both have a number of advantages depending on the specific application [158]. For example, epoxy resins have a higher mechanical strength and offer a greater degree of moisture and abrasion resistance, whereas polyester resins are typically cheaper and easier to store.

There are also a number of processes to impregnate the windings of an electric machine with resin including: dip & bake, trickle impregnation and vacuum pressure impregnation (VPI). A description of the VPI process for form wound stator windings is presented along with key terminology and the equipment used in [159]. Processing factors that affect the VPI performance are also discussed. Fuji Electric Corporate Research and Development, Ltd. describe the development of a global VPI system for
large size (20 to 260 MVA air cooled, and 50 to 340 MVA hydrogen cooled) turbine generators, highlighting the benefits of epoxy impregnated stator windings using the VPI process [160]. The control of VPI systems to increase manufacturing yields, lower unit costs and ensure optimum life-cycle performance of the stator windings are discussed in [161, 162]. An explanation of how impregnation systems have been adapted to use the presently available resins is presented in [163].

The Epoxylite Corporation, along with the US Navy conducted a study to compare the epoxy ‘trickle’ impregnation technique with the dip & bake technique, to determine such factors as: the quality of the finished process, material and energy costs, environmental effects and the labour costs [164]. They observed that the trickle process was generally better. Second generation chemical cure resins were compared to oven bake resins for impregnating windings of motors for home appliances and portable power tools. It was identified that these second generation resins facilitated motor manufacturers in producing smaller, lighter weight and cheaper motors [165].

As the prototype rim-drive motor was designed to operate permanently submerged in sea-water, the whole stator and rotor assemblies were required to be encapsulated and not just the stator windings impregnated. A thermoset epoxy resin was therefore selected as the appropriate encapsulation material. Furthermore, a VPI impregnation system was used, as this system generally removes the most amount of air from the stator windings and hence, increases their thermal conductivity. Good thermal conductivity was especially important since the stator windings were designed to operate at a higher current density loading compared to standard air-cooled motors. The stator and rotor assemblies of the prototype rim-drive motor were wrapped in mica tape prior to epoxy resin encapsulation, as shown in Figure 6.11.

![Stator and Rotor Assemblies Wrapped in Mica Tape Prior to Encapsulation](image-url)
The completed stator and rotor assemblies after undergoing the encapsulation process are shown in Figures 6.12 and 6.13, respectively.

As can be observed in Figures 6.12 and 6.13, the stator and rotor assemblies were encapsulated with different epoxy resins. This was due to the difference in the operating temperatures of the two assemblies. The stator assembly was encapsulated in a higher thermally rated epoxy resin, compared to the rotor assembly, because of the higher operating temperature of the windings.
6.4 Manufacture of the Support Structures for the Prototype Rim-drive Motor

To allow the prototype rim-drive motor to be tested, support structures were manufactured to hold the completed stator and rotor assemblies.

6.4.1 Stator Assembly Support Structure

To support the stator assembly, a structure was manufactured from rolled angle iron, as shown in Figure 6.14.

The stator assembly was subsequently inserted and firmly held into the manufactured support structure, as shown in Figure 6.15.
6.4.2 Rotor Assembly Support Structure

To support the rotor assembly and allow it to rotate, three steel blocks were welded at 120° intervals on the inside of the rotor-core to which right-angled brackets were attached, as shown in Figure 6.16(a). The right-angled brackets were subsequently connected to a steel plate, as shown in Figure 6.16(b).

![Figure 6.16: Rotor Assembly Support Structure](image)

The plate in Figure 6.16(b) was later cut into spokes, which would be connected to a shaft aligned between pedestal bearings, allowing the fine adjustment of the air-gap and also the axial alignment of the rotor assembly inside the stator assembly.

6.4.3 Completed Support Structures for the Prototype Rim-drive Motor

The final completed support structures, with the stator and rotor assemblies of the prototype rim-drive motor inserted, are shown in Figure 6.17.

![Figure 6.17: Stator and Rotor Assemblies Inserted into the Total Completed Support Structures](image)
6.5 Prototype Rim-drive Motor Test-rig

To test the prototype rim-drive motor, a conventional Ward-Leonard system was used, as illustrated in Figure 6.18.

![Figure 6.18: Ward-Leonard Speed Control System](image)

The Ward-Leonard system is a DC motor speed control system. It is composed of a prime mover, generally an induction machine that is coupled to a DC machine (DC machine 1), which is operated at a fixed speed. The armature of DC machine 1 is connected to a second DC machine (DC machine 2) so that the EMF in both DC machines is the same. Finally, the second DC machine is coupled to the machine to be tested, i.e. the prototype motor. The field winding of DC machine 2 is fixed. Smooth speed and torque control are achieved by simple control of field winding 1. Therefore, if the prototype machine is motoring under test, DC machine 2 will act as a generator, DC machine 1 as a motor and consequently, the induction machine will also act as a generator.

The DC load motor (DC machine 2) was a Invensys/Brook Crompton DC machine with a specification of: 22 kW, and 1,500 rpm. It had a full-load torque capability, therefore, of 140 Nm. This was insufficient to test the prototype rim-drive motor at full-load, which was approximately 1,000 Nm. The capability of loading the prototype rim-drive motor to 140 Nm represented only 14% of its full-load capability; this was due to the limitations of the test equipment available in the laboratory.

The windings of the prototype rim-drive motor were connected in a ‘star’ configuration, although it had been designed for a ‘delta’ connection. This was to reduce the torque capability of the motor, to allow it to be characterised further across its speed range,
since the magnetic flux density would be diminished. Wiring the windings in star would have the further advantage of reducing the phase currents in the motor, which was beneficial because the output of the power supply in the laboratory was restricted to 63 A. Furthermore, the reduced phase currents would lower the joule losses. This was important because the motor was not operating in its designed environment of sea-water and therefore, damage to the windings could occur if overheated during testing. As a further safety precaution, the motor was tested initially at reduced phase voltages, to verify the operation of the motor and the alignment of its stator and rotor assemblies. This would also allow the load to be increased gradually.

### 6.5.1 Measuring Equipment

To measure the temperature of the windings in the prototype rim-drive motor, ‘J’ type resistive thermal devices (RTDs) were inserted into the windings at various points prior to encapsulation. These were connected to a Keithley 2701 multimeter/data acquisition system. To measure the rotational speed of the motor, a tachometer was attached to the DC load machine. The speed measurement (a frequency pulse) was also connected to the Keithley multimeter. The voltage, current, power, power factor and supply frequency for each phase of the motor were measured using a Norma D 6000 Wide Band Power Analyser System. The load torque was measured using a Hottinger Baldwin Messtechnik MVD2555 meter, along with an in-line 200 Nm torque transducer. A variable autotransformer (variac) with a maximum current rating of 45 A was used to vary the supply voltage to the motor.

### 6.5.2 Experimental Procedure

To test the prototype rim-drive motor under load, the voltage of the DC motor (DC machine 2) was increased until the prototype rim-drive motor reached synchronous speed (333.33 rpm). The phase voltages of the prototype rim-drive motor were then increased using the variac to the desired test level. The DC motor voltage was subsequently readjusted to bring the prototype rim-drive motor back up to synchronous speed. To load the prototype rim-drive motor, the voltage of the DC motor was reduced in discrete intervals, to the maximum attainable load torque whilst operating performance measurements of the prototype rim-drive motor were recorded. The
temperature of the windings were maintained to within 10 °C, to try and minimise the
effects of temperature on the test results. Once the prototype rim-drive motor reached
the upper limit of the temperature band, the load was removed and the motor was
allowed to cool down before testing recommenced.

### 6.6 Modifications to the Prototype Rim-drive FE Model

There were a number of modifications made to the FE model of the prototype rim-drive
motor, to take account of manufacturing variations. These are presented below.

#### 6.6.1 Phase Resistance

A 4-wire measurement was used to accurately determine the resistance of each phase of
the windings in the prototype rim-drive motor. The results of this measurement are
presented in Table 6.7.

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Red phase</td>
<td>0.292</td>
<td>Ω</td>
</tr>
<tr>
<td>Yellow phase</td>
<td>0.289</td>
<td>Ω</td>
</tr>
<tr>
<td>Blue phase</td>
<td>0.288</td>
<td>Ω</td>
</tr>
</tbody>
</table>

The measured results in Table 6.7 produced an average phase resistance of 0.290 Ω. The
calculated resistance per phase from Chapter 5 for the prototype rim-drive motor design
was estimated to be 0.335 Ω. However, this was at the specified peak full-load operating
temperature of the windings (104 °C). Calculating the resistance per phase using
Equations 5.41 and 5.43, for a temperature of 21 °C (which was the temperature of the
laboratory during the measurement) yielded 0.253 Ω. The measured phase resistance,
therefore, represented an approximately 15% increase over the calculated phase
resistance. This increase could mainly be accounted for in the estimation of the length
of the end-windings, as the calculation assumed perfect semi-circular end-turns.
During testing of the motor, the windings were allowed to reach a maximum operating temperature of 35 °C. The FE model was therefore modified by changing the resistance of each phase of the windings to the average of the three values presented in Table 6.7, and then corrected for temperature. This yielded a per phase resistance of 0.301 Ω.

### 6.6.2 Electric Steel

The FE model of the prototype rim-drive motor used a generic M1000-65A electric steel from the Flux2D materials database. Tesla Engineering, however, manufactured the rotor- and stator-core from M1200-100A non-grain orientated electric steel produced by ThyssenKrupp Stahl. The B-H data was therefore obtained for this steel and implemented in the FE model.

### 6.6.3 Leakage Inductance of the End-windings

The leakage inductance of the end-windings for the prototype rim-drive motor design was calculated to be 1.454 mH per phase, using Equation 5.33. Due to the increase in the phase resistance identified in Section 6.6.1, the leakage inductance was also increased by 15%. It was found after testing the motor, however, that the leakage inductance needed to be increased by an additional 100% to produce consistent agreement between the FEA predicted results and the measured performance results from the motor. This may be, in part, due to the aspect ratio of the motor, as the end-windings are a more dominant component of the total length of the windings compared to typical standard induction motors. These results clearly show the difficulties associated with analytical methods for determining the leakage inductance of the end-winding, as previously discussed in the Chapter 5.

### 6.7 Experimental Test Results for the Prototype Rim-drive Motor

The figures which follow present the experimental test results for the prototype rim-drive motor compared to the FEA predicted results. As the motor was loaded during
testing, the supply voltage reduced as the load increased. This was due to the poor regulation of the laboratory power supply. The presented results, therefore, also show the FEA predicted results corrected for the voltage droop.

### 6.7.1 Supply Voltage = 25 V

The results for torque, phase current, power factor and efficiency as a function of slip for a supply voltage of 25 V are presented in Figures 6.19 to 6.22, respectively.

![Figure 6.19: Torque vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 25 V](image)
Figure 6.20: Phase Current vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 25 V

Figure 6.21: Power Factor vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 25 V
Figure 6.19 shows that at a supply voltage of 25 V, the experimental results for the prototype rim-drive motor correlate with the FEA predicted torque closely up to a slip of approximately 0.25, although the FEA predicts slightly less torque for a given slip value, which could be caused by too much reactance or rotor resistance in the FE model. For slips greater than 0.25, the correlation is not as good, with a maximum error of approximately 12%. Now the FEA results overestimate the torque, which suggests that there is not enough reactance in the FE model.

Figure 6.20 shows that the phase current for both the experimental and the FEA predicted results are closely correlated. Figure 6.21 shows that the power factor results have good correlation but that the biggest deviation occurs at small values of slip. This result further suggest that there is too much reactance in the FE model. Figure 6.22 shows that the efficiency results start with an error of approximately 20% close to zero slip but as the slip is increased the error reduces. The large error at low values of slip suggest that the resistance in the FE model is incorrect.
6.7.2 Supply Voltage = 125 V

The prototype rim-drive motor was then retested but at the higher supply voltage of 125 V. The results for torque, phase current, power factor and efficiency as a function of slip are presented in Figures 6.23 to 6.26, respectively.

Figure 6.23: Torque vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 125 V

Figure 6.24: Phase Current vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 125 V
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Figure 6.25: Power Factor vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 125 V

Figure 6.26: Efficiency vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 125 V
Figure 6.23 shows that at 125 V, the measured torque of the prototype rim-drive motor is marginally higher than the FEA predicted torque up to 0.2 slip, at which point it is in very close agreement, further suggesting that there is too much reactance or rotor resistance in the FE model.

Figure 6.24 shows that the phase current from the experimental results is slightly below the FEA predicted results up to approximately a slip of 0.13, after which they are closely correlated. Figure 6.25 shows that the power factor results are generally in good agreement but also suggest that there is too much reactance in the FE model. Figure 6.26 shows that the efficiency at low values of slip is the largest, with the error reducing as the slip is increased. However, this time the results suggest that the resistance is too low in the FE model at low values of slip.

6.7.3 Supply Voltage = 240 V

The prototype rim-drive motor was finally tested at the full supply voltage of 240 V. The results for torque, phase current, power factor and efficiency as a function of slip are presented in Figures 6.27 to 6.30, respectively.

Figure 6.27: Torque vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 240 V
Experimental Results
- FEA Predicted Results
- FEA Predicted Results Corrected
- Poly. (Experimental Results)

**Figure 6.28:** Phase Current vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 240 V

**Figure 6.29:** Power Factor vs. Slip for the Prototype Rim-drive Motor Experimental Test Results Compared to the FEA Predicted and Corrected Results at 240 V
Figure 6.27 shows that at 240 V, the FEA predicted torque underestimates the torque that was measured from the prototype rim-drive motor. As the load is increased, the measured results increase at a greater rate than the predicted results. The motor was not able to be tested past a slip of 0.04, so it can only be assumed that the same effect as observed in Figure 6.19, where the measured torque results drop below the predicted results after a slip of 0.2, would also occur at this tested voltage level. Figure 6.28 shows that the FEA predicted results for phase current overestimates the current by approximately 10% compared to the measured phase current, which again suggests that the reactance in the FE model is incorrect. The error, however, is consistent across the slip range tested.

Figure 6.29 shows that the power factor results have the same effect as in the torque results shown in Figure 6.27, i.e. the measured results of power factor increase at a greater rate than the predicted results. Again, the same effect as in Figure 6.21 (and just mentioned with regards to the torque results) may occur if the prototype rim-drive motor was able to be tested past a slip of 0.04 at the tested voltage level. Finally, Figure 6.30 shows that the efficiency results show a consistent error, with the predicted results underestimating the measured results by approximately 20%. This result further suggests that the resistance in the FE model is incorrect.
Although there will be experimental measurement errors, the results suggest that there is too much reactance in the FE model at low values of slip. As has been noted previously, the inductance of the end-windings is difficult to accurately predict. Furthermore, the model of the copper-can in the FE model does not take account of the end inductances, as they cannot be established in the circuit of the FE model.

These factors could account for the errors in the reactance between the FE model and the prototype rim-drive motor causing the deviation in the results. The efficiency results tend to suggest that the resistance is also incorrect. Since the phase resistance was accurately measured, this further suggests that the FE model of the copper-can is the main cause of the deviation between the results.

### 6.7.4 Transient Starting Phase Current Test

A transient starting test was conducted on the prototype rim-drive motor to determine the in-rush starting current. The results from this test are presented in Figure 6.31.

![Phase Current from a Transient Starting Test of the Prototype Rim-drive Motor at 240 V](image)

**Figure 6.31:** Phase Current from a Transient Starting Test of the Prototype Rim-drive Motor at 240 V

Figure 6.31 shows that the peak phase current of the prototype rim-drive motor reaches a maximum value of about 270 A at starting. This is approximately three times the
magnetising current of the motor. A direct comparison of the starting current to full-load current of the motor was not possible due to equipment limitations, as previously discussed. This result, however, suggests that the design of the motor has achieved the relatively low in-rush current requirement, which was one of the operating specifications.

6.7.5 Magnetic Saturation of the Electric Steel

To confirm that the major magnetic flux paths in the prototype rim-drive motor had not saturated during testing, the average of the magnetising currents for all three phases were plotted against their corresponding supply voltages. This result is presented in Figure 6.32.

![Figure 6.32: Average 3-phase Phase Magnetising Currents vs. Supply Voltage for all Voltages Tested](chart)

Figure 6.32 shows that significant saturation of the major magnetic flux paths has not occurred during testing. This is to be expected, however, as the prototype motor windings were connected in a star configuration and hence, the prototype motor was operating at reduced magnetic flux.
6.8 Discussion

The results presented in Section 6.7 show the operating performance of the prototype rim-drive motor at several supply voltages. There are clearly some consistent errors between the FEA predicted results and the measured performance results, obtained from the experimental tests conducted on the prototype motor. Sources of potential error between the prototype motor and FE model were eliminated as much as practically possible, which included:

- modifying the resistance of each phase winding in the FE model to match the measured result;
- adjusting the leakage inductance of the end-windings in the FE model until good agreement between the FEA predicted results and the measured test results was achieved;
- controlling the temperature of the motor during testing within a 10 °C narrow band; and
- obtaining accurate B-H data for the electric steel used in the rotor- and stator-core.

There are still a number of factors that could not be controlled, however, that may account for the errors observed between the measured and predicted results, including:

- the cores of both the rotor and stator were manufactured from six segments per lamination. This would increase the reluctance of the magnetic circuit, due to the additional air-gaps and therefore, increase the magnetising current;
- the FE model is limited, as it is only 2-D. This leads to a number of potential errors because 3-D effects are not accounted for in the 2-D FE model, especially the end effects of the motor, i.e. the inductance of the end-windings, which are more dominant in a rim-drive motor due to the aspect ratio;
- a further limitation of the 2-D FE model was in relation to the rotor copper-can. An impedance was not able to be added to the FE model to take account of the end effects in the copper-can. A pseudo 3-D FE model was created; however, this was a simplified approach and only tried to compensate for resistance.
Therefore, the correct reactance of the copper-can was not taken into account in
the 2-D FE model;

- the practical measurements themselves and test equipment used;
- manufacturing tolerances constructing the prototype motor; and
- stray load losses and material inconsistencies.

Considering these factors, therefore, the measured performance results from the
prototype rim-drive motor were considered to be in reasonable agreement with the FEA
predicted results because of the relatively small differences between them.

### 6.9 Conclusions

This chapter began with a presentation of the final design specifications of the prototype
rim-drive motor developed in Chapter 5. This was followed by a description of the
process undertaken to manufacture the prototype rim-drive motor and the support
structures that were also constructed to allow the motor to be tested in the laboratory.
The modifications to the FE model to correct it for phase resistance, end-windings
inductance and electric steel differences were then explained.

This was followed with a presentation of the experimental test results from the
prototype motor, which were compared with their equivalent FEA predicted results. The
experimental results were shown to be generally in good agreement with the FEA
predicted results. Finally, reasons for potential errors in the FE model were also
highlighted. The next chapter will discuss the conclusions to this thesis and present
recommendations for work that could be conducted to develop the rim-driven electric
machine concept further. Finally, patents and publications that were derived from the
work presented in this thesis are listed.
Chapter 7

Conclusions and Recommendations for Further Work

7.1 Conclusions

7.1.1 Introduction

This chapter draws conclusions from the work described and discussed in the preceding chapters of this thesis. Firstly, the background to this research project is given, which is followed by a brief summary of each chapter. The contribution of the research project is then discussed, including the benefits and limitations of the developed rim-driven electric machine topology. This is followed by the areas of work that have been identified to further develop the rim-driven topology. Finally, a list of the patents and publications created from this research project are given.

7.1.2 Background

Increasing environmental and safety concerns, and generally rising energy costs are requiring engineers to improve system efficiencies in a number of major industrial applications including the aerospace, marine and automotive sectors. One way to potentially improve overall system efficiencies and reduce operating costs is to replace
traditional mechanical systems with electric machines due to their operational flexibility.

Implementing direct-drive electric machines in applications where high speed is not required offers several advantages including the removal of the reduction gearbox, reducing through-life costs, weight, noise and overall system volume and therefore, space. Although direct-drive machines are presently being used in a number of industrial applications, there is the possibility for the development of novel machines for use in other industrial application areas such as emergency marine propulsion systems, for example.

One such novel direct-drive electric machine is the ‘rim-driven’ topology presented in this thesis. Although the consideration of driving propeller tips directly is not a new concept, the implementation of this rim-driven concept is being considered for a number of novel industrial applications, since rim-driven machines are naturally large in diameter; a topology that favours large torque production in electric machines.

7.1.3 Review of Presented Work

Chapter 1 briefly mentioned the introduction of DC and AC electric machines, and highlighted how these have facilitated industrial development, and continue to find novel industrial applications where they can be implemented. The main reasons for this fact are increasing environmental concerns with burning fossil fuels and their generally rising procurement costs. Electric machines generally offer greater operational flexibility and overall system efficiency improvements compared to mechanical equivalent systems. One such area is in direct-drive applications where high speed is not required.

This chapter also presented the concept of integrating a propeller directly with the electric machine that is driving it, creating a modern variant on a traditional mechanical system. The advantage of this rim-driven topology with regards to the electric machine, however, was that the natural large diameter of the machine meant that it produced relatively large torque. Furthermore, the intended operating environment, i.e. typically water, also enabled the machine to be designed to be torque-dense; two favourable
factors in electric machines. Finally, Chapter 1 identified several suitable and potential application areas for the rim-driven topology.

Chapter 2 highlighted the present technology in the identified application areas including marine propulsion systems and seal-less pumps. It was shown that the rim-driven technology could potentially be developed as a suitable addition to the presently available technology in these areas. Furthermore, it was highlighted that it could potentially be implemented in a ‘run-of-the-river’ project as an electric generator.

In Chapter 3, basic background information on the materials used in the manufacture of electric machines and the reasons for their choice was given. This was followed with an introduction to the finite element analysis (FEA) numerical technique and the software package subsequently used in the development of the electric motors presented in this thesis.

Three concept studies utilising the rim-driven topology were presented in Chapter 4. The first concept study was a canned line-start permanent-magnet motor for use as a marine vessel thruster, i.e. a secondary propulsion system. It was shown how the present motor developed by Rolls-Royce could be modified to eliminate the need for a power electronic converter and furthermore, how the design could be modified to meet the requirements of the application. The second concept study was a seal-less rim-drive motor pump. It was shown how the rim-driven topology could eliminate the requirement for a dynamic seal, increasing the safety and reliability of the motor pump, especially in safety critical environments such as the nuclear industry. Finally, the third concept study presented the development of a drop-down azimuth thruster rim-drive propulsion motor for use as an emergency propulsion system on-board a submarine.

The analysis conducted on these three concept studies showed that the power factor and efficiency of the designed motors were reduced in comparison to an equivalent industrial motor that was used as a benchmark. This was expected due to the increased air-gap thickness required to accommodate several cans for torque generation and environmental protection and therefore, the reduced power factor and efficiency were not considered as important to the advantages that the rim-drive motors offered in their intended industrial applications.
Chapter 5 presented the development of a prototype rim-drive motor for use as a bi-directional thruster on-board a tidal stream turbine that Rolls-Royce has produced. A detailed design was given and it was shown that 2-D FEA could not be used directly to accurately predict the performance of the prototype rim-drive motor design, which was mainly caused by the limitation of the FE model in modelling the conducting-can on the rotor. A pseudo 3-D model, however, was created and shown to increase the accuracy of the FEA predicted results significantly.

A sensitivity analysis was also performed to identify the optimum conducting-can material and thickness to achieve the operational requirements. Finally, a thermal analysis sensitivity investigation was conducted on the prototype rim-drive motor design, to determine the electric loading the motor could safely tolerate, so as to take advantage of the low duty cycle operational requirement and permanent submersion in a sea-water environment. This resulted in a relatively high electric loading, but it was shown that this was acceptable in a worst case situation, as long as the motor did not operate for more that approximately six minutes.

Finally, Chapter 6 presented the specifications of the finalised prototype rim-drive motor design and discussed the manufacture of the prototype motor, along with support structures to hold the rotor and stator assemblies and a test-rig. The test results from experiments conducted on the prototype motor were then presented. Full characteristic testing of the prototype motor was not possible, however, due to equipment limitations and the fact that the motor was not operating in its intended environment of sea-water. However, the experimental results showed reasonable agreement with the FEA predicted results. Finally, potential sources of error were highlighted.

### 7.1.4 Contribution

The objectives and aims of this research project, which were initially presented in Chapter 1, were as follows:

1. To investigate the design and implementation of electric machines integrated directly into the structure of thrusters/pumps, specifically the housing around propellers themselves, creating canned rim-driven electric machines.
2. The topologies that were to be investigated using the canned rim-driven concept were permanent-magnet and induction machines.

3. To develop an understanding of the requirements for the design and successful implementation of the canned rim-driven topology.

4. To manufacture and test a prototype canned rim-drive motor, to validate the design process.

5. To consider the implementation of the canned rim-driven topology in several potential application areas including:

   a. an emergency back-up marine propulsion system, which can be used in the event of a primary propulsion system failure;
   b. the low-speed manoeuvring (docking) of marine vessels;
   c. a seal-less pump for the fluid process industry;
   d. a bi-directional thruster in a tidal stream turbine generator; and
   e. a run-of-the-river generator.

All of these objectives and aims have been achieved and described within this thesis. Furthermore, the investigations into the application and design of the canned rim-driven electric machine concept led to the development, manufacture and successful performance testing of a novel prototype canned rim-drive induction motor, with a nominal rating of 30 kW (although the motor was capable of producing more torque than was required by the industrial application due to mechanical constraints).

The prototype canned rim-drive motor utilised a conducting-can on the rotor to produce torque, which eliminated the requirement for a traditional squirrel-cage due to the relatively small power requirement of the motor given its physical dimensions. This simplified the rotor construction and hence, increased the reliability of the canned rim-drive motor.

The author feels that the work conducted during this research project and subsequently presented in this thesis, give good reasons for the further development of the canned rim-driven electric machine concept. This is not only true for the applications presented in this thesis but also in other direct-drive applications that have a low duty cycle operational requirement at the expense of high machine efficiency or where system costs are to be minimised, or also where reliability is a critical factor.
7.1.5 Benefits and Limitations

The canned line-start rim-drive motor topology developed and analysed within this thesis has highlighted a number of benefits and also limitations of the technology, which are summarised below. The key benefits are:

- the ability to remove the need for a power electronic converter, reducing system costs and volume, and increase system reliability;
- the removal of complicated gearbox and drive shaft arrangements, reducing system volume, noise, vibration and maintenance;
- the simplification of the rotor design, further reducing costs, volume and weight;
- the ability to increase the electric loading of the motor due to the operating environment, also reducing costs, weight and volume;
- a relatively high torque-density given the volume of the motor; and
- the possibility for increased reliability, especially in the case of the prototype rim-drive motor with only the canned rotor construction.

The key limitations of the rim-drive motor topology, however, are:

- a lower power factor compared to a standard industrial induction motor;
- reduced efficiency due to the increased magnetising current and electric loading; and
- is only beneficial in low duty cycle applications or where reliability is of significant importance compared to efficiency.

7.2 Areas Where Further Work has been Identified

7.2.1 Introduction

Some of the further work identified was either beyond the scope of this research project or was not practically viable due to time constraints or commercial pressures. This section therefore presents further work that could be conducted to develop the canned line-start rim-driven electric machine topology presented in this thesis.
7.2.2 Development of the Canned LSPM Rim-drive Motor

Chapter 4 presented the initial work conducted on the analysis of a PM synchronous rim-drive motor, which was developed for the commercial marine market. An analysis was subsequently conducted to determine if this design could be modified to create a canned LSPM motor with minimal changes to the design. Introducing a conducting-can into the air-gap was shown to be successful but due to commercial pressures and computational limitations, a full detailed design was not possible. Therefore, this is an area for further investigation.

The benefits of achieving the successful design and implementation of a canned LSPM rim-drive motor are however notable. Firstly, the operating efficiency and power factor would be greatly improved compared to a canned rim-drive induction motor. Secondly, the manufacture of the rotor would be simplified by implementing the canned rotor concept rather than a squirrel-cage LSPM motor topology. Thirdly, the rotor-can structure could serve two functions by providing induction torque, as well as environmental containment; therefore, facilitating the use of a canned LSPM motor in fluid environments. Finally, in industrial applications where volume utilisation is critical or cost is a priority, the canned LSPM rim-drive motor could be a viable alternative solution to the present technology.

7.2.3 3-D FE Modelling and Analysis

Chapter 5 presented the work conducted to modify the conductivity of the rotor conducting-can to account for 3-D effects. This is however a simplified solution. A more detailed and potentially more accurate approach to the design and analysis of the canned rim-drive motor topology presented in this thesis would be to use 3-D FEA techniques. 3-D FEA would be able to calculate the total eddy current loss in the conducting-can directly, as well as the reactance. Furthermore, the end-winding turns of the stator could be modelled directly rather than providing the 2-D FE model with a calculated value for the end-windings inductance (which is notoriously difficult to calculate and has been shown to be inaccurate in the case of the prototype rim-drive motor), again potentially improving the accuracy of the FEA predicted performance.
results. This is, therefore, an area of work where the rim-drive motor concept could be further developed.

### 7.2.4 Further Testing of the Prototype Rim-drive Motor

One of the main objectives of this research project was to investigate the use of the rim-drive motor topology for exploitation in practical industrial applications. The best evaluation of success, however, is the successful implementation of the rim-drive motor topology. A prototype rim-drive motor was therefore manufactured, which was successfully tested. To exploit the rim-drive motor topology further and gain a greater understanding of its performance, therefore, this motor needs to be realised in the application and environment that it was designed to operate in, i.e. as a bi-directional thruster in a tidal stream turbine.

As discussed in Chapter 6, the prototype rim-drive motor was successfully tested in the laboratory. Power limitations and appropriate test equipment, however, limited the testing that could be conducted. Furthermore, since the prototype rim-drive motor was designed to operate in an environment permanently submersed in sea-water, which was again not available in the laboratory, full characteristic profiling of the operation of this motor has not been achieved.

To achieve this goal, however, a number of issues would have to be initially overcome. Firstly, an appropriate propeller would have to be selected and securely mounted inside the rotor assembly. Secondly, an appropriate support structure for the stator assembly, as well as the rotor assembly (with the propeller mounted) allowing it to rotate freely but still maintaining a constant air-gap, which is also suitable for submersion in a sea-water environment would have to be constructed. Thirdly, electric power supply cables would have to be provided, with connections that are also suitable for permanent submersion in a sea-water environment, to supply power to the prototype rim-drive motor, along with test and data logging equipment used to capture performance test results.

At the time of writing, the author is in discussions with Rolls-Royce to take the prototype rim-drive motor from the university laboratory environment and to put it into
Chapter 7

Conclusions and Recommendations for Further Work

operation in the real-life environment it was designed to operate in, initially for full evaluation testing. This is, therefore, an area of further work to be undertaken.

7.2.5 Development of the Canned Rim-drive Motor Topology

A further development of the canned rim-drive motor topology presented in this thesis could be to implement a multiple-layer conducting-can arrangement, where each conducting-can is composed of a different material. An aluminium-can on top of a copper-can, for example, could be implemented. This would offer the advantage of a varying resistance with rotor speed. The analysis of this canned rim-drive motor topology, therefore, is a further area of work that could be conducted.

7.2.6 Development of a Run-of-the-river Rim-driven Generator

Chapter 2 identified and briefly discussed the potential for the rim-driven electric machine concept to be implemented to create a run-of-the-river generator for remote locations, where finances are limited or connection to the national grid is prohibitively expensive. These remote locations typically require only a relatively small amount of power. A partially active stator could therefore be constructed, which would reduce the capital costs of the overall run-of-the-river generator system. No further work was conducted on the design of this topology within this thesis due to commercial pressures, although a patent was filed for the concept. This is therefore an area of further work that could be explored, to utilise the rim-driven electric machine concept.

7.3 Patents and Publications

Two patents have been filed as a result of the work and concepts developed within this thesis. They are:

1. Multiple Can Rim-driven Electrical Machine
2. Canned Induction Machine with an Arc Stator
In addition to these patents, the following publication has also resulted from the work presented in this thesis:

References


References


[81] "Bow Thruster," [online]. Available at: http://images.google.co.uk/imgres?imgurl=http://www.torkmaster.com/uploaded_images/bow%2520thruster%2520jasmin.jpg&imgrefurl=http://www.torkmaster.com/category.asp%3Fcat_id%3D2%26cat%3D2%26sy%3D5&usg=__CfeLi2bhcQExq5PvVjysRBAE=&h=263&w=350&sz=28&hl=en&start=1&um=1&tbnid=uLJKeTICDeXMLM:&tbnh=90&tbnw=120&prev=/images%3Fq%3Dbow%2Bthrusters%26um%3D1%26hl%3Den%26lr%3D, [accessed: 05/02/2009].


References


Appendix A

800 kW PM Benchmark Motor Specifications
### Table A.1: 800 kW PM Benchmark Motor Service Conditions

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
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<tbody>
<tr>
<td>Output power</td>
<td>800</td>
<td>kW</td>
</tr>
<tr>
<td>Voltage</td>
<td>616.3</td>
<td>V_{rms}</td>
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<tr>
<td>Phases</td>
<td>3</td>
<td>-</td>
</tr>
<tr>
<td>Frequency</td>
<td>158.4</td>
<td>Hz</td>
</tr>
<tr>
<td>Poles</td>
<td>66</td>
<td>-</td>
</tr>
<tr>
<td>Synchronous speed</td>
<td>288</td>
<td>rpm</td>
</tr>
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</table>

### Table A.2: 800 kW PM Benchmark Motor Rotor-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotor I/D</td>
<td>1,692</td>
<td>mm</td>
</tr>
<tr>
<td>Rotor O/D</td>
<td>1,732</td>
<td>mm</td>
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<tr>
<td>Back-iron thickness</td>
<td>20</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M270-35A</td>
<td>-</td>
</tr>
<tr>
<td>Axial length</td>
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<td>mm</td>
</tr>
<tr>
<td>Air-gap</td>
<td>10</td>
<td>mm</td>
</tr>
<tr>
<td>Magnet length (radial)</td>
<td>20</td>
<td>mm</td>
</tr>
<tr>
<td>Magnet pitch</td>
<td>74</td>
<td>mm</td>
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</table>

### Table A.3: 800 kW PM Benchmark Motor Stator-core Data

<table>
<thead>
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<th>Value</th>
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</thead>
<tbody>
<tr>
<td>Stator I/D</td>
<td>1,792</td>
<td>mm</td>
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<td>Stator O/D</td>
<td>1,967.6</td>
<td>mm</td>
</tr>
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<td>Back-iron thickness</td>
<td>30</td>
<td>mm</td>
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<tr>
<td>Lamination material</td>
<td>M270-35A</td>
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</tr>
<tr>
<td>Number of slots</td>
<td>72</td>
<td>-</td>
</tr>
<tr>
<td>Axial length</td>
<td>380</td>
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</tr>
<tr>
<td>Pole pitch</td>
<td>84.35</td>
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<tr>
<td>Slot pitch</td>
<td>78.19</td>
<td>mm</td>
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<tr>
<td>Slot width</td>
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<td>Slot depth</td>
<td>57.8</td>
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Table A.4: 800 kW PM Benchmark Motor Windings Data

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<thead>
<tr>
<th>Description</th>
<th>Value</th>
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<tbody>
<tr>
<td>Coils per slot</td>
<td>1</td>
<td>-</td>
</tr>
<tr>
<td>Coils per group</td>
<td>2</td>
<td>-</td>
</tr>
<tr>
<td>Turns per coil</td>
<td>15</td>
<td>-</td>
</tr>
<tr>
<td>Number of parallel paths</td>
<td>6</td>
<td>-</td>
</tr>
<tr>
<td>Number of turns per phase (in series)</td>
<td>30</td>
<td>-</td>
</tr>
<tr>
<td>Conductor diameter</td>
<td>0.4</td>
<td>mm</td>
</tr>
<tr>
<td>Number of conductors per turn</td>
<td>512</td>
<td>-</td>
</tr>
<tr>
<td>Total cross-sectional area per turn</td>
<td>64.4</td>
<td>mm²</td>
</tr>
<tr>
<td>Conductor material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Packing factor</td>
<td>0.78</td>
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<tr>
<td>Resistance per phase</td>
<td>3.31</td>
<td>mΩ</td>
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Table A.5: 800 kW PM Benchmark Motor Full-load Performance Data

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</tr>
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<tbody>
<tr>
<td>Full-load current (line-to-line)</td>
<td>799.7</td>
<td>A</td>
</tr>
<tr>
<td>Output torque</td>
<td>26,526</td>
<td>Nm</td>
</tr>
<tr>
<td>Input power</td>
<td>819,672</td>
<td>W</td>
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<tr>
<td>Stator joule losses (per phase)</td>
<td>6,349</td>
<td>W</td>
</tr>
<tr>
<td>Stator iron losses</td>
<td>1,400</td>
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<td>Rotor iron losses</td>
<td>5,575</td>
<td>W</td>
</tr>
<tr>
<td>PM losses</td>
<td>6,705</td>
<td>W</td>
</tr>
<tr>
<td>Efficiency (no windage or friction)</td>
<td>97.6</td>
<td>%</td>
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<tr>
<td>Power factor</td>
<td>0.94</td>
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Table A.6: 800 kW PM Benchmark Motor Densities and Mass

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
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<tbody>
<tr>
<td>Copper density</td>
<td>8,920</td>
<td>kg.m⁻³</td>
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<tr>
<td>Lamination material density</td>
<td>7,650</td>
<td>kg.m⁻³</td>
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<tr>
<td>PM density</td>
<td>7,500</td>
<td>kg.m⁻³</td>
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<tr>
<td>Winding mass</td>
<td>511</td>
<td>kg</td>
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<tr>
<td>Copper-can mass</td>
<td>59</td>
<td>kg</td>
</tr>
<tr>
<td>Stator- and rotor-core mass</td>
<td>1,366</td>
<td>kg</td>
</tr>
<tr>
<td>PM mass</td>
<td>279</td>
<td>kg</td>
</tr>
<tr>
<td>Total motor mass</td>
<td>2,215</td>
<td>kg</td>
</tr>
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Appendix B

Seal-less Rim-drive Motor Pump Specifications
## Appendix B

### Seal-less Rim-drive Motor Pump Specifications

#### Table B.1: Seal-less Rim-drive Motor Pump Service Conditions

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
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<tr>
<td>Output power</td>
<td>200</td>
<td>kW</td>
</tr>
<tr>
<td>Voltage</td>
<td>440</td>
<td>V&lt;sub&gt;rms&lt;/sub&gt;</td>
</tr>
<tr>
<td>Winding connection</td>
<td>Delta</td>
<td>-</td>
</tr>
<tr>
<td>Phases</td>
<td>3</td>
<td>-</td>
</tr>
<tr>
<td>Frequency</td>
<td>60</td>
<td>Hz</td>
</tr>
<tr>
<td>Poles</td>
<td>4</td>
<td>-</td>
</tr>
<tr>
<td>Synchronous speed</td>
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<td>rpm</td>
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<tr>
<td>Stator temperature</td>
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<td>Rotor temperature</td>
<td>300</td>
<td>°C</td>
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#### Table B.2: Seal-less Rim-drive Motor Pump Rotor-core Data

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<tr>
<td>Rotor I/D</td>
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<td>Rotor O/D</td>
<td>373.4</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>40</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M400-50A</td>
<td>-</td>
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<tr>
<td>Number of bars</td>
<td>58</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>436.5</td>
<td>mm</td>
</tr>
<tr>
<td>Air-gap</td>
<td>1.25</td>
<td>mm</td>
</tr>
<tr>
<td>Bar material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Rotor bar area</td>
<td>182</td>
<td>mm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
</tbody>
</table>

#### Table B.3: Seal-less Rim-drive Motor Pump Stator-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator I/D</td>
<td>382.92</td>
<td>mm</td>
</tr>
<tr>
<td>Stator O/D</td>
<td>536.9</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>40</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M400-50A</td>
<td>-</td>
</tr>
<tr>
<td>Number of slots</td>
<td>72</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>436.5</td>
<td>mm</td>
</tr>
<tr>
<td>Stator-slot area</td>
<td>333</td>
<td>mm&lt;sup&gt;2&lt;/sup&gt;</td>
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</table>
## Table B.4: Seal-less Rim-drive Motor Pump Windings Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coils per slot</td>
<td>2</td>
<td>-</td>
</tr>
<tr>
<td>Coils per group</td>
<td>6</td>
<td>-</td>
</tr>
<tr>
<td>Turns per coil</td>
<td>5</td>
<td>-</td>
</tr>
<tr>
<td>Number of parallel paths</td>
<td>4</td>
<td>-</td>
</tr>
<tr>
<td>Number of turns per phase (in series)</td>
<td>30</td>
<td>-</td>
</tr>
<tr>
<td>Conductor diameter (1)</td>
<td>1.4</td>
<td>mm</td>
</tr>
<tr>
<td>Number of conductors per turn (1)</td>
<td>4</td>
<td>-</td>
</tr>
<tr>
<td>Conductor diameter (2)</td>
<td>1.32</td>
<td>mm</td>
</tr>
<tr>
<td>Number of conductors per turn (2)</td>
<td>7</td>
<td>-</td>
</tr>
<tr>
<td>Total cross-sectional area per turn</td>
<td>15.74</td>
<td>mm²</td>
</tr>
<tr>
<td>Conductor material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Mean turn length</td>
<td>1,680</td>
<td>mm</td>
</tr>
<tr>
<td>Resistance per phase</td>
<td>0.01952</td>
<td>Ω</td>
</tr>
<tr>
<td>End-winding inductance per phase</td>
<td>0.258</td>
<td>mH</td>
</tr>
</tbody>
</table>

## Table B.5: Seal-less Rim-drive Motor Pump Full-load Performance Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Magnetising current (phase)</td>
<td>187.35</td>
<td>A</td>
</tr>
<tr>
<td>Full-load current (phase)</td>
<td>252.86</td>
<td>A</td>
</tr>
<tr>
<td>Slip</td>
<td>0.00976</td>
<td>-</td>
</tr>
<tr>
<td>Output speed</td>
<td>1,782</td>
<td>rpm</td>
</tr>
<tr>
<td>Output torque</td>
<td>1,071</td>
<td>Nm</td>
</tr>
<tr>
<td>Input power</td>
<td>208</td>
<td>kW</td>
</tr>
<tr>
<td>Power factor</td>
<td>0.62</td>
<td>-</td>
</tr>
<tr>
<td>Total stator joule losses</td>
<td>3,744</td>
<td>W</td>
</tr>
<tr>
<td>Stator iron losses</td>
<td>1,429</td>
<td>W</td>
</tr>
<tr>
<td>Rotor cage joule losses</td>
<td>1,972</td>
<td>W</td>
</tr>
<tr>
<td>Rotor iron losses</td>
<td>748</td>
<td>W</td>
</tr>
<tr>
<td>Rotor copper-can losses</td>
<td>114</td>
<td>W</td>
</tr>
<tr>
<td>Rotor Inconel-can losses</td>
<td>3.17</td>
<td>W</td>
</tr>
<tr>
<td>Efficiency (no windage or friction)</td>
<td>96.18</td>
<td>%</td>
</tr>
</tbody>
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## Table B.6: Seal-less Rim-drive Motor Pump Densities and Mass

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper density</td>
<td>8,920</td>
<td>kg.m⁻³</td>
</tr>
<tr>
<td>Lamination material density</td>
<td>7,650</td>
<td>kg.m⁻³</td>
</tr>
<tr>
<td>Inconel density</td>
<td>8,440</td>
<td>kg.m⁻³</td>
</tr>
<tr>
<td>Copper-can mass</td>
<td>2.29</td>
<td>kg</td>
</tr>
<tr>
<td>Squirrel-cage mass</td>
<td>47.65</td>
<td>kg</td>
</tr>
<tr>
<td>Winding mass</td>
<td>93.35</td>
<td>kg</td>
</tr>
<tr>
<td>Stator- and rotor-core mass</td>
<td>459.81</td>
<td>kg</td>
</tr>
<tr>
<td>Rotor Inconel-can mass</td>
<td>2.17</td>
<td>kg</td>
</tr>
<tr>
<td>Total motor mass</td>
<td>605.27</td>
<td>kg</td>
</tr>
</tbody>
</table>
Figure B.1: Air-gap Magnetic Field Produced by the Seal-less Rim-drive Motor Pump Design at No-load and Full-load

Figure B.2: Harmonic Analysis of the Air-gap Magnetic Field Produced by the Seal-less Rim-drive Motor Pump Design at No-load and Full-load
Appendix C

DDAT Rim-drive Propulsion Motor Specifications
### Table C.1: DDAT Rim-drive Propulsion Motor Service Conditions

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Output power</td>
<td>183</td>
<td>kW</td>
</tr>
<tr>
<td>Voltage</td>
<td>440</td>
<td>V&lt;sub&gt;rms&lt;/sub&gt;</td>
</tr>
<tr>
<td>Winding connection</td>
<td>Delta</td>
<td>-</td>
</tr>
<tr>
<td>Phases</td>
<td>3</td>
<td>-</td>
</tr>
<tr>
<td>Frequency</td>
<td>60</td>
<td>Hz</td>
</tr>
<tr>
<td>Poles</td>
<td>12</td>
<td>-</td>
</tr>
<tr>
<td>Synchronous speed</td>
<td>600</td>
<td>rpm</td>
</tr>
<tr>
<td>Stator temperature</td>
<td>135</td>
<td>°C</td>
</tr>
<tr>
<td>Rotor temperature</td>
<td>80</td>
<td>°C</td>
</tr>
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</table>

### Table C.2: DDAT Rim-drive Propulsion Motor Rotor-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotor I/D</td>
<td>840</td>
<td>mm</td>
</tr>
<tr>
<td>Rotor O/D</td>
<td>970</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>45</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M420-50D</td>
<td>-</td>
</tr>
<tr>
<td>Number of bars</td>
<td>87</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>110</td>
<td>mm</td>
</tr>
<tr>
<td>Air-gap</td>
<td>3.5</td>
<td>mm</td>
</tr>
<tr>
<td>Bar material</td>
<td>Copper</td>
<td>-</td>
</tr>
<tr>
<td>Rotor bar area</td>
<td>25</td>
<td>mm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
</tbody>
</table>

### Table C.3: DDAT Rim-drive Propulsion Motor Stator-core Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator I/D</td>
<td>981</td>
<td>mm</td>
</tr>
<tr>
<td>Stator O/D</td>
<td>1,137.8</td>
<td>mm</td>
</tr>
<tr>
<td>Back-iron thickness</td>
<td>45</td>
<td>mm</td>
</tr>
<tr>
<td>Lamination material</td>
<td>M420-50D</td>
<td>-</td>
</tr>
<tr>
<td>Number of slots</td>
<td>72</td>
<td>-</td>
</tr>
<tr>
<td>Axial length (active)</td>
<td>110</td>
<td>mm</td>
</tr>
<tr>
<td>Stator-slot area</td>
<td>692</td>
<td>mm&lt;sup&gt;2&lt;/sup&gt;</td>
</tr>
</tbody>
</table>
### Table C.4: DDAT Rim-drive Propulsion Motor Windings Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coils per slot</td>
<td>2</td>
<td></td>
</tr>
<tr>
<td>Coils per group</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>Turns per coil</td>
<td>25</td>
<td></td>
</tr>
<tr>
<td>Number of turns per phase (in series)</td>
<td>100</td>
<td></td>
</tr>
<tr>
<td>Number of parallel paths</td>
<td>6</td>
<td></td>
</tr>
<tr>
<td>Conductor diameter</td>
<td>1.25</td>
<td>mm</td>
</tr>
<tr>
<td>Number of conductors per turn</td>
<td>5</td>
<td></td>
</tr>
<tr>
<td>Total cross-sectional area per turn</td>
<td>6.16</td>
<td>mm²</td>
</tr>
<tr>
<td>Conductor material</td>
<td>Copper</td>
<td></td>
</tr>
<tr>
<td>Mean turn length</td>
<td>903</td>
<td>mm</td>
</tr>
<tr>
<td>Resistance per phase</td>
<td>0.061</td>
<td>Ω</td>
</tr>
<tr>
<td>End-winding inductance per phase</td>
<td>0.67</td>
<td>mH</td>
</tr>
</tbody>
</table>

### Table C.5: DDAT Rim-drive Propulsion Motor Full-load Performance Data

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Magnetising current (phase)</td>
<td>203.55 A</td>
<td></td>
</tr>
<tr>
<td>Full-load current (phase)</td>
<td>281.46 A</td>
<td></td>
</tr>
<tr>
<td>Slip</td>
<td>0.04445</td>
<td></td>
</tr>
<tr>
<td>Output speed</td>
<td>573.33 rpm</td>
<td></td>
</tr>
<tr>
<td>Output torque</td>
<td>3,042 Nm</td>
<td></td>
</tr>
<tr>
<td>Input power</td>
<td>212 kW</td>
<td></td>
</tr>
<tr>
<td>Power factor</td>
<td>0.57</td>
<td></td>
</tr>
<tr>
<td>Total stator joule losses</td>
<td>14.3 kW</td>
<td></td>
</tr>
<tr>
<td>Stator iron losses</td>
<td>933 W</td>
<td></td>
</tr>
<tr>
<td>Stator Inconel-can losses</td>
<td>5.2 kW</td>
<td></td>
</tr>
<tr>
<td>Rotor cage joule losses</td>
<td>8.7 kW</td>
<td></td>
</tr>
<tr>
<td>Rotor copper-can joule losses</td>
<td>1.4 kW</td>
<td></td>
</tr>
<tr>
<td>Rotor Inconel-can losses</td>
<td>10 W</td>
<td></td>
</tr>
<tr>
<td>Rotor iron losses</td>
<td>611 W</td>
<td></td>
</tr>
<tr>
<td>Efficiency (no windage or friction)</td>
<td>86.2 %</td>
<td></td>
</tr>
</tbody>
</table>

### Table C.6: DDAT Rim-drive Propulsion Motor Densities and Mass

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper density</td>
<td>8,920 kg/m³</td>
<td></td>
</tr>
<tr>
<td>Inconel density</td>
<td>8,440 kg/m³</td>
<td></td>
</tr>
<tr>
<td>Lamination material density</td>
<td>7,650 kg/m³</td>
<td></td>
</tr>
<tr>
<td>Copper-can mass</td>
<td>3 kg</td>
<td></td>
</tr>
<tr>
<td>Squirrel-cage mass</td>
<td>26.5 kg</td>
<td></td>
</tr>
<tr>
<td>Winding mass</td>
<td>27 kg</td>
<td></td>
</tr>
<tr>
<td>Stator- and rotor-core mass</td>
<td>312 kg</td>
<td></td>
</tr>
<tr>
<td>Rotor Inconel-can mass</td>
<td>1.4 kg</td>
<td></td>
</tr>
<tr>
<td>Stator Inconel-can mass</td>
<td>2 kg</td>
<td></td>
</tr>
<tr>
<td>Total motor mass</td>
<td>372 kg</td>
<td></td>
</tr>
</tbody>
</table>
Figure C.1: Air-gap Magnetic Field Produced by the DDAT Rim-drive Propulsion Motor Design at No-load and Full-load

Figure C.2: Harmonic Analysis of the Air-gap Magnetic Field Produced by the DDAT Rim-drive Propulsion Motor Design at No-load and Full-load
Appendix D

Prototype Rim-drive Motor Stator Windings Layout
### Table D.1: Prototype Rim-drive Motor Stator Double-layer Windings Layout per Stator-slot

<table>
<thead>
<tr>
<th>Slot Number</th>
<th>Top Layer</th>
<th>Bottom Layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>R1+</td>
<td>R36+</td>
</tr>
<tr>
<td>2</td>
<td>R2+</td>
<td>Y35-</td>
</tr>
<tr>
<td>3</td>
<td>Y1-</td>
<td>Y36-</td>
</tr>
<tr>
<td>4</td>
<td>Y2-</td>
<td>B35+</td>
</tr>
<tr>
<td>5</td>
<td>B1+</td>
<td>B36+</td>
</tr>
<tr>
<td>6</td>
<td>B2+</td>
<td>R1-</td>
</tr>
<tr>
<td>7</td>
<td>R3-</td>
<td>R2-</td>
</tr>
<tr>
<td>8</td>
<td>R4-</td>
<td>Y1+</td>
</tr>
<tr>
<td>9</td>
<td>Y3+</td>
<td>Y2+</td>
</tr>
<tr>
<td>10</td>
<td>Y4+</td>
<td>B1-</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Slot Number</th>
<th>Top Layer</th>
<th>Bottom Layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>11</td>
<td>B3-</td>
<td>B2-</td>
</tr>
<tr>
<td>12</td>
<td>B4-</td>
<td>R3+</td>
</tr>
<tr>
<td>13</td>
<td>R5+</td>
<td>R4+</td>
</tr>
<tr>
<td>14</td>
<td>Y6+</td>
<td>Y3-</td>
</tr>
<tr>
<td>15</td>
<td>R7-</td>
<td>B1-</td>
</tr>
<tr>
<td>16</td>
<td>R8-</td>
<td>B35+</td>
</tr>
<tr>
<td>17</td>
<td>Y5-</td>
<td>B36+</td>
</tr>
<tr>
<td>18</td>
<td>Y6-</td>
<td>R1-</td>
</tr>
<tr>
<td>19</td>
<td>R7-</td>
<td>R2-</td>
</tr>
<tr>
<td>20</td>
<td>R8-</td>
<td>Y1+</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Slot Number</th>
<th>Top Layer</th>
<th>Bottom Layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>21</td>
<td>Y7+</td>
<td>Y6+</td>
</tr>
<tr>
<td>22</td>
<td>Y8+</td>
<td>B5-</td>
</tr>
<tr>
<td>23</td>
<td>B7-</td>
<td>B6-</td>
</tr>
<tr>
<td>24</td>
<td>B8-</td>
<td>R7+</td>
</tr>
<tr>
<td>25</td>
<td>Y9+</td>
<td>R8+</td>
</tr>
<tr>
<td>26</td>
<td>Y10-</td>
<td>Y7-</td>
</tr>
<tr>
<td>27</td>
<td>Y10-</td>
<td>Y8-</td>
</tr>
<tr>
<td>28</td>
<td>Y11-</td>
<td>B7-</td>
</tr>
<tr>
<td>29</td>
<td>Y12+</td>
<td>R8-</td>
</tr>
<tr>
<td>30</td>
<td>B9+</td>
<td>B10+</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Slot Number</th>
<th>Top Layer</th>
<th>Bottom Layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>31</td>
<td>R11-</td>
<td>R10-</td>
</tr>
<tr>
<td>32</td>
<td>R12-</td>
<td>Y9+</td>
</tr>
<tr>
<td>33</td>
<td>Y10+</td>
<td>B9-</td>
</tr>
<tr>
<td>34</td>
<td>Y11-</td>
<td>B10-</td>
</tr>
<tr>
<td>35</td>
<td>Y12+</td>
<td>R11-</td>
</tr>
<tr>
<td>36</td>
<td>B11-</td>
<td>Y10-</td>
</tr>
<tr>
<td>37</td>
<td>B12-</td>
<td>Y9-</td>
</tr>
<tr>
<td>38</td>
<td>R13-</td>
<td>R10-</td>
</tr>
<tr>
<td>39</td>
<td>R14-</td>
<td>Y11-</td>
</tr>
<tr>
<td>40</td>
<td>Y12-</td>
<td>Y10-</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Slot Number</th>
<th>Top Layer</th>
<th>Bottom Layer</th>
</tr>
</thead>
<tbody>
<tr>
<td>41</td>
<td>B13+</td>
<td>B12+</td>
</tr>
<tr>
<td>42</td>
<td>B14+</td>
<td>R13-</td>
</tr>
<tr>
<td>43</td>
<td>R14-</td>
<td>R12-</td>
</tr>
<tr>
<td>44</td>
<td>Y15+</td>
<td>Y14-</td>
</tr>
<tr>
<td>45</td>
<td>Y16+</td>
<td>B13-</td>
</tr>
<tr>
<td>46</td>
<td>B15-</td>
<td>B14-</td>
</tr>
<tr>
<td>47</td>
<td>R16-</td>
<td>R15-</td>
</tr>
<tr>
<td>48</td>
<td>Y17+</td>
<td>Y16-</td>
</tr>
<tr>
<td>49</td>
<td>Y18+</td>
<td>B17-</td>
</tr>
<tr>
<td>50</td>
<td>B18-</td>
<td>B17-</td>
</tr>
</tbody>
</table>

<table>
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