Mississippi State University Scholars Junction

Theses and Dissertations

Theses and Dissertations

5-12-2023

Design of high-power ultra-high-speed permanent magnet machine

Md Khurshedul Islam Mississippi State University, khurshedeeeiiuc@gmail.com

Follow this and additional works at: https://scholarsjunction.msstate.edu/td

Part of the Electrical and Electronics Commons, Other Electrical and Computer Engineering Commons, and the Power and Energy Commons

Recommended Citation

Islam, Md Khurshedul, "Design of high-power ultra-high-speed permanent magnet machine" (2023). *Theses and Dissertations*. 5762. https://scholarsjunction.msstate.edu/td/5762

This Dissertation - Open Access is brought to you for free and open access by the Theses and Dissertations at Scholars Junction. It has been accepted for inclusion in Theses and Dissertations by an authorized administrator of Scholars Junction. For more information, please contact scholcomm@msstate.libanswers.com.

Design of high-power ultra-high-speed permanent magnet machine

By

Md Khurshedul Islam

Approved by:

Seungdeog Choi (Major Professor) Masoud Karimi-Ghartemani (Committee Member) Yong Fu (Committee Member) Prashant Singh (Committee Member) Qian Jenny Du (Graduate Coordinator) Jason M. Keith (Dean, Bagley College of Engineering)

A Dissertation Submitted to the Faculty of Mississippi State University in Partial Fulfillment of the Requirements for the Degree of Doctor of Philosophy in Electrical and Computer Engineering in the Department of Electrical and Computer Engineering

Mississippi State, Mississippi

May 2023

Copyright by

Md Khurshedul Islam

2023

Name: Md Khurshedul Islam Date of Degree: May 12, 2023 Institution: Mississippi State University Major Field: Electrical and Computer Engineering Major Professor: Seungdeog Choi Title of Study: Design of high-power ultra-high-speed permanent magnet machine Pages in Study: 149

Candidate for Degree of Doctor of Philosophy

The demand for ultra-high-speed machines (UHSM) is rapidly growing in high-tech industries due to their attractive features. A-mechanically-based-antenna (AMEBA) system is another emerging application of UHSM. It enables portable wireless communication in the radio frequency (RF)-denied environment, which was not possible until recently. The AMEBA system requires a high-power (HP) UHSM for its effective communication performance. However, at the expected rotational speed range of 0.5 to 1 million rpm, the power level of UHSM is limited, and no research effort has succeeded to improve the power level of UHSM.

The design of HP-UHSM is highly iterative, and it presents several critical challenges, unlike low-power UHSM, such as critical-bending-resonance (CBR), strong mutual influence among Multiphysics performances, exponential air-friction loss, and material limitation. When the magnetic loading of the UHSM rotor is increased to improve the power level, the rotor experiences serious mechanical vibration due to the excessive centrifugal forces and CBR. This vibration limits the operation of HP-UHSM and leads to structural breakdown. Furthermore, the design process becomes more critical when it considers the multidisciplinary design constraints and application requirements. This dissertation proposed a new Multiphysics design method to develop HP-UHSM for critical applications. First, the critical design constraints which prevent increasing the output power of UHSM are investigated. Then, a Multiphysics optimization model is developed by coupling several multidisciplinary analysis modules. This proposed optimization model enables (i) defining multidisciplinary design constraints, (ii) consideration of Multiphysics mutual influence, and (iii) a trade-off analysis between the efficiency and design-safety-margin. The proposed design model adopts the multiphase winding system to effectively increase the electrical loading in the slotless stator. Finally, a 2000 W 500,000 rpm HP-UHSM is optimized for an AMEBA system using the proposed design method.

The optimized 2 kW 500,000 rpm machine prototype and its dynamo setup are built in the laboratory. Extensive finite element simulations and experimental testing results are presented to validate the effectiveness of the proposed design method. The results show that the proposed HP-USHM has 94.5% efficiency, 47 kW/L power density, 30% global design safety margin at the maximum speed and no CBR frequency below 11 kHz.

DEDICATION

To my family and extended family

ACKNOWLEDGEMENTS

I would like to extend appreciation and gratitude to those who supported me with technical knowledge, guidance, and motivation to accomplish my graduate study's goal.

First, I would like to thank my advisor, Dr. Seungdeog Choi, for his guidance and support during my graduate study. His continuous inspiration and high work ethics motivate me to achieve my Ph.D. goal.

I would also like to thank my committee members Dr. Karimi, Dr. Fu, and Dr. Singh, for their suggestions and motivation.

Special thanks to my colleagues from the Power Electronics and Energy System Laboratory (PEESL) at Mississippi State University, Dr. Moinul Shahidul Haque, Mr. Ashik Amin, Mr. Moniruzzaman, Mr. Tahmid Ibne Manna, and Ms. Kazi Nishat Tasnim for their suggestions, brainstorming sessions, and inspiration.

Also, I would like to thank alumni of PEESL, especially Dr. Zakirul Islam and Dr. AKM Arafat, for their suggestions and support.

The final recognition goes to my family, especially to my mother, Mrs. Nilu-fa-Begum, my father, Ajgar Ali, my siblings, Md Shahedul Islam, Afroja Yeasmin Mishi, Md Jahedul Islam, my wife, Jasmine Akter, and my son, Mehrab Islam Ayzaan, for their love, patience, and encouragement.

TABLE OF CONTENTS

DEDIC	CATION	ii
ACKN	OWLEDGEMENTS	iii
LIST O	DF TABLES	vii
LIST O	OF FIGURES	ix
CHAPT	ΓER	
I.	INTRODUCTION	1
	 1.1 Motivation	1 3 3 4 5 6 6 6 7 7 7 10 11
II.	 CRITICAL DESIGN CONSTRAINTS OF HIGH-POWER ULTRA-HIGH-MACHINE 2.1 Introduction 2.2 Critical Design Constraints of HP-UHSM 2.2.1 Rotor Topology 2.2.2 Stator Topology 2.2.3 Stator Electric Loading in Slotless Stator 2.2.4 Air-friction Loss 2.2.5 Natural Resonance Frequency 2.2.6 Retaining Sleeve Thickness 2.2.7 Material Limitation 2.2.7.1 Permanent Magnet 2.2.7.3 Stator Core Materials 2.2.7.3 Stator Core Materials 	SPEED 14 14 15 15 18 20 26 27 29 30 30 31 32

	2.2.8	8 Winding Limitations	33
		2.2.8.1 Winding configuration:	33
	~	2.2.8.2 Winding Pattern:	.35
	2.2.9	9 Thermal Limitations Constraints	36
	2.2.	10 Static Interference-fit Limit	37
	2.2.	11 Mutual Influence of Multiphysics Performances	39
		1 5	
III.	MODEI	LING OF PROPOSED MULTI-DISCIPLINARY ANALYSIS MODULES F	OR
	HIGH-I	POWER ULTRA-HIGH-SPEED MACHINE	.41
	3.1	Introduction	.41
	3.2	Electromagnetic Analysis Model	42
	3.3	Air-friction Loss Analysis Model	.47
	3.4	Thermal Analysis Model	.49
	3.5	Structural Analysis Model	57
	3.5	1 Contact Pressure Due to Shrink-fit	58
	3.5	Centrifugal Stress Due to Rotation	50
	3.5	2 Thermal Expansion Due to Temperature	.57
	36	Rotordynamic Analysis Model	.00
	5.0		.02
IV.	MULTI	PHYSICS DESIGN OF HIGH-POWER ULTRA-HIGH-SPEED MACHINE	.65
	4.1	Introduction	65
	4.1	IIIIroquetion	.03
	4.2	Existing Design Models and Limitations	.03
	4.3	Proposed Multiphysics Design Method	.69
V.	DESIG	N AND OPTIMIZATION OF HIGH-POWER ULTRA-HIGH-SPEED	
	MACH	INE FOR AMEBA SYSTEM	75
	_		
	5.1	Introduction	.75
	5.2	Derivation of HP-UHSM Specification from AMEBA System	.75
	5.3	Initial Considerations	.78
	5.4	PCA Analysis and Design Variables	
	5.5	Multiple Optimization Objectives	
	5.6	Multi-disciplinary Optimization Constraints	82
	5.7	Optimization Results	.83
	5.7.	Case Studies	.83
		5.7.1.1 Case 1	.83
		5.7.1.2 Case 2	.83
		5.7.1.3 Case 3	87
	5.7.2	2 Performance Comparison Between Three-phase and Six-phase Windings.	.87
	5.8	Multi-disciplinary Optimization Constraints	90
VI.	PERFO	RMANCE EVALUATION OF THE PROPOSED HIGH-POWER ULTRA-	
	HIGH-S	SPEED MACHINE USING FEA SIMULATION	.91
	6.1	Introduction	.91
		V	

	6.2 El	ectromagnetic Analysis	91
	6.3 Th	nermal Analysis	
	6.4 St	ructural Analysis	
	6.5 Ro	otordynamic Analysis	106
	6.5.1	Undamped Natural Frequency Analysis	106
	6.5.2	Selection of Guide Bearing Stiffness	106
	6.5.3	Campbell Diagram Analysis	
	6.5.4	Unbalance Response Analysis	110
VII.	PROTOT	YPING AND EXPERIMENTAL VALIDATION	113
	7.1 In	troduction	113
	7.2 Pr	ototyping Proposed HP-UHSM	113
	7.2.1	Rotor Prototype	113
	7.2.2	Stator Prototype	116
	7.2.3	Dynamo Prototype	
	7.3 Ex	sperimental Analysis of HP-UHSM	121
	7.3.1	Impulse Hammer Test	121
	7.3.2	Machine Parameters	
	7.3.3	Back-EMF Performance	
	7.3.4	Reaction Torque Performance	126
	7.3.5	Thermal Performance	129
	7.3.6	Efficiency Estimation	130
	7.3.7	Dynamic Performance	
	7.4 St	ate-of-the-art Performance Comparison	
VIII.	CONCLU	ISION AND FUTURE RESEARCH SCOPE	136
	8.1 Co	onclusion and Summary of the Proposed Study	136
	8.2 Fu	Iture Work	138
	8.2.1	Integration of Magnetic Bearing in the Proposed HP-UHSM	138
	8.2.2	Implementation of the Proposed Design Method in Switch Reluctan	nce
		Machine	139
	8.2.3	Apply the Proposed Design Method to Optimize other HP-UHSM.	140
	8.2.4	Investigation of the Acoustic Noise	140
	8.2.5	Torque Measurement	141
REFER	RENCES		142

LIST OF TABLES

Table 2.1	Impact of input current on HP-UHSM, $(N_{TP} = 20)$	23
Table 2.2	Impact of coil turns on HP-UHSM, $(I_a = 4 \text{ A})$	23
Table 2.3	Important properties of the PM materials	31
Table 2.4	Key properties of the different sleeve materials	32
Table 2.5	Key electromagnetic and thermal properties of stator materials	33
Table 4.1	Existing design methodologies of UHSM	66
Table 5.1	Proposed UHSM specifications for AMEBA system	77
Table 5.2	Input variables and their limits for DOE	80
Table 5.3	Inputs and output parameters of selected DPs	86
Table 5.4	Design parameters of optimized machines (M-1, M-2, and M-3)	88
Table 5.5	Multiphysics performances of the three-phase and six-phase designs	88
Table 6.1	Heat flow of HP-UHSM due to air-friction loss	98
Table 6.2	Heat rate of HP-UHSM due to electromagnetic losses	98
Table 6.3	Thermal conductivity of different active materials of the proposed HP- UHSM	100
Table 7.1	Comparison between the measured and calculated undamped natural bending frequencies (UNBFs) of proposed rotor prototype	123
Table 7.2	Comparison between the measured and FEA Electrical parameters of the proposed HP-UHSM	124
Table 7.3	Comparison of phase to neutral back-EMF between the six-phase and three-phase HP-UHSM.	126
Table 7.4	Comparison of coil temperature between the three-phase the six-phase HP-UHSM	131
	••	

Table 7.5Performance comparison of the proposed HP-UHSM and existing UHSMs......135

LIST OF FIGURES

Figure 1.1	(a) A simplified illustration of an AMEBA system using a motor drive, (b) an initial AMEBA transmitter [1]
Figure 1.2	A 70 W, 300 000 rpm turbo-compressor developed by Celeroton [5]3
Figure 1.3	 (a) 3 kW, 40,000 rpm NASA flywheel energy storage [7], (b) 30 kW, 100,000 rpm homopolar motor for flywheel energy storage [8], and (c) 100 W 300,000 rpm permanent magnet synchronous motor flywheel energy storage [9].
Figure 1.4	(a) Dental hand piece with a low-speed motor and several stages of mechanical transmission. (b) direct-driven dental handpiece proposed in [10]5
Figure 1.5	A 100 W 500,000 rpm UHSM for solar impulse application [12]6
Figure 1.6	(Left) ultra-high-speed rotor with a six-faced mirror head for a laser scanning system, (Right) 300 W 500,000 rpm UHSM for the laser scanning system [13]
Figure 1.7	The scenario of UHSMs: existing drives, trends, and emerging applications
Figure 2.1	Conventional PM rotor topologies of permanent magnet machine [33]15
Figure 2.2	Rotor topology for low-power UHSMs16
Figure 2.3	Impact of inner shaft on the torque development of UHSM at 500,000 rpm16
Figure 2.4	Cross-section of the proposed rotor geometry for HP-UHSM17
Figure 2.5	 (a) Slotted UHSM geometry, (b) Slotless UHSM geometry, (c) electromagnetic torque of slotted stator, (d) electromagnetic torque of slotless stator, (e) airgap flux density of slotted stator, (f) airgap flux density of slotless stator, (g) core loss of slotted and slotless stator, and (h) rotor eddy current loss
Figure 2.6	Coil diameter (unserved) and peak current carrying capacity vs strand numbers of Litz coil, obtained by using manufacturer data [37]
Figure 2.7	Effective airgap length difference in slotted and slotless stator

Figure 2.8	Effective airgap length and average airgap diameter in the slotless stator22	2
Figure 2.9	Performance variation of UHSM at different winding configurations. All machines have similar geometry, the same rotor and stator dimensions, and a rated speed of 500,000 r/min	5
Figure 2.10	Air-friction loss at of the proposed rotor at different speed and outer rotor radius	7
Figure 2.11	The impact of air-friction loss on the PM axial temperature variation of the proposed rotor at 500,000 rpm	7
Figure 2.12	Undamped natural resonances of the proposed rotor at different axial length2	8
Figure 2.13	The variation of VMES on inner sleeve and PM center with the sleeve thickness (at 500 000 r/min and $R_1 = 4$ mm)	0
Figure 2.14	End-winding different between the toroidal and airgap winding	4
Figure 2.15	Performance comparison between the concentrated and distributed winding of 2-pole 6-slot machine: (a) concentrated winding (DW), (b) MMF harmonic of DW, (c) distributed winding (CW), and (d) MMF harmonic of CW. The torque performance is measured at 500,000 rpm, and with the same SEL and RML	6
Figure 2.16	Required static interference-fit length vs PM radius at 500,000 rpm. The PM and sleeve material is <i>Sm2Co</i> 17 and Ti-6Al-4V, respectively	8
Figure 2.17	Mutual influence of Multiphysics performances of HP-UHSM	9
Figure 3.1	2D and 3D cross section of proposed HP-UHSM geometry4	3
Figure 3.2	Parametric FEA electromagnetic Model of the HP-UHSM: (a) $1/8^{th}$ of radial cross-section, and (b) $1/2^{th}$ of the axial cross-section	3
Figure 3.3	Phasor diagram of different multiphase winding configurations	7
Figure 3.4	Airgap air model for 3D CFD analysis4	8
Figure 3.5	3D view of the HP-UHSM with casing	0
Figure 3.6	Simplified lumped parameter thermal network (LPTN) of proposed HP- UHSM	0
Figure 3.7	Fourier's law of thermal conduction for a plane wall body5	1
Figure 3.8	Fourier's law of thermal conduction for a cylindrical body	2

Figure 3.9	Newton's law of cooling.	52
Figure 3.10	A PM of axial length L, with temperature variation as a function of x, where $(0 < x < L)$.	55
Figure 3.11	PM segmentation into <i>k</i> nodes along the axial direction	55
Figure 3.12	Temperature impact on the 500,000 rpm rotor at different <i>RPM</i> .	57
Figure 3.13	(a) Stress analysis model and (b) yielding criteria of the proposed HP- UHSM.	57
Figure 3.14	Different mode shapes of UHS rotor.	63
Figure 3.15	Rotor geometry for Rotordynamic model: (1/2 th cross-section)	64
Figure 4.1	Proposed Multiphysics design strategy of HP-UHSM for AMEBA application.	70
Figure 4.2	Co-simulation analysis of Multiphysics analysis modules in ANSYS Workbench.	71
Figure 5.1	PPM dipole geometry and example of AMEBA rotor dynamic.	76
Figure 5.2	Sensitivity analysis of design parameters on optimization outputs of the proposed HP-UHSM.	79
Figure 5.3	Spearman correlation coefficient matrix of output parameters of the proposed HP-UHSM.	80
Figure 5.4	HP-UHSM performances vs. number of phases	84
Figure 5.5	Trade-off plot and Pareto-front line of the optimization results	85
Figure 5.6	Variation of different losses with PM radius at 500 kr/min	85
Figure 5.7	Optimized HP-UHSM models for 3 different cases (not drawn to scale)	87
Figure 6.1	2D and 3D FEA model of the optimized HP-UHSM with a 6-phase toroidal winding configuration.	92
Figure 6.2	The non-linear BH curve of (a) <i>Sm</i> ₂ <i>Co</i> ₁₇ and (b) Amorphous <i>Metglas</i> -2605SA.	93
Figure 6.3	6-phase back-EMF at 500,000 rpm and (b) rated six-phase input current at 500,000 rpm.	94
Figure 6.4	Electromagnetic torque of the proposed HP-UHSM at the rated condition	94

Figure 6.5	Mutual inductance interaction calculation between two winding sets.	94
Figure 6.6	Flux density distribution of (Left) 100W and (Right) 2000 W UHSM, generated with the rated stator current only.	95
Figure 6.7	Flux density distribution and flux lines at rated load condition.	95
Figure 6.8	Efficiency map of the proposed HP-UHSM	96
Figure 6.9	Loss distribution of proposed HP-UHSM at rated condition.	96
Figure 6.10	Losses vs. operating frequency of the proposed HP-UHSM	97
Figure 6.11	FEA thermal analysis model in ANSYS Steady-State-Thermal	99
Figure 6.12	(Top) temperature distribution of the proposed HP-UHSM at the rated loading condition, (bottom) PM axial temperature variation using FEA and quasi-3D thermal model.	101
Figure 6.13	Structural FEA model of the proposed HP-UHSM.	103
Figure 6.14	Radial displacement of HP-UHSM (N = 25 °C & T = 130 °C) at 500,000 rpm.	103
Figure 6.15	Stress distribution of the proposed HP-UHSM: (Left) 0 rpm, (Right) 500,000 rpm.	104
Figure 6.16	Stress (VMS) distribution in the PM of HP-UHSM	104
Figure 6.17	Stress (VMS) distribution in the sleeve of the HP-UHSM	105
Figure 6.18	Deformation mode shape of the proposed rotor at different undamped natural frequencies: (a) lateral mode (1 Hz), (b) conical mode (5 Hz), (c) 1 st order bending mode (9012 Hz), and (d) 2 nd order bending mode (17053 Hz).	107
Figure 6.19	Natural frequencies vs guide bearing stiffness	108
Figure 6.20	Campbell diagram of HP-UHSM. [S-stable, US-unstable, CSP- critical speed point]	109
Figure 6.21	1 st order bending frequency and different observation points for unbalance response analysis.	111
Figure 6.22	Unbalance response analysis result: (Frequency Response Function)	111
Figure 7.1	The AMEBA rotor development using the shrink-fit technique	114
Figure 7.2	Induction heater with graphite cruciate and Ceramic Container	114

Figure 7.3	Rotor parts and assembly of the proposed HP-UHSM	115
Figure 7.4	Slotless stator core made of Amorphous Metglas iron sheet.	117
Figure 7.5	Three-phase stator winding pattern applied in the M-3 prototype	117
Figure 7.6	Six-phase stator winding pattern applied in proposed HP-UHSM (M-2) prototype.	118
Figure 7.7	(a) Three-phase stator and (b) six-phase stator prototype using slotless stator and toroidal winding	118
Figure 7.8	Prototype: Six-phase HP-UHSM (b) three-phase HP-UHSM, and (c) front view.	119
Figure 7.9	Motoring test bench of the proposed HP-UHSM.	119
Figure 7.10	(a) Cascaded rotor for dynamo testing, and (b) Dynamo setup of the proposed HP-UHSM.	120
Figure 7.11	Impact hammer test setup of AMEBA rotor prototype	122
Figure 7.12	Frequency spectrum result of the impulse hammer test.	122
Figure 7.13	Full experimental setup of the proposed HP-UHSM.	125
Figure 7.14	(Top) phase-A back-EMF at 120,000 rpm, (bottom) back-EMF versus speed	125
Figure 7.15	The measured back-EMF of both HP-UHSMs at 50,000 rpm.	126
Figure 7.16	Customized six-phase GaN inverter development for 2000 W 500,000 rpm HP-UHSM	127
Figure 7.17	Sensor less FOC control applied for motoring operation	127
Figure 7.18	(Top) Phase-A input current at 120,000 rpm, (bottom) reaction torque vs. phase current (RMS)	128
Figure 7.19	The measured reaction torque of three-phase HP-UHSMs at 120,000 rpm	128
Figure 7.20	The input current of both HP-UHSMs at 60,000 rpm motoring operation	129
Figure 7.21	Measured coil temperature of the proposed HP-UHSM at different speeds: (top) using thermal imager (bottom) using pt100 sensor.	131
Figure 7.22	Measured and FEA efficiency of the dynamo setup.	133

Figure 7.23	Speed tracking test of the proposed HP-UHSM (realizing the AMEBA operation).	133
Figure 7.24	Temperature of the proposed six-phase HP-UHSM at 350,000 rpm	134
Figure 8.1	Proposed HP-UHSM geometry with magnetic bearing configuration	139
Figure 8.2	High-speed switched reluctance machine topology.	140

CHAPTER I

INTRODUCTION

1.1 Motivation

Wireless communication between the earth's surface and underwater or undersea facilities (submarines, mines, tunnels, etc.) is very difficult due to the short skin-depth of electromagnetic waves in such harsh media, which leads to high attenuation [1]. Fortunately, the skin-depth increases as the transmitting frequency decrease. Hence extremely low-frequency such as ultra-low-frequency (ULF) 0.3-3 kHz and very-low-frequency (VLF) 3-30 kHz can penetrate long distances in the radio frequency (RF)-denied environment. However, constructing ULF-VLF antenna is very costly and challenging. It is well known that the antenna size is inversely proportional to its operating frequency, so communication at ULF-VLF requires the construction of gigantic antennas [2], [3]. These giant coil antennas are inefficient in field generation and consume MWs of input power. For example, the US Navy's VLF transmitter at Maine uses two 1.2 miles long antenna and consumes 2 MW of power to transmit a coded text message at a relatively low data rate. Consequently, bi-directional (facilities to earth's surface) wireless communication was not possible until recently due to the immense antenna size and huge power level requirement.

One of the most power-efficient solutions to this problem is to use a mechanically based antenna (AMEBA) for generating the ULF-VLF signal, as shown in Figure 1.1(a). Unlike conventional coil antennas, the AMEBA system uses a motor drive to rotate a polarized



Figure 1.1 (a) A simplified illustration of an AMEBA system using a motor drive, (b) an initial AMEBA transmitter [1].

permanent magnet (PPM) dipole at the ULF-VLF for generating the alternating magnetic field with no additional power consumption [4]. Also, the transmitter's field generation efficiency can be improved significantly using the high-energy-density rare-earth magnet as a PPM dipole. For example, let us compare a mechanical antenna using a cylindrical *Nd-Fe-B* magnet (Grade N52, L=R=10 cm) with an N-turn coil loop electric antenna of the same cross-sectional area. To obtain the same static magnetic field, the electrical coil antenna requires a large DC current of ~10⁵/N A.

Figure 1.1(a) shows that the motor drive is the key component in the AMEBA system, which rotates the PPM at a transmitting frequency. Figure 1.1(b) shows our initial AMEBA transmitter prototype, where a 12 W 10,000 revolutions per minute (rpm) motor drive has been used to drive a PPM dipole of 1.83×10^{-6} kgm² [1]. Due to the limited power and speed rating of motor drive, the antenna has a maximum data transfer rate (DTR) of 2 Hz/s with a bandwidth of 88-116 Hz. It can transmit only ~7 characters per minute to a few meters in the RF-denied environment. However, our current NSF project has aimed at reducing the long transmitting time and increasing the limited operating distance. Using an ultra-high-speed machine (UHSM) of 500,000 rpm, the operating bandwidth can be increased up to 8.3 kHz. And increasing the output



Figure 1.2 A 70 W, 300 000 rpm turbo-compressor developed by Celeroton [5].

power of the UHSM to 2 kW will allow driving a PPM dipole of 1.62×10^{-4} kgm² (88 times larger than the previous PPM) with a dynamic of 12 Hz/s. With these improvement in the motor drive, the AMEBA transmitter will be able to transmit 160 characters within just 2 minutes beyond 1 km distance. Also, the efficient and compact high-power (HP) UHSM in the AMEBA system will immediately enable bi-directional wireless communication in the RF-denied environment.

This is one of the promising examples of what is possible with an HP-UHSM drive. Other applications of HP-UHSM are presented in section 1.2, an overview of the state-of-the-art research and development is given in section 1.3, and the critical challenges of designing an HP-UHSM are outlined in section 1.4.

1.2 Applications of HP-UHSM

1.2.1 Turbo-compressors

One of the major applications of UHSM is the turbo-compressor, which is used in the combustion engine or fuel cell system. By increasing the output power of UHSM, the turbo-



Figure 1.3 (a) 3 kW, 40,000 rpm NASA flywheel energy storage [7], (b) 30 kW, 100,000 rpm homopolar motor for flywheel energy storage [8], and (c) 100 W 300,000 rpm permanent magnet synchronous motor flywheel energy storage [9].

compressor system performance can be improved. For example, an HP-UHSM can increase efficiency and reduce the volume and weight of the fuel cell system significantly. Figure 1.2 shows a 70 W, 300,000 rpm turbo-compressor developed by Celeroton for the fuel cell [5].

1.2.2 Flywheel Energy Storages

Currently, flywheels are getting more attention for spacecraft energy storage, which stores energy by means of rotational energy. A flywheel can increase its energy storage capability either by increasing the mass of the rotor and running it at a low speed or by increasing the rotational speed and running it with a lower mass [6]. A flywheel with high-speed and lower mass is the better choice because it will reduce flywheel size and weight by a proportional factor of speed increased. Therefore, increasing the power rating of UHSM will further increase the energy storage capability of the flywheel energy storage system. Figure 1.3 shows some UHSMs designed for the flywheel energy storage application by the different research teams [7]-[9].



Figure 1.4 (a) Dental hand piece with a low-speed motor and several stages of mechanical transmission. (b) direct-driven dental handpiece proposed in [10].

1.2.3 Medical Spindles

The conventional dental handpieces are powered by air-turbines using a compressed airsupply. The main problem with the technique is that its accurate speed control is impossible. Also, a typical dental surgery requires four to five different speeds to perform the operation. To do this, the conventional technique uses a gearbox for changing the speed or uses multiple handpieces with different air supply. Replacing the conventional air-spindle with a direct-drive and adjustable UHSM can reduce the number of handpieces and additional gearboxes. It will reduce the handpiece cost significantly with the added benefit of a smaller handpiece size. Furthermore, an HP-UHSM drive will guarantee high torque for low-speed treatment and accurate controllability at high-speed treatment. Figure 1.4 shows a regular dental drill powered by 40,000 rpm, with a gear system to increase its operating speed to 200,000 rpm and a new direct-drive dental handpiece with an operating speed up to 400,000 rpm [10].



Figure 1.5 A 100 W 500,000 rpm UHSM for solar impulse application [12].

1.2.4 Solar Impulse Systems

The Solar Impulse project aims to build a solar-powered aircraft that will fly around the world day and night without any conventional fuel or pollution [11]. During the day, it stores the solar energy in batteries first, then in altitude, and climbs up to 12,000 m. One of the key components of this aircraft is a cabin air pressurization system. A conventional air compressor will increase the aircraft's weight by approximately 20 kg. On the other hand, using a UHSM of 500,000 rpm can reduce the compressor weight to 0.2 kg, with the additional advantage of higher efficiency and lower footprint [12]. Furthermore, increasing the power rating of the UHSM will increase the efficiency and capability of the compressor significantly. Figure 1.5 shows a 100 W 500,000 rpm UHSM developed for solar impulse system.

1.2.5 Optical Systems

Optical systems such as scanners, laser, and high-speed cameras use a rotating mirror to divert a light source. For example, a rotating mirror camera rotates its mirrors with a speed of 1200,000 rpm to produce 25 million frames per second [13]. Usually, these mirrors are rotated by



Figure 1.6 (Left) ultra-high-speed rotor with a six-faced mirror head for a laser scanning system, (Right) 300 W 500,000 rpm UHSM for the laser scanning system [13].

using air or helium turbines. By replacing these conventional turbines with an ultra-high-speed motor drive can reduce the size and weight of the optical system significantly. And the accurate speed control feature of UHSM will improve the reliability of the safety critical optical system such as depth scanning of human retinas. In [14], a 300 W 500,000 rpm UHSM drive is developed and tested for a laser scanner system, as shown in Figure 1.6.

1.2.6 Other Applications

Various other applications of ultra-high-speed electrical drives are developed by the hightech industries. These include mega gravity science [15], respirators, micromachining, blowers, turbines, PCB drilling, and pneumatics [16].

1.3 State of the Art

In the last two decades, several research groups and high-tech industries have investigated high-speed motor derives for different applications. Figure 1.7 shows the existing high-speed motor drives, their emerging application areas, and trends. It also shows a " $rpm\sqrt{kW}$ " constant line, dividing the speed region between high-speed and ultra-high-speed defined by [17]. In [18],



Figure 1.7 The scenario of UHSMs: existing drives, trends, and emerging applications.

various ultra-high-speed motor up to 240,000 rpm and 5 kW are designed for gas turbine and compressor application. For dental hand pieces, a 10 W 200,000 rpm UHSM is designed in [12]. A 1 kW 452,000 rpm UHSM is designed in [19] for fuel cell air compression application. In [20], [21] various 200,000 rpm UHSMs with a power level of 200 W to 900 W are designed for PCB drilling and grinding applications. A 2 kW 200,000 rpm UHSM is designed in [22] for cryogenic applications. A 14.4 kW 110,000 rpm UHSM is designed for air-compressor in the fuel cell application [23]. As per commercial products, 200,000 rpm UHSM is available from ATE at power levels from 100 W to 1000 W [24], and higher speed up to 400,000 rpm are available from Calnetix, although their detailed information cannot be found [25].

The most critical design challenges occur when the operating speed goes above 400,000 rpm. Considering the wide bandwidth requirement (0.3-10 kHz) of the AMEBA system, the motor having an operating speed of ~500,000 rpm or more are our interest in this study. In literature, a few UHSMs have been designed with a rated speed from ~500,000 rpm to 1200,000 rpm [13],

[26]-[31]. The first 500,000 rpm permanent magnet synchronous machine (PMSM) was designed for low-power mesoscale gas turbine application in 2005 [26]. The machine has a rated power of 100 W with 88.9% efficiency and ~20 kW/L power density. This motor is commercially available in [27]. Later the same design method is applied to design a 100 W 1 million rpm PM machine [28]. Doubling the rated speed increases the machine's electrical efficiency and power density but it reduces the shaft torque to 0.59 mNm and increases both the bearing and air-friction loss considerably. Another 500,000 rpm 100 W PMSM has been designed in [29] for gas turbine application. It uses a multi-disciplinary design algorithm and replaces the high-cost amorphous stator core of [26]. A 500,000 r/min 300 W PMSM is designed in [14] for a laser scanning application. It is a self-bearing motor that uses the magnetic bearing technique to ensure precise speed control and reduce friction losses. A 55 W 850,000 PMSM has been presented for micro turbine application in [30]. Besides the permanent magnet motor, the switched reluctance motor has also been studied for ultra-high-speed operation in [31]. It has a rated power of 125 W at 1200,000 rpm and it is designed by targeting the micromachining application.

However, these UHSMs cannot be used in high-power application such as AMEBA system because they have very low shaft torque or output power at ultra-high-speed operation. At 500,000 rpm or more speed, the highest reported output power is 300 W only. It has an efficiency of 86.2% and a power density of 16 kW/L only, which are significantly lower than [14]. On the other hand, the studied AMEBA system requires a 2 kW 500,000 rpm motor drive with at least 45 kW/L power density and 94% efficiency. Increasing the output power level of UHSM presents several critical design challenges including the critical bending resonance below rated speed, mechanical vibration, mutual influence among Multiphysics performance, non-linear axial temperature variation, higher DC-link voltage requirement, and manufacturing limitations. Therefore, the design models of low-power UHSM presented in the literature needs to be further studied to design an efficient, robust, and compact high-power UHSM. In state-of-the-art, no research attempt has been taken to design a UHSM for high-power applications yet. Moreover, UHSMs have never been investigated for the critical requirements of the AMEBA application either. This is one of the main driving forces of this proposed study.

1.4 Challenges

The main challenges against developing an HP-UHSM for AMEBA application are:

- I. The power level of electrical machines can be improved by increasing the stator's electric loading (SEL) or the rotor's magnetic loading (RML). The RML of 500,000 rpm UHSM is highly restricted by excessive friction loss and mechanical issues. Increasing SEL can improve the power level, but it reduces the efficiency and power density of the UHSM and increases the DC-link requirement. Therefore, increasing the output power of UHSM with a simultaneous increase in efficiency and power density is a challenge.
- II. Increasing the power level of UHSM makes its design process highly iterative and critical due to the strong mutual influence among Multiphysics performances and the higher number of design variables and constraints. A Multiphysics integrated design model with multi-objective optimization is required to design an efficient and robust HP-UHSM. Developing such an optimization model is one of the key challenges.
- III. The UHSM often encounters one or more critical bending resonance (CBR) frequencies below the rated speed. On the other hand, the HP-UHSM for AMEBA systems must not have any such CBR frequency. Designing an HP-UHSM without any CBR frequency below the rated speed is a challenging.

- IV. The AMEBA system requires a very compact HP-UHSM with at least a 30% design safety margin (DSM) from all physics perspectives. Achieving such a DSM of HP-UHSM at 500,000 rpm is a critical challenge.
- V. Commercial development of HP-UHSM is not available yet. Implementing interference fit in the rotor and developing a slotless stator coil are very critical and cumbersome. Also, UHS bearings, shaft couplers, sensors, and power electronics are not commercially available. Hence, the prototyping and testing of HP-UHSM is another challenge.

1.5 Publications and Scientific Contribution

A selection of publications based on this Ph.D. dissertation and project is listed below [85]-[93]:

Journal:

- M. K. Islam, S. Choi, Y. -K. Hong and S. Kwak, "Design of High-Power Ultra-High-Speed Rotor for Portable Mechanical Antenna Drives," in *IEEE Transactions on Industrial Electronics*, vol. 69, no. 12, pp. 12610-12620, Dec. 2022.
- [II] M. K. Islam, K. N. Tasnim, S. Choi, S. Kwak and Y. -K. Hong, "Performance Evaluation and Comparison of Three-Phase and Six-Phase Winding in Ultrahigh-Speed Machine for High-Power Application," in *IEEE Transactions on Industrial Electronics*, vol. 70, no. 5, pp. 4570-4582, May 2023.
- [III] M. K. Islam, K. N. Tasnim, S. Choi, S. Kwak and A. Arafat, "Designing High-Power Ultra-High-Speed Motor Using a New Multiphysics Multi-Objective Optimization Method for Mechanical Antenna Applications," *in IEEE Access*, vol. 10, pp. 106305-106323, 2022.

Conference:

- M. K. Islam, K. N. Tasnim, S. Choi and P. Singh, "Comparison of Three-Phase and Six-Phase High-Power Ultra-High-Speed Machine for Portable Mechanical Antenna," 2022 IEEE Applied Power Electronics Conference and Exposition (APEC), 2022, pp. 167-174.
- [II] M. K. Islam and S. Choi, "Multiphysics Optimization Model to Design High-Power Ultra-High-Speed Machine for Portable Mechanical Antenna Application," 2021 IEEE Energy Conversion Congress and Exposition (ECCE), 2021, pp. 4624-4631.
- [III] M. K. Islam and S. Choi, "Modeling and Design of a 6-Phase Ultra-High-Speed Machine for ELF/VLF Wireless Communication Transmitter," 2021 IEEE Applied Power Electronics Conference and Exposition (APEC), 2021, pp. 1062-1069.
- [IV] M. K. Islam and S. Choi, "Rotordynamic Analysis of 500 000 r/min 2 kW Ultra-High-Speed Machine for Portable Mechanical Antenna," 2021 IEEE International Electric Machines & Drives Conference (IEMDC), 2021, pp. 1-7.
- [V] M. K. Islam, K. N. Tasnim, S. Choi, "Design of High-Efficiency Ultra-High-Speed Motor for Fuel-Cell Air Compressor Using an Amorphous Stator with Multiphase Winding" 2023 IEEE Applied Power Electronics Conference and Exposition (APEC), 2023.
- [VI] M. K. Islam and S. Choi, "Analysis of Axial Temperature Variation Effect on the Performance of Five-Phase Permanent Magnet assisted Synchronous Reluctance Motor," 2020 IEEE Energy Conversion Congress and Exposition (ECCE), Detroit, MI, USA, 2020, pp. 1107-1114.

A summary of the main scientific contribution of this dissertation is presented below:

- The critical design constraints which limit the output power of UHSM are investigated. These constraints are mathematically derived for integration into the optimization model. Also, their impact on the manufacturing capability and machine performance is presented.
- Several multi-disciplinary analysis modules of HP-UHSM have been developed. It includes electromagnetic, air-friction, structural, thermal, and Rotordynamic analysis module. Then, a new Multiphysics integrated optimization model is established by coupling these analysis modules in a commercial FEA software.
- 3. The impact of multiphase winding on the slotless UHSM is studied. For the first time, a multiphase (six-phase) stator winding has been implemented in the slotless stator of UHSM. Also, the performance comparison between the six-phase and conventional three-phase winding in the HP-UHSM is presented.
- 4. A 2 kW 500,000 rpm HP-UHSM has been optimized for AMEBA system. The machine is prototyped and experimentally tested. This is the highest rated-power machine at 500,000 rpm or more speed. The machine has 94.5% efficiency and no CBR below 11 kHz. A dynamo setup of the proposed HP-UHSM is also developed by using a cascaded rotor.

CHAPTER II

CRITICAL DESIGN CONSTRAINTS OF HIGH-POWER ULTRA-HIGH-SPEED MACHINE

2.1 Introduction

According to [32], the output power of a permanent magnet (PM) machine is directly dependent on the stator's electric loading (A_{stator}) and rotor's magnetic loading (B_{rotor}) as (2.1):

$$P_{out,PM} = A_{stator} \times B_{rotor} \tag{2.1}$$

Considering the design and machine sizing perspective, this equation can be expanded as (2.2):

$$P_{out} = \frac{1}{1+K_{\phi}} \frac{2}{p} m \pi^3 k_w k_t k_p \eta B_g A_t \omega D_g^2 L_e \propto \omega D_g^2 L_e$$
(2.2)

where K_{ϕ} is the ratio of rotor side electric loading to stator side electric loading, *m* is the number of phases, k_w is the winding factor, k_t is the current waveform factor, K_p is the electrical power waveform factor, η is the machine efficiency, B_g is the amplitude of fundamental airgap flux density, A_t is the total electrical loading, ω is the operating speed, *p* is the number of pole pairs, D_g is the average airgap diameter, and L_e is the effective stack length. It is evident that the output power of a PM motor is proportional to the rotor sizing dimensions (especially D_g and L_e). For a given electrical loading, the machine output power can be increased by increasing these rotor outer dimensions. However, for UHSMs (> 400,000 rpm), the output power cannot be increased directly by using this sizing equation unlike a conventional machine. Because, at this speed range, the rotor experiences an excessive centrifugal force, air-friction loss, and bending resonance issue, which introduces several restrictions on increasing D_g and L_e . In addition, the HP-UHSMs also face



Figure 2.1 Conventional PM rotor topologies of permanent magnet machine [33].

several limitations from the material properties and prototyping aspects. This chapter describes eleven critical design constraints or factors that prevent increasing output power in the UHSM.

2.2 Critical Design Constraints of HP-UHSM

2.2.1 Rotor Topology

The commonly used permanent magnet (PM) rotor topologies are shown in Figure 2.1 [33]. The power level of these rotors can be increased using a larger outer rotor diameter and inserting more PM in poles. However, these conventional rotor topologies are not suitable for UHSM. For a rotational speed over 100,000 rpm, a simple and robust rotor geometry is required to mechanically sustain at ultra-high- speed rotation. Figure 2.2 shows a rotor topology used in the literature for low power (LP) UHSM up to 400,000 rpm [34]-[36]. It uses the minimum pole number (p=1) to minimize the high frequency switching loss core losses and switching losses. It has a steel or core shaft at the rotor center and a retaining sleeve is used on the outer surface to protect the PM from scattering. This topology is mechanically rigid and easy to build. Also, its



Figure 2.2 Rotor topology for low-power UHSMs.



Figure 2.3 Impact of inner shaft on the torque development of UHSM at 500,000 rpm.

cylindrical PM produces a pure sinusoidal back-electromotive force (EMF). However, it is not suitable for the HP-UHSM because its center shaft limits the PM usage in the rotor, reducing its rotor's torque density considerably. Figure 2.3 shows that for constant electrical loading and outer



Figure 2.4 Cross-section of the proposed rotor geometry for HP-UHSM.

rotor dimensions, increasing the inner shaft radius significantly decreases the average electromagnetic torque.

To solve this problem, a special rotor topology is adopted in this study, as shown in Figure 2.4. Here, the inner shaft core is completely removed from the rotor center to maximize the PM usage, resulting in a high torque density of the rotor. In addition, this coreless rotor reduces the high-frequency core loss in the rotor. A two-pole cylindrical PM is buried inside the retaining sleeve using the shrink-fit technique. Two separate shaft parts are used on two sides. The axial extension of the sleeve holds these shaft parts. These shaft parts can be installed using the shrink-fit technique or high-strength adhesive. This rotor topology not only increases the torque density of the rotor but also increases the rotor's robustness from the thermal and structural perspective.

2.2.2 Stator Topology

For the stator of HP-UHSM, both the slotted and slotless configurations can be implemented. These two stator configurations have a very different electromagnetic performance like their geometry. Figure 2.5 shows the geometry and key electromagnetic performance comparison of the slotted and slotless stator in a three-phase UHSM. Due to the smaller effective airgap between the PM and stator core, the slotted stator provides a higher electromagnetic interaction between them, resulting in higher average electromagnetic torque. However, the slotted stator causes a significantly higher stator core loss and rotor PM eddy current loss due to its slot harmonics [35]. Also, the slotted stator introduces a considerable pulsation in the electromagnetic torque performance. For safety critical application like AMEBA, neither of these is acceptable. Because the losses will reduce the motor efficiency and increase the coil temperature, whereas the torque pulsation will lead the rotor to unwanted vibration and acoustic noise.

To solve this problem in HP-UHSM, a slotless stator can be used, which does not have any slot harmonic effect. Hence, it drastically reduces the eddy current loss, stator core loss, and torque pulsation. However, the slotless stator has a higher effective air-gap length (Distance between the stator core and rotor PM), which reduces the machine's torque density drastically. As shown in Figure 2.5, for the same volume and input electrical power, the output electromagnetic torque of the slotless stator is 31% lower than the slotted stator. To get the same torque in slotless stator, more electrical loading is needed, which will reduce both the efficiency and power density of the machine. This is one of the major limiting factors of increasing the output power in UHSM.


Figure 2.5 (a) Slotted UHSM geometry, (b) Slotless UHSM geometry, (c) electromagnetic torque of slotted stator, (d) electromagnetic torque of slotless stator, (e) airgap flux density of slotted stator, (f) airgap flux density of slotless stator, (g) core loss of slotted and slotless stator, and (h) rotor eddy current loss.

The portable and safety-critical AMEBA system requires a highly efficient motor with no torque pulsation. By considering these requirements, the slotless stator is selected in this study for the HP-UHSM. Whereas the torque density of the slotless stator is improved by adopting the multiphase winding configuration.

2.2.3 Stator Electric Loading in Slotless Stator

Equation (2.1) states that the output power of a PM is the product of its stator's electric loading (SEL) and rotor's magnetic loading (RML). The SEL of a slotless PM machine is defined (2.3) [32]:

$$A_{stator} = \frac{2 m I_a N_{TP}}{\pi D_g}$$
(2.3)

where *m* is the number of phases, I_a is the per-phase input current, N_{TP} is the number of turnsper-phase, and D_g is the average air-gap diameter. In literature, all UHSMs has a three-phase winding, where the SEL is increased by using a higher I_a and N_{TP} . However, this technique is not suitable for increasing the SEL effectively in the slotless UHSM due to the following reasons:

First, most UHSMs use multistrand Litz coil instead of a single conductor to reduce the eddy current loss on the coil at high-frequency operation. However, when the I_a is increased to improve the SEL, the required parallel strand (N_{PS}) of Litz coil increases exponentially to safely withstand the input current. For a 40-AWG coil, the required N_{PS} for a given I_a is derived as (2.4) from Figure 2.6:



Figure 2.6 Coil diameter (unserved) and peak current carrying capacity vs strand numbers of Litz coil, obtained by using manufacturer data [37].



Figure 2.7 Effective airgap length difference in slotted and slotless stator.

$$N_{PS} = 0.4 \, I_a^2 + 8.6 \, I_a + 3.4 \tag{2.4}$$

Figure 2.6 also shows that increasing N_{PS} increases the nominal diameter (d_s) of the Litz coil considerably. For example, when N_{PS} is increased from 42 to 125 turns, d_s is increased from 0.7 to 1.4 mm. Such an increment in coil diameter will significantly increase the required-slot-area and end-winding length of the phase winding. The slot area of litz coil is calculated as (2.5)[37]:

$$Slot area (S_A) = \frac{\pi d_s^2 N_{TP}}{4k_{SF}} + A_{ins}$$
(2.4)



Figure 2.8 Effective airgap length and average airgap diameter in the slotless stator.

where k_{SF} is the slot fill factor, and A_{ins} is the area of insulating material. Increasing both the slotarea and end-winding length will drastically reduce the PD of the machine, which is calculated by $PD = P_{shaft}/\pi R_5^2 L_e$, where L_e is the effective motor length considering the end-winding and R_5 is the outer stator radius.

Second, the effective airgap length in the slotless stator changes with the SEL. Figure 2.7 shows the slotted and slotless stator of a typical UHSM, where g_m and g_e are the physical and effective airgap lengths, respectively. In slotted stator, $g_e = g_m$ and both the g_e and D_g remain constant for any change of SEL. However, in slotless stator, $g_e \neq g_m$ and both the g_e and D_g change considerably as the SEL varies. Figure 2.8 (a) and (b) show that when the SEL in a slotless stator is increased by using a higher I_a , the required slot area is increased due to the larger coil diameter. As a result, both the effective airgap length and the average airgap diameter are increased from g_e to g'_e and D_g to D'_g respectively. The airgap length has the opposite impact on the output torque development and the average airgap diameter is inversely proportional to the electric

Phase current (I _{rms})	Output Power (W)	Power density (kW/L)	Back-EMF (V _{peak})	Efficiency (%)
3	1170	36	185	92.3
4	1387	32	166	91.9
5	1544	29	152	91.6
6	1727	26	141	91.2
7	1937	24	133	90.7
8	2090	22	126	90.1

Table 2.1 Impact of input current on HP-UHSM, $(N_{TP} = 20)$

Table 2.2 Impact of coil turns on HP-UHSM, $(I_a = 4 \text{ A})$

Phase current	Output Power	Power density	Back-EMF	Efficiency (%)
(I_{rms})	(W)	(kW/L)	(V_{peak})	
20	1012	38	120	91.3
30	1308	32	155	90.0
40	1575	27	185	90.7
50	1806	24	213	90.2
60	2020	21	238	89.3

loading as shown in (2.2). Both of these phenomena will reduce the electromagnetic interaction between the stator core and rotor's PM, resulting in a significant drop in the machine efficiency and torque density. A similar impact will occur when the SEL is increased using a higher N_{TP} in the slotless stator. Table-2.1 and 2.2 present the performance variation of a slotless UHSM when the SEL is increased by applying higher I_a , and N_{TP} , respectively. The simulations are performed at 500,000 r/min. The parametric analysis shows that when the I_a , is increased from 3 A to 8 A ($N_{TP} = 20$), the output power increases from 1170 W to 2090 W. However, the PD and efficiency of the UHSM are decreased by 39% and 2.4%, respectively. Similarly, when the N_{TP} is increased from 20 to 60 ($I_a = 4 A$), the output power reaches 2 kW, but the PD drops from 38 to 21 kW/L and the efficiency decreases from 91.3% to 89.3%. Moreover, the back-EMF increases rapidly as the N_{TP} increases. It is also observed that the output power doesn't increase linearly with the I_a and N_{TP} . Rather, the increment becomes smaller as I_a and N_{TP} increase. Thus, increasing the SEL in a slotless UHSM by using a higher I_a and N_{TP} in the three-phase winding is not effective, because it reduces the machine efficiency and PD significantly.

To solve this critical limitation of UHSM, a multiphase winding in the slotless stator is adopted to increase SEL effectively. If E and I are the phase voltage and current, the input power of a multiphase motor can be written as (2.6) [38]:

$$P_{in} = \sum_{i=1}^{m} E_i I_i$$
 where $m = 5,6,7,9....$ (2.5)

From equation (2.6), it is evident that for a required input power, increasing the phase number allows reducing of the amplitude of input current (I_a) or phase voltage (proportional to N_{TP}). Such a power splitting feature provides an additional degree of freedom to improve electric machine performance with reduced stress on power electronic devices and reduced DC bus ripple. It also increases the slot fill factor in the machine [39]. Figure 2.8(c) shows that when the SEL is increased in a slotless stator by increasing the number of phases, it reduces the I_a for the same N_{TP} , resulting in fewer N_{PS} and lower Litz coil diameter, and eventually the coil requires a smaller slot area compared to Figure 2.8(b). Consequently, both the g_e and D_g are smaller than Figure 2.8(b) and the same as Figure 2.8(a) despite having a higher SEL.

Figure 2.9 presents the performance variation of a slotless UHSM when the SEL is increased by using a higher number of phases. The simulation result shows that the three-phase design can generate a maximum output power of 1350 W at 91.6% efficiency with the specified active volume. Whereas the output power is increased to 1695 W in the six-phase design and 1835 W in the nine-phase design with the same volume. Furthermore, as the phase number increases, the



Figure 2.9 Performance variation of UHSM at different winding configurations. All machines have similar geometry, the same rotor and stator dimensions, and a rated speed of 500,000 r/min.

machine efficiency and PD are also increased significantly with the reduced back-EMF at UHS. However, it is also observed that as the phase number increases, the output power of the studied machine is not increasing at the same rate. The increment of power level is 25.5% from threephase to the six-phase, whereas it is only 8.2% from the six-phase to the nine-phase design. Because the higher slot number and more insulation materials reduce the available slot area of the nine-phase design. Moreover, increasing phase numbers will also increase the switching loss, power electronics cost, and make it difficult to install in a compact UHSM.

Therefore, it is concluded that the optimal multiphase winding technique can effectively increase the electric loading in a slotless stator with higher PD and efficiency, enabling the possibility of designing compact HP-UHSM. In this study, the Multiphysics optimization obtains the optimal multiphase winding of the slotless HP-UHSM.

2.2.4 Air-friction Loss

In the airgap of an electrical machine, there is an interaction between the air and the rotating rotor body. The friction loss on the rotor surface due to this interaction is known as air-friction or airfriction loss (P_f) . This loss mainly depends on the rotor geometry, rotor surface roughness coefficient, and the rotational speed of the rotor. In a low-speed machine, this loss is minimal compared to the other losses, hence is often neglected in the overall loss calculation. However, when the speed of the machine becomes more than 50,000 rpm, the air-friction loss increases considerably [6]. Hence, at a 500,000 rpm machine, the air-friction loss is one of the major limiting factors of increasing the output power. According to the scaling law (1), the power level of UHSM can be increased by enlarging the outer rotor radius (R_2) . However, increasing R_2 of UHS rotor results in an exponential rise of the air-friction loss as $P_f \propto R_2^4$. Figure 2.10 shows the variation of air-friction loss as a function of rotational speed and rotor dimensions. It is observed that the air-friction loss is negligible below 100,000 rpm for all R_2 . But as speed increases above 300,000 rpm, the air-friction loss increases sharply. At 500,000 rpm, this loss becomes significant, even a dominant part of the overall loss, which reduces the motor's efficiency and affects the rotor's axial temperature distribution significantly. Figure 2.11 depicts the axial temperature variation of the cylindrical PM as the air-friction loss changes. As shown, the axial temperature variation of the PM increases significantly as the air-friction loss increases. This asymmetric temperature rise reduces the electromagnetic performance of the machine and creates localized hotspots on the PM. It can also cause uneven magnetization of the PM, resulting in unwanted rotor vibration by increasing the torque ripple. Therefore, this loss must be calculated accurately and restricted during the optimization by considering its impact on both the electromagnetic and thermal performance.



Figure 2.10 Air-friction loss at of the proposed rotor at different speed and outer rotor radius.



Figure 2.11 The impact of air-friction loss on the PM axial temperature variation of the proposed rotor at 500,000 rpm.

2.2.5 Natural Resonance Frequency

The rotor's natural resonance frequency (NRF) is another limiting factor in designing HP-UHSM. As the air-friction loss limits the outer rotor radius (R_2) of UHSM, a sizeable stack length (L) needs to be used to increase its power density. However, increasing the stack length increases



Figure 2.12 Undamped natural resonances of the proposed rotor at different axial length.

the overall rotor axial length (L_t). And increasing the rotor's axial length decreases its natural resonance frequency (ω_{NRF}) exponentially as (2.7) [40]-[41]:

$$\omega_{NRF} \propto \sqrt{\frac{1}{L_t^4}}$$
 (2.6)

Figure 2.12 shows the variation of the 1st, 2nd, and 3rd order NRF of the proposed rotor when its total axial length increases. It is observed that as the rotor axial length increases, the NRF decreases exponentially. An NRF is called the critical bending resonance (CBR) frequency of the rotor when it falls below the rated frequency of the machine. Operating a UHSM at or close to any CBR frequency will lead the rotor to mechanical vibration, structural breakdown, and eventually catastrophic system failure [42]. Furthermore, considering the AMEBA application, this vibration can directly affect the transmitter's modulated signal profile. To avoid these unwanted situations, the CBR frequency of HP-UHSM must be calculated during the geometry optimization and restricted above the rated frequency with an appropriate separation margin (C_{SM}) as (2.8):

$$\omega_{n^{th} CBR} > C_{SM}. \, \omega_m \qquad \text{where } n = 1, 2, 3, \dots$$

where $\omega_{n^{th} CBR}$ is the nth CBR frequent and ω_m is the mechanical rotational speed of the rotor.

2.2.6 Retaining Sleeve Thickness

PMs are strong in compression, but they are brittle and prone to cracking due to lower tensile strength. Therefore, in PM based UHSM, a retaining sleeve is used on the rotor surface to protect the PM from scattering at UHS rotation. In addition, the sleeve thickness has a negative correlation with the rotor stress. Figure 2.13. shows that when the sleeve thickness increases from 0.2 mm to 1.2 mm, the maximum von-mises equivalent stress (VMES) on both the inner sleeve and PM center are decreased significantly. However, enlarging the sleeve thickness increases the outer rotor diameter, which increases the air-friction loss exponentially as discussed in section 2.2.4. On the other hand, when the rotor's outer diameter is fixed, increasing the sleeve thickness will decrease the rotor's torque density drastically by reducing the PM usage in the rotor, as mentioned in section 2.2.1. As a result, the loss-minimizing optimization algorithms (used in the existing design method of UHSM) always search for the smallest sleeve thickness value [43]. However, in the HP-UHSM rotor, a large amount of PM is used to increase the rotor's torque density, which requires enough sleeve thickness to protect the PM; otherwise, the fragile PM will break down and scatter at UHS operation. To avoid this situation, during the optimization of HP-UHSM, the sleeve thickness for each design point must be calculated and restricted above the minimum required sleeve thickness. For the proposed rotor geometry, the minimum required sleeve thickness $(t_{SL,min})$ is calculated by (2.9):

$$t_{SL,min} = \frac{k_{SF} \,\rho_{SL} \,\omega^2 (R_1 + R_2) R_1^2}{64 \sigma_{f_{SL}}} \tag{2.8}$$



Figure 2.13 The variation of VMES on inner sleeve and PM center with the sleeve thickness (at 500 000 r/min and $R_1 = 4$ mm).

Where R_1 is the PM radius, R_2 is the outer rotor radius, C_{SF} is the mechanical safety factor, and $\sigma_{f_{SL}}$ is the flow tensile stress of sleeve material. According to (2.3), the $t_{SL,min}$ is proportional to the third power of R_1 . In practice, the $t_{SL,min}$ can also be constrained by the manufacturing ability [12]. In that case, the minimum thickness limit of the sleeve must be constrained in the optimization.

2.2.7 Material Limitation

2.2.7.1 Permanent Magnet

In electrical machines, both ferrite and rare-earth materials are widely used as the PM. Compared to rare-earth PM, ferrite PM is much cheaper and resistant to demagnetization by outside fields. But a rare-earth magnet of the same size as a ferrite magnet has much higher magnetic field. The UHSM requires a PM with a stronger magnetic field and higher thermal operating point to increase its power density and output power. Therefore, the rear-earth PM materials are the suitable option for the rotor of HP-UHSM. The two commonly used rear earth magnets are Neodymium alloy (*Nd-Fe-B*) and Samarium Cobalt (Sm_2Co_{17}). Table-2.3 shows the

key properties of both PM materials. The *Nd-Fe-B* offers a stronger magnetic field with the highest energy product $(BH)_{max}$ and it is cheaper than the Sm_2Co_{17} . However, the maximum operating temperature of *Nd-Fe-B* is very low, limiting its use in the high-temperature rotor design. On the other hand, the Sm_2Co_{17} has a comparable residual flux density, but a much higher operating temperature than *Nd-Fe-B*.

The operating temperature of HP-UHSM is expected to be high, especially in the rotor due to the smaller surface area and excessive air-friction loss at UHS operation. Hence, considering the high-power density requirement and high-temperature operation of the HP-UHSM, the Sm_2Co_{17} is selected as the PM material in this study. It has a $(BH)_{max}$ of 26 MGOe and a maximum operating temperature of 350 °C.

Quantity	Unit	Nd-Fe-B	<i>Sm</i> ₂ <i>Co</i> ₁₇
Residual flux density	Т	1-1.5	0.8-1.16
Coercivity	MA/m	0.86-2.79	0.49-2.79
Density	kg/m ³	7500	8300
Young's Modulus	GPa	160	104
Poisson's ratio	-	0.24	0.28
Coeff. of thermal expansion	μт/С	7.5	10
Compressive Yield Strength	МРа	780	800
Tensile Strength	МРа	80	120
Maximum Operating Temperature	°C	80-160	300-350

Table 2.3Important properties of the PM materials

2.2.7.2 Retaining Sleeve Materials

In HP-UHSM, a retaining sleeve is used on the rotor surface to provide mechanical support to the PM against its excessive centrifugal force. Usually, a smaller sleeve thickness is recommended to ensure the maximum electromagnetic coupling between the stator core and PM. On the other hand, the thickness and strength of the sleeve should be high enough to withstand PM stress. Hence, the selection of proper sleeve material is crucial. The Non-ferromagnetic material having high tensile strength, good thermal conductivity, and lightweight is the best choice for the sleeve material. Commonly used sleeve materials are titanium alloy, stainless steel, and Inconel alloy. Furthermore, non-metallic wound material such as carbon fiber, glass fiber, and carbon-graphite is also widely used as sleeve material [29]. Table-2.4 shows the key properties of these sleeve materials. As shown, these sleeve materials have very different physical and thermal properties from each other. Hence, the sleeve material must be selected by optimization and based on the Multiphysics performance. In the proposed design method, sleeve material optimization is coupled with the main machine sizing algorithm for optimal sleeve material selection.

Quantity	Units	Carbon fiber	Inconel	Titanium	Stainless
		(strengthen in X)	alloy	alloy	steel
Density	kg/m^3)	1490	8192	4430	7500
Young's Modulus	GPa	121	180	114	190
Poisson's ratio	-	0.4	0.284	0.35	0.265
Coefficient of thermal	μт/С	2.5~4.3	6.5	9.5	10
expansion					
Operating temperature	°C	1500	982	600	950
Tensile Strength	МРа	3220	720	950	505

Table 2.4Key properties of the different sleeve materials

2.2.7.3 Stator Core Materials

In the stator core of UHSM, the magnetic field rotates with very a high frequency, 1833 Hz at 111,000 rpm. Hence, a high-frequency stator core material must be used to reduce the stator core loss at UHS operation. Commonly used stator materials are silicon iron, ferrite iron, amorphous iron, nickel-iron, soft magnetic composites (SMC), and nanocrystalline [12]. Table-2.5 shows their key electromagnetic and thermal properties.

The nanocrystalline has excellent thermal characteristics, magnetic properties, and the lowest specific loss coefficient. However, it is not easily available in required dimensions and shapes. The SMC and silicon iron-based materials have very high magnetic properties with a good temperature profile, but their use is strictly limited especially in the high-frequency application due to the highest specific loss coefficient. Ferrite iron-based material has a lower specific loss coefficient but has the lowest saturation flux density and curie temperature. Amorphous iron-based material has a considerably low core loss coefficient with good magnetic, thermal, and electric properties.

In this study, a ferrite iron-based material named *Epcos-N87*, a silicon iron-based material *50PN595*, and an amorphous iron-based material *Metglas-2605SA1* is considered as possible candidate for stator core material. The optimal stator core material is obtained by using Multiphysics optimization.

Material names	Curie temperature (°C)	Saturation Flux density (T)	Core Loss coefficient (W/cm3)
Nanocrystalline	528	1.32	0.02
SMC	450	2.00	2.82
Silicon-iron	700	1.75	1.88
Ferrite-iron	125	0.39	0.185
Amorphous	390	1.56	0.151

 Table 2.5
 Key electromagnetic and thermal properties of stator materials

2.2.8 Winding Limitations

2.2.8.1 Winding configuration:

Different winding configurations can be implemented in the slotless stator of UHSM [12]-[32]. These are summarized as:





Figure 2.14 End-winding different between the toroidal and airgap winding.

- I. *Trapezoidal self-supporting winding* [44]. It has very small end-winding. But the maximum winding fill factor of this winding configuration is limited to 0.5 only. Also, it requires special equipment, and the manufacturing cost is high.
- II. *Skewed self-supporting winding* [45]. It has the same benefit and drawbacks as trapezoidal self-supporting winding.
- III. Straight winding [46]. It is easy to manufacture and has a winding factor close to 1.However, it has a very high end-winding length, drastically decreasing the machine's power density.
- IV. Toroidal ring-wound winding [47]. It has smaller end-winding and a higher winding fill factor than conventional airgap winding. It also provides better heat extraction capability. However, it increases the machine's outer dimension in the radial direction, reducing the power density of the UHSM.

Figure 2.14 shows the axial end-winding length difference between conventional airgap winding and toroidal winding. It shows that the axial end-winding of the airgap winding is 40%

longer than the toroidal winding. The longer axial end winding not only reduces the machine power density, but also requires a long shaft, which reduces the critical bending frequency of the rotor. Therefore, the toroidal ring-wound winding is considered in this study to reduce the axial endwinding length, increase the winding fill factor, and avoid the prototyping complexity.

2.2.8.2 Winding Pattern:

The UHSMs are designed with a minimum number of rotor poles and stator slots. Increasing the slot number increases the coil resistance and terminal voltage at UHS operation. Hence, the minimum slot number, i.e., two slots per phase configuration is selected for the studied HP-UHSM. There are two possible winding patterns in this configuration. These are distributed winding (DW) and concentrated winding (CW). Figure 2.15 shows the two winding patterns and their key performance differences such as magnetomotive force (MMF) harmonics and output torque.

The CW has lower end-winding, lower copper loss, and is easy to build. But the MMF of CW has a lower fundamental value and higher total harmonic distortion (THD). Consequently, it reduces the output electromagnetic torque and increases the torque ripple significantly. On the other hand, the DW has higher a fundamental MMF value and lower MMF THD, providing a significantly higher electromagnetic torque with a negligible torque ripple. Considering the AMEBA application, full-pitched DW configuration is selected in this design. Because the higher torque ripple of CW will cause mechanical vibration in the rotor, which can directly affect the modulating signal profile of the AMEBA transmitter. For conventional machines, the DW has a drawback of longer end-winding in the axial direction. However, since the HP-UHSM will utilized the toroidal ring-wound winding, the axial end-winding length of the DW will be very smaller.



Figure 2.15 Performance comparison between the concentrated and distributed winding of 2-pole 6-slot machine: (a) concentrated winding (DW), (b) MMF harmonic of DW, (c) distributed winding (CW), and (d) MMF harmonic of CW. The torque performance is measured at 500,000 rpm, and with the same SEL and RML.

2.2.9 Thermal Limitations Constraints

In HP-UHSM, the rotor experiences a high operating temperature due to the excessive airfriction loss on the rotor surface. This rotor temperature rise reduces the PM's maximum energy product $(BH)_{max}$, considerably decreasing the torque density. Also, thermal expansion affects the UHS rotor's radial displacement and stress development. Furthermore, in HP-UHSM, the coil temperature is also critical due to its limited cooling system. The miniature UHSMs often use a natural air-cooling system. Hence, the coil temperature is limited by its thermal insulation limit. If the coil temperature exceeds the thermal insulation limit, the machine will suffer from higher copper loss, short-circuit and eventually thermal breakdown.

Therefore, during the HP-UHSM optimization, both the PM and coil temperature must be calculated and restricted based on their corresponding thermal limit as (2.10)-(2.11):

$$T_{Coil} < T_{coil_limit} \tag{2.9}$$

$$T_{PM} < T_{PM_limit} \tag{2.10}$$

Here, T_{coil_limit} is the temperature limit imposed by the coil's insulation class and T_{PM_limit} is the temperature limit of a magnet at which it provides the maximum energy density $(BH)_{max}$.

2.2.10 Static Interference-fit Limit

An interference fitting is used between the sleeve and PM to secure their rigid assembly with a positive contact pressure (P_c) and ensure proper torque transfer from the PM to a rotor shaft. Also, the stress development on the PM can be controlled by using a higher static interference-fit length (SIFL). The applied SIFL in the proposed rotor is calculated as (2.12):

$$\Delta \delta_o = (R_{pm_o} - R_{sl_i}) \tag{2.11}$$

where R_{pm_o} (= R_1) is the outer PM radius and R_{sl_i} is the inner sleeve radius. For low-torque UHS rotors [26]-[31], the required SIFL is ~ 2 to 7.5 µm, which is easy to implement. Whereas the required SIFL for the rotor of HP-UHSM increases exponentially as the PM radius increases.



Figure 2.16 Required static interference-fit length vs PM radius at 500,000 rpm. The PM and sleeve material is Sm_2Co_{17} and Ti-6Al-4V, respectively.

However, in practice, the maximum possible SIFL for a specific rotor geometry and material is limited by the rotor material's allowable maximum temperature ($T_{SL,max} \& T_{PM,max}$), coefficient of thermal expansion (CTE) (α_{th}), and the PM radius (R_1) as (2.13):

$$\Delta \delta_{o,max} = \alpha_{th} R_1 (T_{SL,max} - T_{PM,max})$$
(2.12)

Figure 2.16 shows the required and possible SIFL of the proposed rotor geometry when the PM radius is increased. Here, the required SIFL is calculated so way that it keeps the stress on PM below its maximum tensile limit and maintains a positive pressure at the contact zone. On the other hand, the possible SIFL is calculated using (13). It is observed that the required SIFL increases exponentially as the PM radius increases, especially after a magnet radius of 3 mm. It is also shown that considering these specific materials, the maximum possible magnet radius is restricted to 4.6 mm. Further increasing the PM radius is not possible due to infeasible SIFL. Therefore, in the HP-UHSM optimization, the SIFL for each design points must be calculated and restricted below their maximum limit as (2.14):



Figure 2.17 Mutual influence of Multiphysics performances of HP-UHSM.

$$\Delta \delta_o < \Delta \delta_{o,max} \tag{2.13}$$

2.2.11 Mutual Influence of Multiphysics Performances

When the output power of UHSM increases, the mutual influences among its Multiphysics performances become critical. For example, enlarging the rotor diameter increases the air-friction loss exponentially and develops excessive centrifugal stress on the rotor materials. The air-friction loss reduces the machine's efficiency and increases rotor temperature significantly. This temperature further affects the torque profile by reducing the PM's flux density and influences the stress development of the rotor by thermal expansion. Secondly, increasing the rotor's stack length reduces its natural resonance frequencies, which can fall below the fundamental operating frequency. Operating a UHSM near or at its natural frequency can cause the rotor to mechanical breakdown. Thirdly, since the UHSM uses a slot-less stator, variation of the stator diameter directly impacts on the machine's efficiency by changing the electromagnetic coupling between the stator and rotor. Also, the machine's power density is inversely proportional to the 2nd power of the outer stator diameter. Such mutual influences become severe as the power of UHSM further increases. To solve this problem in HP-UHSM, the critical mutual influences among Multiphysics performances must be considered during the optimization. To do this, a Multiphysics optimization

model is developed in this study where different physics models are coupled to enable mutual influence among them. Figure 2.17 shows the critical mutual influences, taken into consideration in the proposed design method.

CHAPTER III

MODELING OF PROPOSED MULTI-DISCIPLINARY ANALYSIS MODULES FOR HIGH-POWER ULTRA-HIGH-SPEED MACHINE

3.1 Introduction

This section presents the development of multi-disciplinary analysis modules for HP-UHSM. It includes the electromagnetic, air-friction, thermal, structural, and Rotordynamic analysis modules. The critical design constraints discussed in chapter II are integrated into the corresponding analysis modules.

The finite-element analysis (FEA) is a powerful, accurate, and preferred platform for designing and analyzing the Multiphysics performance of electric motors [48]. However, it is timeconsuming due to the need for high-precision mesh generation [49]. Consequently, a complete FEA-based Multiphysics optimization will require enormous computational power and time due to its highly iterative process and higher number of design variables. This problem becomes even worse if different physics modules are coupled to each other to consider the Multiphysics performance influence for every design point. Therefore, the full FEA-based Multiphysics optimization of HP-UHSM is a challenge considering the design time and computational cost. To solve this problem, a hybrid optimization model is developed in this dissertation that uses both the FEA and analytical models. In the following sub-sections, the development of these models is explained in detail.

3.2 Electromagnetic Analysis Model

In HP-UHSM, the main challenge of electromagnetic design is obtaining an effective SEL in the slotless stator and accurately estimating electromagnetic losses by including the thermoelectrical influences. The former affects efficiency, and the latter ensures thermal safety. Hence, in the proposed design method, an FEA-based electromagnetic analysis module is developed for the electromagnetic analysis, where the multiphase winding is adopted for effective SEL increment in the slotless stator. The electromagnetic performance and losses are calculated by integrating the temperature effect on the PM and coil. Figure 3.1 shows the 3D and 2D cross-section geometry of the proposed HP-UHSM. It has a coreless rotor and slotless stator. In the rotor, the PM is buried inside a hollow cylinder sleeve and the axial extension of the sleeve will form the shaft. In the stator, multi-strands Litz wire is used for developing the toroidal winding.

Figure 3.2 shows the simplified parametric electromagnetic analysis model of the proposed HP-UHSM, developed in the ANSYS 2D Maxwell. This model has sixteen motor-sizing parameters, including the input phase current (I_a), number of phases (m), number of turns per phase (N_c), and geometry dimensions. The main output parameters are the average torque (T_e), stator peak flux density ($B_{p,s}$), stator current density (J_{den}), electromagnetic losses, and power density (PD) considering end windings. The output torque is defined as the average torque of one (last) mechanical revolution. The torque ripple is defined as (3.1):

$$T_{ripple} = \frac{T_{e,max} - T_{e,min}}{T_{e,avg}}$$
(3.1)

The peak flux density on arc line at the last simulation time is defined as core flux density. The current density is calculated as (3.2):

$$J_{den} = \frac{N_{TP} I_a(RMS)}{K_{cu} K_{SF} A_s}$$
(3.2)



Figure 3.1 2D and 3D cross section of proposed HP-UHSM geometry.



Figure 3.2 Parametric FEA electromagnetic Model of the HP-UHSM: (a) 1/8th of radial cross-section, and (b) 1/2th of the axial cross-section.

where K_{cu} is the copper fill factor (ratio of the copper area of the strands in the wire to the area of the wire), K_{SF} is the slot fill factor (ratio of the area occupied by wire to the total cross-section area of the slot), and A_s is the slot-area. The slot area is calculated using (2.5).

In HP-UHSM, the temperature variation significantly affects the coil's conductivity and PM's energy density. To consider these effects during optimization, the temperature dependency of the coil and PM is defined as (3.3)-(3.4):

$$\sigma(T) = 1/\{1 + \lambda(T_{wind} - 25^{o})\}$$
(3.3)

$$(T) = 1 - \alpha (T_{pm} - 25^{\circ}) \& H(T) = 1 - \beta (T_{pm} - 25^{\circ})$$
(3.4)

where T_{wind} and T_{pm} are the maximum temperatures of coil and PM, $\mu(T)$ and H(T) are the temperature-dependent relative permeability and magnetic coercivity of the PM, λ is the temperature coefficient of copper resistivity, α and β are the temperature coefficients of PM.

At UHS operation, the windings experience a proximity loss ($P_{Cu,prox}$) due to eddy currents induced by the magnetic field and a conduction loss ($P_{Cu,cond}$) due to the DC resistance and skin effect of the coil. In the conventional low-power design methods, an analytical electromagnetic model is used, where it is assumed that the stator winding has a negligible impact on the magnetic field generation. However, the HP-UHSM utilizes a higher SEL using multiphase winding; hence its electric loading has a considerable contribution to the magnetic field generation, which must be modeled in the loss calculation, especially the copper proximity loss. In the proposed model, the proximity loss of the HP-UHSM is calculated using the magnetic field produced by both PM and input electrical loading as (3.5):

$$P_{Cu,prox} = \frac{(2\pi f)^2 d_s^2 B_{p,cu}^2 \pi \left(L + 2L_{ew}\right) \left[R_{si}^2 - \left(R_{SL,o} + g_m\right)^2\right]}{32 \rho_{cu}}$$
(3.5)

where $B_{p,cu}$ is the peak flux density on the coils contributed by both PM and SEL, *L*, L_{ew} , R_{si} , $R_{SL,o} (= R_2)$, and g_m are the stack length, end-winding length, inner stator radius, outer sleeve radius, and airgap length, *f* is the electrical frequency, and ρ_{cu} is the copper resistivity. The multi-stranded Litz wire is considered for the winding, which has a negligible skin effect at high frequency. Hence, the conduction loss of the HP-UHSM is calculated using (3.6)-(3.7)

$$P_{Cu,cond} \approx m I_a^2 R_{dc,p} \tag{3.6}$$

$$R_{dc,p} = \frac{4\rho_{cu}l_{cu}}{n \pi d_s^2} \left\{ 1 + \alpha_{cu}(T_{wind} - T_o) \right\}$$
(3.7)

where $R_{dc,p}$ is the phase DC resistance, *n* is the number of parallel Litz strands or conductors, l_{cu} is the total conductor length, α_{cu} is the copper conductivity, T_{wind} is the winding temperature, and T_o is the room temperature (25 °C). The stator core loss is calculated by using the modified Bertotti iron loss separation model, which is defined in the ANSYS Maxwell as (3.8):

$$P_{iron} = P_{hy} + P_{ed} + P_{ex} = = \left(k_h f B_{p,s}^2 + k_c f^2 B_{p,s}^2 + k_e \left(f B_{p,s}\right)^{1.5}\right) V_s$$
(3.8)

where V_s is the stator volume, P_{hy} , P_{ed} , and P_{ex} are the hysteresis loss, eddy current loss, and excess loss, respectively. k_h , k_c , and k_e are their corresponding loss coefficients calculated using the stator core material manufacturer data. The power loss in the coreless rotor is mainly due to the eddy currents generated in the sleeve and PM, calculated as (3.9):

$$P_{rotor} = \int_{V} \sigma E^2 dV_r \tag{3.9}$$

where σ is the material conductivity, *E* is the electric field applied to the rotor, and *V_r* is the effective rotor volume. The machine's power density (PD) is calculated as the ratio of shaft output power divided by the active volume of the motor. It is defined as (3.10):

$$PD = \frac{P_{shaft}}{V_{motor}} = \frac{2\pi \times T_e(Nm) \times \omega_m(rpm)}{60 \times 1000 \times \pi R_5^2(L + 2L_{ew})}$$
(3.10)

For stator winding, different multiphase configurations are considered. The phasor diagrams of commonly used multiphase winding topologies are shown in Figure 3.3 [39],[50]-[54]. The five-phase and seven-phase configurations use unconventional winding setup, where all phases are equally shifted by an electrical displacement angle (γ_n) . $\gamma_n = 360^o/n$, where *n* is 5 or 7. These configurations are suitable for higher fault-tolerant capability and reducing the electromagnetic force harmonic. However, the required power electronics for these configurations, such as five-phase and seven-phase inverter are not commercially available yet. Moreover, a complex control algorithm and high computational power are needed to control these machines. On the other hand, the six-phase or nine-phase configurations consist of two (A1-B1-C1 and A2-B2-C2) or three (A1-B1-C1, A2-B2-C2, and A3-B3-C3) conventional three-phase winding sets. For a balanced system, $\alpha = \beta = 120^{\circ}$, where α is the phase shift between phase A1 and B1; and β is the phase shift between phase A1 and C1. The three-phase winding sets are separated by a displacement angle (θ), which can be any integer or fractional number from 0 to 360°, based on the available pole/slot numbers. These configurations can be easily driven by utilizing two or three conventional three-phase inverters. Also, they have the flexibility of the "optimal-phaseselection", for example, a nine-phase machine can be operated as either three, six, or nine-phase based on the required power and optimal loading condition [55]. The optimal multiphase winding



Figure 3.3 Phasor diagram of different multiphase winding configurations.

is obtained in the multi-physics optimization by considering the efficiency, power density, DClink voltage, and global design margin.

3.3 Air-friction Loss Analysis Model

In the HP-UHSM, the air-friction loss is the most dominant part of the total losses at UHS rotation (especially > 400,000 rpm). For the proposed machine geometry, the air-friction loss is calculated as (3.11):

$$P_{air-friction} = P_f = \pi C_r C_d \omega^3 R_{SL,o}^4 l_r \rho_{air_T_a}$$
(3.11)

where $\rho_{air_T_a}$ is the air density at the airgap temperature T_a , ω is the mechanical rotation speed, and $l_r = (L + 2L_2)$ is the rotor axial length exposed to the airgap. C_r is the roughness coefficient of the rotor surface. $C_r = 1$ for a smooth rotor surface. C_d is the drag coefficient, which depends on the rotor geometry and airflow behavior. The airflow behavior of the studied rotor is determined using the Taylor number (*Ta*), defined as (3.12) [56]:



Figure 3.4 Airgap air model for 3D CFD analysis.

$$Ta = \frac{g_m \omega \rho_{air} R_{SL,o}}{\mu_{air}} \sqrt{\frac{g_m}{R_{SL,o}}}$$
(3.12)

where μ_{air} is the dynamic viscosity of air. According to [56], [6], the airflow of the studied rotor at 500,000 rpm is always turbulent even with the minimum $R_{SL,o}$. At this condition, the air-friction loss increases dramatically and the calculation of C_d becomes critical. In low-power UHSMs, the C_d is estimated by using the empirical equation based on the Taylor number (*Ta*) and the Reynold number (*Re*) as (3.13):

$$C_d \propto T a^{-0.2}$$
 and $C_d \propto R e^{-0.5}$ (3.13)

However, the accuracy of these constant equations drops considerably in an HP-UHSM due to a higher L/D and the variation of thermo-physical properties of the confined airgap air [28].

To solve this issue, a computational fluid dynamic (CFD) analysis is developed in the proposed model to calculate the C_d variation during the optimization. In the 3-D CFD analysis, the

rotor geometry, air-gap surfaces, and air-flow behavior can be realized as the designed and installed condition, resulting in the accurate calculation of air-friction loss. Figure 3.4 shows the CFD model of an airgap air in the ANSYS fluent. In this model, the C_d is defined as (3.14):

$$C_{d} = \frac{Shear \ stress \ on \ rotor \ surface}{Dynamic \ Pressure} = \int (\omega, R_{SL,o}, g_{m}, l_{r}, C_{r}, T_{a})$$
(3.14)

In this design, g_m , C_r , and ω are fixed considering the studied AMEBA system requirements. Hence, the C_d is calculated using a parametric CFD analysis for different values of L/D rotor ratio and air temperature (T_a), which is then used as a look-up table in the air-friction loss calculation.

3.4 Thermal Analysis Model

The power-dense HP-UHSM experiences high operating temperature at UHS due to its high loss per unit area and limited cooling system. But the maximum machine temperature is restricted by the desire energy product (BH_{max}) of rotor PM and the insulation type of stator coils. Furthermore, the temperature variation influences the machine performance, such as structural deformation and DC phase resistance. Therefore, the machine temperature must be calculated in the HP-UHSM optimization and observed its impact on other performances. To do that, a lumped parameter thermal network (LPTN) of the full machine is developed and integrated into the optimization model. Figure 3.5 shows the full motor assembly, and Figure 3.6 shows the simplified LPTN of the proposed HP-UHSM. Unlike conventional LPTM, it provides the temperature variation of the machine in both the radial and axial directions.



Figure 3.5 3D view of the HP-UHSM with casing.



Figure 3.6 Simplified lumped parameter thermal network (LPTN) of proposed HP-UHSM.

In the LPTM model, the current sources represent the heat generation source, estimated from the corresponding losses. The voltage source represents the ambient temperature point. The voltage obtained from the electrical network is equivalent to the average steady-state temperature at the corresponding node. In general, heat can be transferred by conduction, convection and



Figure 3.7 Fourier's law of thermal conduction for a plane wall body.

radiation process. However, in an electric machine, most of the heat transfers by means of conduction and convection. On the other hand, the radiation heat transfer is negligible; hence it is ignored in this case. The losses are imported from the electromagnetic and air-friction loss model. The bearing loss mainly depends on the bearing type, reloading, application, and rotational speed of the rotor. In this case, two ball bearings are considered, and their power loss is estimated using the empirical model as (3.15), validated in [57]:

$$P_{bearing} = c_4 \left(\frac{Rotating\ frequency}{1\ Hz}\right)^{C_5}$$
(3.15)

where c_4 and c_5 are two empirical constants. The equivalent convective and conduction thermal resistances are calculated using Newton's law of cooling and Fourier's law of conduction found in [28]-[60].

Figure 3.7 shows a plane wall body of cross-sectional area A and uniform thickness Δx . If the uniform temperature of two side of the wall are T_1 and T_2 , where $T_1 > T_2$, then the heat flow (*q*) through the wall can be expressed as (3.16):

$$q = -\lambda A \frac{dT}{dx} = -\frac{\lambda A}{\Delta x} (T_2 - T_1)$$
(3.16)



Figure 3.8 Fourier's law of thermal conduction for a cylindrical body.



Figure 3.9 Newton's law of cooling.

The equivalent conductive thermal resistance of the wall body (R_{th}) can be obtained as (3.17):

$$R_{th} = \frac{\Delta x}{\lambda A} \tag{3.17}$$

Figure 3.8 shows a cylindrical body length *L*, inner radii r_{in} , outer radii r_{out} , the corresponding temperature T_{in} and T_{out} , where $T_{in} > T_{out}$. The equivalent conductive thermal resistance of this cylindrical body can be expressed as (3.18):

$$R_{th} = \frac{\ln\left(\frac{r_{out}}{r_{in}}\right)}{2\pi\lambda L} \tag{3.18}$$

Newton's Law of cooling states that the rate of heat loss from a body to its surroundings is directly proportional to the temperature difference between the body and its surroundings. Figure 3.9 shows a body of surface, which has a uniform surface temperature of T_{I} , and its surrounding's fluid temperature is T_A ($T_I > T_A$). If h is the heat transfer coefficient, then the equivalent thermal convective resistance is calculated as (3.19):

$$R_{th} = \frac{1}{hA} \tag{3.19}$$

The heat transfer coefficients (HTC) are calculated using the machine geometry, materials properties, and rotational speed. The HTC on the airgap rotor surface is calculated as (3.20) [60]:

$$h_{air-gap} = Nu * \frac{\rho_{air}}{2 \times g_m}$$
(3.20)

where Nu is the Nusselt number, defined as (3.21) [58]:

$$Nu = \begin{cases} 2 & ; \ 0 < Ta < 1740 \\ 0.409 \ T_a^{0.241} - 137Ta^{-0.75} & ; \ Ta \ge 1740 \end{cases}$$
(3.21)

with Ta is the Taylor number (3.22):

$$Ta = \frac{\omega^2 r_{ag} g_m^3}{v_{air}^2 C_r} \tag{3.22}$$

Where, $r_{ag} = R_2 + \frac{g_m}{2}$ is the average airgap radius, C_r is the rotor roughness factor, and v_{air} is the kinetic viscosity of air.

The HTC of non-airgap rotor surface is calculated using the Nusselt number (Re) and Reynold number as (3.23)-(3.24):

$$h_{non-air-gap} = \frac{Nu \times \rho_{air}}{R_s}$$
(3.23)

$$Nu = 0.0628 \times Re^{0.7}$$
, and $Re = \frac{2\pi\omega R_s^2}{60 v_{air}}$, $R_s = \text{shaft radius}$ (3.24)

The heat transfer coefficient on the non-airgap rotor surface can be calculated using the empirical formula given by (3.25) [61]:

$$h_{ERF} = 28(1 + \sqrt{0.45\omega_r}) \tag{3.25}$$

In this LPTM, the machine's radial temperatures are presented by ten nodal points (T_1 to T_{10}). The model is solved simultaneously using a matrix of 10 nodes. For node *i*, the nodal equation is written as (3.26):

$$T_i \sum_{j=1}^{n} \frac{1}{R_{i-j}} + \sum_{j=1}^{n} \frac{-T_j}{R_{i-j}} = P_i \qquad i, j = 1, 2, \dots, n$$
(3.26)

here R_{i-j} is the equivalent resistance between *i* and *j* nodes, *n* is the adjacent node, P_i and T_i are the corresponding loss and temperature at the *i*th node, respectively.

The axial temperature of the thermally shorted element such as the PM is calculated using the finite volume method (FVM), as shown in Figure 3.10. If the PM has a uniform body, a crosssectional area of *A* and axial length of *L*, with an axial temperature variation of T(x), where 0 < x < L. In FVM, the heat conduction of element dx can be represented by using a second-order differential equation as (3.27) [60]:

$$I\frac{\Delta^2 T}{\Delta x^2} + \frac{1}{\lambda A} \left[\sum_a \frac{T_a(i) - T(i)}{R'_a} + \sum_b \frac{T_b - T(i)}{R'_b} \right] + \frac{\dot{q}}{\lambda} = 0$$
(3.27)

here λ is the PM thermal conductivity, \dot{q} is the heat generated by PM volume. $T_a(i)$ corresponds to all adjacent nodes, which are a function of x. T_b corresponds to all adjacent single nodes. R'_a and R'_b are the axial length thermal resistance between $T_a(i)$ and T(i), and $T_a(i)$ and T_b .


Figure 3.10 A PM of axial length L, with temperature variation as a function of x, where (0 < x < L).



Figure 3.11 PM segmentation into *k* nodes along the axial direction.

In this study, the PM is segmented into *k* equal volume of Δx , as shown in Figure 3.11. Each segment is represented as a sub-node where $\Delta x = L/k$. The temperature at sub-node *i* (for $1 \le i \le k$) is $T_i = T(x = x_i)$ where $x_i = \left\{\frac{i-1}{2}\right\}L$. The boundary nodes B_o and B_L are define at x = 0 and x = L. The temperature equation for conduction heat of interior nodes $(2 \le i \le k - 1)$ are defined as (3.28):

$$\frac{T_{i+1} + T_{i-1} - 2T_i}{\Delta x^2} + \frac{1}{\lambda A} \left[\sum_a \frac{T_{a,i} - T_i}{R'_a} + \sum_b \frac{T_b - T_i}{R'_b} \right] + \frac{\dot{q}}{\lambda} = 0$$
(3.28)

where $T_{a,i}$ is the temperate at node I for the adjacent node $T_{a,i}(x)$. As, the node 1 and *n* are located at a distance of dx/2 from the boundary B_o and B_L , the corresponding nodal equation is:

$$\frac{T_{i+1} + T_{i-1} - 3T_i}{\Delta x^2} + \frac{1}{\lambda A} \left[\sum_a \frac{T_{a,i} - T_i}{R'_a} + \sum_b \frac{T_b - T_i}{R'_b} \right] + \frac{\dot{q}}{\lambda} = 0$$
(3.29)

For a constant boundary temperature T, the nodal equation is written as (3.30):

$$T_{B_0} = T \text{ or } T_{B_L} = T \tag{3.30}$$

For a boundary node along the axis of symmetry, the temperature equation is:

$$T_{B_0} + (-1)T_1 = 0 \text{ and } (-1)T_n + T_{B_L}$$
 (3.31)

For a given node, the equation (3.28) to (3.31) form a matrix of simultaneous equation as (3.32):

$$\begin{bmatrix} a_{B_{o}} & -b_{B_{o}} & \dots & 0 & 0 \\ -c_{1} & a_{1} & \dots & \dots & 0 \\ \vdots & \ddots & \vdots & \vdots \\ 0 & \dots & \dots & a_{n} & -b_{n} \\ 0 & 0 & & -c_{B_{L}} & a_{B_{L}} \end{bmatrix} \begin{bmatrix} T_{B_{o}} \\ T_{1} \\ \vdots \\ T_{n} \\ T_{B_{L}} \end{bmatrix} = \begin{bmatrix} d_{B_{o}} \\ d_{1} \\ \vdots \\ d_{n} \\ d_{B_{L}} \end{bmatrix}$$
(3.32)

where a_n , b_n , c_n , and d_n are the node coefficients. This is a tridiagonal matrix. Hence, the tridiagonal matrix algorithm (TDMA) employing Gauss-Seidel iteration is applied to solve (3.32).

The temperature of each node can be constrained as a thermal limit and transferred to the other physics models. Node T_4 and T_6 are the winding and the PM temperature, which can be transferred to other physics models as well as constrained as a thermal aspect, such as (3.33):

$$T_{4} < T_{coil,Limit} \quad \text{(Limited by the coil insulation type)}$$

$$T_{6} < T_{PM,Limit} \quad \text{(Considering a desire } (BH)_{max})$$

$$(3.33)$$



Figure 3.12 Temperature impact on the 500,000 rpm rotor at different R_{PM} .



Figure 3.13 (a) Stress analysis model and (b) yielding criteria of the proposed HP-UHSM.

3.5 Structural Analysis Model

The mechanical stress of the UHS rotor due to its excessive centrifugal force is one of the major limiting factors in HP-UHSM design. The rear-earth PM is a fragile material and has a low tensile strength. Although a high strength retaining sleeve is used on the rotor surface to protect the PM, there must be a suitable safety margin for the developed stress to ensure a reliable operation at UHS. In addition, the stress development and deformation of the HP-UHSM's rotor

are considerably influenced by its operating temperature. Figure 3.12 shows the temperature impact on the PM stress and the structural safety factor (SF) of UHSM at different PM radius (RPM). As shown, a 50 °C temperature rise can decrease around 5% structural SF of the UHS rotor by increasing its critical PM stress. It is also observed that when the power of the UHSM increases by enlarging the PM radius, the temperature influence on PM stress becomes more significant and causes a drastic drop in the structural SF. Avoiding such stress increment phenomena in the fragile PM can lead an HP-UHSM to unwanted structural breakdown at the UHS operation. Therefore, unlike conventional models, the temperature effect is integrated into the proposed structural analysis model to consider the critical thermo-physical influence during the optimization. Also, several practical constraints considering the material and manufacturing aspects are defined to ensure a feasible and robust high-power rotor development.

Figure 3.13(a) shows the proposed 2D structural analytical model, developed using a rotating disk's stress equilibrium theory. The proposed rotor has three primary sources of mechanical stress: (i) contact pressure due to interference fitting, (ii) centrifugal force due to UHS rotation, and (iii) thermal expansion due to operating temperature.

3.5.1 Contact Pressure Due to Shrink-fit:

The shrink-fit technique is used to implement the interference fitting between the PM and sleeve. A possible method to shrink-fit between the PM and sleeve is to heat up the sleeve and cool down the PM. Then, install the PM into the sleeve, which has a smaller inner radius than the outer PM radius. At standstill, the applied SIFL ($\Delta \delta_o$) and the contact pressure between the PM and sleeve (p_c standstill) is calculated as (3.34) and (3.35):

$$\Delta \delta_o = R_{pm,o} - R_{sl,i} \tag{3.34}$$

$$p_{c_standstill} = \frac{\Delta \delta_o E_m E_s (R_2^2 - R_1^2)}{E_s [(R_1^3 - R_1 R_2^2) v_m - R_1^3 + R_1 R_2^2] + E_m [(R_1 R_2^2 - R_1^3) + R_1^3 + R_1 R_2^2]}$$
(3.35)

where $R_{pm,o}$ is the outer magnet radius and $R_{sl,i}$ is the sleeve inner radius. At the rotating condition, the dynamic shrink-fit length can be determined by (3.36), and the developed dynamic contact pressure at the interference-fit zone is calculated by using (3.37):

$$\Delta\delta = u_m + u_s - \Delta\delta_o \tag{3.36}$$

$$p_{c_dynamic} = \frac{\Delta \delta E_m E_s (R_2^2 - R_1^2)}{E_s [(R_1^3 - R_1 R_2^2) v_m - R_1^3 + R_1 R_2^2] + E_m [(R_1 R_2^2 - R_1^3) + R_1^3 + R_1 R_2^2]}$$
(3.37)

3.5.2 Centrifugal Stress Due to Rotation

During the rotating condition, the radial (σ_r) and tangential (σ_t) stress developed in the UHS rotor is derived using the static equilibrium equation [62] [12], which converts the dynamic disc problem into a steady-state equilibrium as (3.38):

$$\frac{\partial \sigma_r}{\partial r} + \frac{1}{r}(\sigma_r - \sigma_t) + \rho r \omega^2 = 0$$
(3.38)

where *r* is the distance from the center, ρ is the material density, and ω is the angular velocity. Applying the strain-displacement relation (3.39), and Hooke's law (3.40) into (3.41), leads to the non-homogeneous differential equation as (3.42):

$$\epsilon_r = \frac{\partial u}{\partial r} \quad and \quad \epsilon_t = \frac{u}{r}$$
 (3.39)

$$\epsilon_r = \frac{(\sigma_r - \nu \sigma_t)}{E} \quad and \quad \epsilon_t = \frac{(\sigma_t - \nu \sigma_r)}{E}$$
(3.40)

$$\frac{\partial^2 u}{\partial r^2} + \frac{1}{r}\frac{du}{dr} - \frac{1}{r}u + \frac{(1-v^2)}{E}\rho r\omega^2 = 0$$
(3.41)

where u is the radial displacement. The solution of the (3.41) is written as (3.42):

$$u = \frac{1}{E} \left(k_1 r (1 - v) - k_2 \frac{1}{r} (1 + v) - \frac{1 - v^2}{8} \rho r^3 \omega^2 \right)$$
(3.42)

where k_1 , and k_2 are the constants. Now, by using inverted Hooke's law and strain-displacement relation, the σ_r and σ_t developed on the rotating rotor are derived as (3.43):

$$\sigma_{r} = k_{1} + k_{2} \frac{1}{r} - \frac{3 + v}{8} \rho r^{2} \omega^{2}$$

$$\sigma_{t} = k_{1} - k_{2} \frac{1}{r^{2}} - \frac{1 + 3v}{8} \rho r^{2} \omega^{2}$$
(3.43)

3.5.3 Thermal Expansion Due to Temperature

As mentioned earlier, when the rotor is running at UHS, the rotor temperature rises considerably due to the excessive air-friction loss. Since the PM and sleeve material have a positive CTE, their mechanical displacement will be affected by this rising temperature, which also contributes to the stress development on the rotor. The thermal expansion of i^{th} material (u_i) due to the temperature rise is calculated by using (3.44):

$$u_i = \alpha_{\text{th}_i} r \left(T_{max_i} - T_o \right) \tag{3.44}$$

where , α_{th} is the thermal expansion coefficient and T_{max_i} is the maximum temperature of i = PM or Sleeve, T_o is the room the temperature.

Finally, the total radial displacement and mechanical stress on PM and sleeve due to the shrink-fit pressure, rotational speed, and temperature are calculated simultaneously as a function of r as (3.45):

$$u_{i}(r) = \frac{1}{E_{i}} \left(k_{1,i}r(1-v_{i}) - k_{2,i}\frac{1}{r}(1+v_{i}) - \frac{1-v_{i}^{2}}{8}\rho_{i}r^{3}\omega^{2} \right) + \alpha_{th,i}r(T_{max,i} - T_{o})$$

$$\sigma_{r,i}(r) = k_{1,i} + k_{2,i}\frac{1}{r} - \frac{3+v_{i}}{8}\rho_{i}r^{2}\omega^{2} + \alpha_{th,i}E_{i}(T_{i} - T_{o})$$

$$\sigma_{t_{i}}(r) = k_{1,i} - k_{2,i}\frac{1}{r^{2}} - \frac{1+3v_{i}}{8}\rho_{i}r^{2}\omega^{2} + \alpha_{th,i}E_{i}(T_{i} - T_{o})$$

$$(3.45)$$

There are two sets of equations for (3.45), one for PM and another for a sleeve. Hence in total ten unknown variables: u_{PM} , u_{SL} , $\sigma_{r,PM}$, $\sigma_{r,SL}$, $\sigma_{t,PM}$, $\sigma_{r,PM}$, $k_{1,PM}$, $k_{2,PM}$, $k_{1,SL}$, $k_{2,SL}$. The four constants ($k_{1,PM}$, $k_{2,PM}$, $k_{1,SL}$, $k_{2,SL}$) are calculated four the geometrical boundary conditions as:

(a) The solid cylindrical PM is buried inside the sleeve. Hence the PM expansion in the center is zero:

$$u_{PM} (r = 0) = 0 \tag{3.46}$$

(b) The shrink-fit technique has been considered between the PM and sleeve. Hence the expansion difference between them is equal to the dynamic shrink-fit length:

$$u_{SL}(r = R_{PM}) - u_{PM}(r = R_{PM}) = \Delta\delta$$
(3.47)

(c) For the similar reason of (b), the mechanical stress in the radial direction of both PM and sleeve are the same at the interference radius:

$$\sigma_{r,PM}(r = R_{PM}) = \sigma_{r,SL}(r = R_{PM}) \tag{3.48}$$

(d) At the outer edge of the sleeve the radial stress is zero:

$$\sigma_{r,SL}(r = R_2) = 0 \tag{3.49}$$

The ten unknown variables and ten equations are solved for unknown variables. A MATLAB script is written to solve these second-order equations. The other practical constraints are defined as follows:

$$\Delta \delta = \Delta \delta + R_{PM} C_{Tem}$$

$$\Delta \delta_o < \Delta \delta_{o,max} \text{ and } t_{SL} > t_{SL,min}$$

$$P_c > 0 MPa \ (0 \le \omega \le \omega_{max}, 0 \le T_i \le T_{max,i})$$

$$(3.50)$$

where $C_{Tem} = (\alpha_{th,PM} - \alpha_{th,SL}) (T_{i,max} - T_o)$. The $t_{SL,min}$ and $\Delta \delta_{o,max}$ are defined as (9), and (13) respectively.

To find the yield measure, the Von-Mises yield criterion (VMYC) is commonly used for ductile material (Sleeve). Whereas the VMYC or maximal principal sheer theory can be used for brittle material (PM), as shown in Figure 3.13(b). In this model, the von-mises equivalent stresses (VMES) and the safety margins (S_m) of both materials are calculated as (3.51):

$$VMES_{i} = \sqrt{\left(\sigma_{r,i} - \sigma_{t,i}\right)^{2} + \sigma_{r,i} \cdot \sigma_{t,i}}$$

$$S_{m,i} = \frac{UTS_{i} - VMES_{i}}{VMES_{i}}$$

$$(3.51)$$

3.6 Rotordynamic Analysis Model

The UHS rotor uses a high L/D ratio, and it is supported by miniature bearings with a lower stiffness value. Hence, the UHS rotor experiences several natural frequencies of different mode shapes such as lateral mode, conical mode, 1st order bending mode, 2nd order bending mode and so on, as shown in Figure 3.14. In HP-UHSM, any CBR frequency should be avoided before the fundamental operating frequency because it directly affects the system dynamics and drastically limits the system's operating speed region. For example, one CBR frequency below the rated speed can limit ~30% operating bandwidth of the AMEBA system. Operating the AMEBA motor near any CBR frequency will lead the antenna to extensive vibration, directly affecting the transmitting signal profile. Therefore, it is strongly recommended to have an appropriate separation margin, at-least 20 to 30%, between the CBR frequencies and the fundamental operating frequency of the HP-UHSM [63]-[65]. To do that, a Rotordynamic model must be developed to calculate the CBR frequencies of the HP-UHS rotor and restrict them above the rated frequency during the optimization.



Figure 3.14 Different mode shapes of UHS rotor.

At the design stage, accurate calculation of CBR frequencies is very difficult because they are highly manipulated by the rotor's guide-bearing stiffness and installation process. Therefore, in this proposed design method, the undamped natural frequency (UNF) of the rotor is used as an indicator of the actual CBR frequency. This consideration is rational because the actual CBR will be higher than the UNF due to the bearing damping. In the traditional turbo-machinery system, a simplified beam shaft geometry is often used to estimate the UNF [16]. However, the proposed rotor has a special coreless geometry, and the physical properties of its materials are quite different. Therefore, the UNF model is derived using the actual rotor geometry, material properties, and boundary conditions.



Figure 3.15 Rotor geometry for Rotordynamic model: (1/2th cross-section).

The UNF (ω_n) of a cylindrical beam shaft made from a single material can be calculated using the Rayleigh-Ritz equation as (3.52) [66]:

$$\omega_n = \alpha_n \sqrt{\frac{Er^2}{64\rho L^4}} \tag{3.52}$$

where *E* is Young's modulus, ρ is the mass density, r_s is the radius and L_s is the length of the shaft, and α_n is a series constant of n_{th} natural frequency depends on the shaft's material properties and boundary conditions. By assuming a uniform joint between the PM and sleeve, the equation (3.52) can be modified to calculate the UNF of the proposed rotor geometry as (3.53):

$$\omega_n = \sqrt{\frac{(1/512)a_n^2 (E_{SL} + E_{PM}) R_{SL,o}^4}{\left[\rho_{SL} \left\{ R_s^2 L_1^4 + R_s^2 L_3^4 + 2R_{SL,o}^2 L_2^4 + \left(R_{SL,o} - R_{PM} \right)^2 L^4 \right\} + \rho_{PM} R_{PM}^2 L^4 \right]}$$
(3.53)

where ω_n is the n_{th} UNF. $R_1 R_2$, R_s , L, L_1 , L_2 , and L_3 are rotor dimensions as shown in Figure 3.15. ρ_{SL} and ρ_{PM} are the mass density of the sleeve and PM material, respectively. E_{SL} and E_{PM} are the Young's modulus of the sleeve and PM materials respectively. In the optimization model, the 1st UNF (ω_1) is calculated and restricted above the fundamental frequency with the desire safety margin. The series constant α_n of the 1st-order bending frequency is calculated from the FEA using ANSYS modal simulation.

CHAPTER IV

MULTIPHYSICS DESIGN OF HIGH-POWER ULTRA-HIGH-SPEED MACHINE

4.1 Introduction

The design process of UHSM is highly iterative and critical from a Multiphysics perspective. It becomes more critical when the output power requirement is increased at UHS. In HP-UHSM, the design parameters are highly sensitive to multi-disciplinary performances and material limitations. For example, a) enlarging the rotor diameter causes air-friction loss and affects the thermal, electromagnetic, and structural performance of the machine, b) variation of the stator diameter directly impacts the machine's efficiency by changing the electromagnetic coupling between the stator and rotor, and c) the structural design safety margin of the rotor is strictly limited by the mechanical properties of the PM. Therefore, a Multiphysics optimization by integrating multi-disciplinary analysis is highly recommended to design a robust and efficient HP-UHSM.

4.2 Existing Design Models and Limitations

In recent years, rigorous research has been done to optimize and design UHSM ranging from 500,000 to million rpm. However, they are mostly done for low-power design, for example, 100W 500,000 rpm machine for gas turbine application [67], 150W 500,000 rpm machine for spindles application [68], 55W 850,000 rpm for microturbine application [30], 300 W 500,000 rpm machine for laser scanning application, and 100W 1 million rpm for solar impulse application

[14]. A comparative illustration of existing design methodologies and their implementations is given in Table-4.1.

UHSM Design Methodologies	Implementation	Reference
1. Efficiency optimization method		
-Based on electromagnetic and air-friction loss analysis	100W, 500,000 rpm	[67]
using an analytical model.	55W, 850,000 rpm	[30]
-Rotor and stator are optimized concurrently.		
2. Stress optimization method		
-Based on cohesive zone model-based structural analysis	125W, 1200,000 rpm	[69]
using the FEA model.	74W, 1000,000 rpm	[70]
-Rotor and stator are optimized separately.		
3. Multidisciplinary design method		
-Based on electromagnetic & air-friction loss, and structural	138W, 1200,000 rpm	[43]
analysis using the FEA model.	100W, 500,000 rpm	[71]
-Rotor and stator are optimized separately.		
4. Self-bearing motor design method		
-Based on bearing force, electromagnetic loss, and air-	150W, 500,000 rpm	[68]
friction loss analysis using an analytical model.	300W, 500,000 rpm	[14]
-Additional bearing winding is used in the airgap for		
magnetic levitation of the rotor.		

Table 4.1Existing design methodologies of UHSM

In [67]-[30], a total loss minimizing optimization is used to design low-power UHSMs. These design optimization methods are based only on electromagnetic and structural analysis. Also, it is assumed that the UHSM has a constant temperature (in the axial and radial direction), and the impact of such temperature variation is negligible. However, when the power of UHSM is increased, its loss-per-unit area becomes very high, and the motor temperature changes considerably in a nonlinear distribution. These temperature changes significantly affect the PM's energy density and rotor's physical expansion. In addition, the input current has considerable influence on magnetic field distribution due to high stator electrical loading. Avoiding these effects

can lead an HP-UHSM to the degradation of mechanical integrity and electromagnetic performance.

In [14], a 300W 500,000 rpm machine is designed by using the self-bearing design method. In this method, the analytical model of [67] has been extended by adding a radial bearing design analysis to develop an integrated self-bearing UHSM. Here, the output power is improved by increasing the SEL using a higher input current and coil turns. However, increasing the SEL in a slotless stator using a 3-phase winding increases the effective airgap length of the UHSM. Consequently, the efficiency and power density of the machine are decreased considerably compared to [67]. In addition, the back-EMF of a PM motor is proportional to the number of coilturns [72]. Hence, the machine requires a larger DC link voltage.

In [43]-[71], a multidisciplinary design approach has been presented to develop a 138W 1200,000 rpm UHSM. Here, the optimization is performed for the rotor and stator separately using the finite element analysis. First, the rotor is sized for a specific air-friction loss (20%), and then the stator is optimized for an electrical efficiency (\geq 75%), including the rotor eccentricity. An external water jacket cooling system is also adopted to limit the winding temperature. However, it is observed that the PM of the final motor experiences a 50 °C more temperature than it was designed in the optimization. This has led to a 2% electrical efficiency reduction and a 14% rotor stress increase due to the thermo-electrical and thermo-structural effects, respectively. Such mutual influences can be severe as the UHSM power rating increase.

Research is also carried out on switched reluctance UHSM for low-power applications [69]-[70]. In this motor technology, the fragile permanent magnet is not present in this motor technology, but the rotor core geometry is still critical to the stress, CBR, and air-friction loss at high-speed operation. A 1200,000 rpm 100 W switch reluctance UHSM has been presented in

[69]. Here, a cohesive zone model based on structural analysis is used to design the rotor, while the stator is designed separately for the required torque and current density. It is shown that the rotor has three CBR frequency below its rated speed and does not operate at or near those CBRs to avoid any catastrophic failure. However, this criterion is not suitable for emerging applications like AMEBA. The CBRFs below rated speed would severely limit the AMEBA's operating bandwidth and might lead to unwanted vibration.

All these design methods are developed to design UHSM for very low shaft torque at the rated speed, while emerging applications such as the AMEBA system require much higher torque with additional design constraints. Therefore, these design methods must be further studied to design an HP-UHSM by considering the following issues:

- The CBR must be integrated into design constraints in rotor geometry optimization, along with the air-friction loss and centrifugal stress.
- The mutual influence of Multiphysics performances must be considered during geometry optimizations.
- Multiple objectives such as efficiency, design safety margin and power density must be utilized to optimize the HP-UHSM for emerging applications.
- The multiphase winding configuration must be utilized in the slotless stator to increase SEL effectively.
- The slotless stator must utilize the multiphase winding configuration to increase SEL effectively.
- Multi-disciplinary design constraints including the material and manufacturing limitations must be defined in the optimization.

4.3 **Proposed Multiphysics Design Method**

Due to the inter-disciplinary design complexity discussed in chapter II, a Multiphysics design strategy is developed in this section to design an HP-UHSM for the AMEBA application. Figure 4.1 shows the simplified workflow of the proposed Multiphysics design method. Unlike conventional, this model systematically integrates the proposed multiphase electromagnetic analysis, exponentially increasing air-friction loss, 3D thermal analysis, temperature integrated stress analysis, and CBR estimation, developed in chapter 3, to address the new challenges of HP-UHSM. The model is established in the commercial motor design software ANSYS Workbench platform, as shown in Figure 4.2. It is a hybrid optimization model, where an FEA model is used for electromagnetic analysis and an analytical model are used for four other physics analyses. The analytical models are written using the excel script feature of the Workbench. All Physics models are coupled using a "Parameter set" co-simulation linker. This coupling system successfully enables the mutual influence among Multiphysics performances during the optimization. For instance, the thermal model will use the electrical and air-friction losses as the input heat source and send the estimated temperature to the other physics models to consider the thermal mutual effects. In addition, with the benefit of Multiphysics models, a global DSM can be considered, and multi-disciplinary design constraints can be defined in this optimization. Here, the global DSM is defined as the minimum DSM among all physics performances, which is written as (3.54):

Global DSM = $min (DSM_{electromagntic}, DSM_{mechanical}, DSM_{thermal}, DSM_{rotordynamic})$ (3.54)



Figure 4.1 Proposed Multiphysics design strategy of HP-UHSM for AMEBA application.



Figure 4.2 Co-simulation analysis of Multiphysics analysis modules in ANSYS Workbench.

The proposed design method has eight steps and three decision points. The steps are summarized as follows:

• Step 1:

The design process starts with this step. Here, the motor specifications are studied from the AMEBA operation requirements. These requirements are the rated torque, speed, rotor dynamic, and so on.

• Step 2:

In this step, the initial considerations are made based on the AMEBA system requirements. These include the system power density, rotor and stator topology, winding pattern candidates, and motor cooling system.

• Step 3:

In this step, he possible materials for all active parts are studied and the potential candidates are selected for the optimization process. In this step, the number of stator phase is also defined. It can be 3-phase, 5-phase, 6-phase, 7-phase, and 9-phase. The possible candidates can be selected by using the initial parametric analysis of UHSM.

• Step 4:

In this step, the machine sizing is performed by using an integrated Multiphysics analysis. The optimization model has five physics modules, including an electromagnetic module, airfriction loss analysis module, thermal analysis module, structural analysis module, and Rotordynamic analysis module. To enable the mutual influences, these Multiphysics analysis modules are systematically coupled by using the co-simulation feature.

The selection of appropriate optimization objectives and design variables is another challenge in the design of HP-UHSM because its performance is crucial from various aspects, such as efficiency, DSM, and PD. In this design method, a parameter correlation analysis (PCA) is used to determine the effective objective functions and design variables based on their sensitivity. The PCA also helps to decrease the number of design variables based on their influence on output performances. Different correlation methods, such as the Spearman, Pearson, and Kendall analyses can be used. The HP-UHSM has many nonlinear relationships between the design variables and objectives; hence the Spearman correlation coefficient is the most suitable for this study, which is defined as (3.55) [73]:

$$\rho_{X_i,Y_i} = 1 - \frac{6\sum_{i=1}^n (X_i - Y_i)^2}{n(n^2 - 1)}$$
(3.55)

where *n* is the number of samples, X_i and Y_i are the *i*th optimization objectives or outputs and design parameters, respectively.

In this optimization, the design of the experiment (DOE) technique is used to generate enough effective samples of each design parameter. In DOE, each parameter's sampling size, upper limit, and lower limit are selected in such a way that it covers the entire design space.

The HP-UHSM presents several non-linearities at UHS operation. To address these nonlinearities, a set of response surfaces is generated by an interpolation technique using the DOE samples. In this design method, the Kriging semi-parametric interpolation method is used, whose response value is defined as (3.56) [74]:

$$Y(n) = Y_o(x) + Z(x)$$
(3.56)

where Y(x) is the unknown response function of design parameter x, $Y_o(x)$ is the deterministic function of x, and Z(x) is the error function, defined by vector parameter with zero-mean, σ^2 variance, and non-zero covariance matrix C_m. The matrix C_m is defined by (3.57):

$$C_{m} = Cov[x^{m}, x^{n}] = \sigma^{2} \mathcal{R}[R_{F}(x^{m}, x^{n})] \qquad m, n = 1, 2,, n$$
(3.57)

where x^m , x^n are the sample points from DOE, \mathcal{R} and R_F are the correlation matrix and correlation function, and n is the total sample number of x. The Kriging method can generate an effective approximation model for higher-order local non-linearities of electric machines [74]. These response surfaces will be utilized in optimization for the prediction purpose.

• Step 5:

In multi-objective optimization, there is no single optimal solution. Rather, the optimal solutions are a compromise among all other objectives. Hence, in this step, a Pareto-front analysis is performed to select the best optimal solutions. Once all the DPs are solved, the Pareto-front can

be obtained based on the defined constraints and objectives. The best optimal design is the one that satisfies all the multi-physics constraints and has the highest global DSM and efficiency. Change the materials or phase number in the outer loop if no DP is found with a desire DSM and efficiency.

• Step 6:

In this step, a complete FEA co-simulation is performed to evaluate the Multiphysics performance and its influence on the machine. In this analysis, 3-D FEA is used considered for all physics to accurately predict the critical performances such as eddy current loss, thermal distribution, end winding effect, and vibration. This step also validates the accuracy of the analytical modules.

• Step 7:

The bearing selection is another challenge in the design process of UHSM. In this step, the appropriate bearing selection is performed by using the natural frequency vs bearing stiffness variation analysis. Then, the casing and UHS test bench are designed and validated by modal analysis and vibration analysis using Harmonic simulation.

• Step 8:

This is the final step of this design process. If the optimized motor satisfies all the design constraints and desired performances, it is prototyped and assembled using the shrink-fit technique. Then, experimental analysis is performed to validate the FEA results of step 6. In this step, the hammer test, no-load test, dynamo-test, load-test, thermal-test, and parameter measurements are performed. The design process is completed if the testing results validate the FEA results

CHAPTER V

DESIGN AND OPTIMIZATION OF HIGH-POWER ULTRA-HIGH-SPEED MACHINE FOR AMEBA SYSTEM

5.1 Introduction

In this section, an HP-UHSM is optimized by using the proposed Multiphysics design method of chapter IV. The machine specifications are derived from the studied AMEBA application requirements. The initial considerations are made based on the system requirement and motor design experience. The motor will be used in the AMEBA system for wireless communication in the RF-denied environment using extremely-very low frequency (ELF-VLF). This electro-mechanical transmitter concept received significant academic attention since it was first described in 2017 by the DARPA AMEBA program [75]. Our previous work [1] presents the possibility and effectiveness of using a rotating permanently polarized magnet (PPM) dipole in the AMEBA system, shown in Figure 1.1(b). It uses a high-speed motor drive for rotating a PPM dipole to generate an alternating magnetic field at ELF-VLF. It is also observed that the operating bandwidth, distance, and efficiency of the AMEBA transmitter can be increased significantly by using an HP-UHSM. Therefore, an HP-UHSM targeting a rotational speed of 500,000 rpm is designed in this chapter.

5.2 Derivation of HP-UHSM Specification from AMEBA System

The design specifications of the HP-UHSM are derived from the AMEBA system requirements. In our previous AMEBA design [1], a 12 W 10,000 rpm motor drive was used with



Figure 5.1 PPM dipole geometry and example of AMEBA rotor dynamic.

a PPM dipole of $1.83 \times 10^{-6} kgm^2$. Due to the limited power and speed rating of motor drives, the antenna has a maximum data transfer rate (DTR) of 2 Hz/s with a bandwidth of 88-116 Hz. It can transmit only ~7 characters per minute at a distance of a few meters. In this studied design, the antenna is aimed to transmit more than 50 characters per minute beyond 1 km distance and increase the bandwidth up to 8.3 kHz. To achieve that, the AMEBA system requires an 8,333 Hz frequency (f) and a PPM of $1.1 \times 10^2 Am^2$ magnetic dipole moment (m_d). The magnetic field from this rotating dipole can be found as (3.58):

$$\overrightarrow{B_{dip_{rot}}} = \frac{3\mu_0 m_d}{8\pi r^3} \sum_{k \in \{x, y, z\}} a_k \cos(\omega_m t) + b_k \sin(\omega_m t)]\hat{k}$$
(3.58)

where ω_m corresponds to the rotating frequency of the dipole. Considering 8,333 Hz as the rotating frequency and the minimum pole number (p = 2), the required motor speed can be calculated by $N_r = 120 f/p$. Considering a hollow cylinder PPM to generate the required m_d , the total motor inertia is calculated as (3.59):

$$J_L = J_r + J_s + J_d \approx J_d \approx \frac{1}{2}\pi\rho(R_o^2 - R_i^2)(R_o^2 + R_i^2)l_d$$
(3.59)

where J_r , J_s and J_d are the rotor, shaft, and dipole inertia; ρ , R_o , R_i , and l_d are the density and PPM dipole dimensions, as shown in Figure 4.1. In this design, $J_d = 1.62 \times 10^{-4} kgm^2 \gg (J_r + J_s)$. Figure 5.1 also shows the dynamic of the AMEBA rotor for communication operation, which requires a frequency change of 6 Hz with 0.5s transient, i.e., a DTR of 12 Hz/s. Considering this rotor dynamic, the required motor power can be calculated by (3.60):

$$P_{out} = \frac{2\pi}{60} S_F \underbrace{\left(\tau_l + J_L \frac{d\omega_m}{dt}\right)}_{T_e} \times N_r \tag{3.60}$$

where SF is a safety factor, $\frac{d\omega_m}{dt}$ is the DTR, and τ_l is the load torque. Furthermore, for the safetycritical AMEBA application, a Multiphysics DSM is required, and the machine should have at least state-of-the-art UHSM efficiency. Based on these considerations, the key specifications of the UHSM are summarized in Table-5.1.

Parameters	Value
Rated output power	2000 W
Rated shaft torque	38.2 mNm
Base speed	500,000 rpm
Active power density (including winding)	> 45 kW/L
Efficiency	> 94%
First critical bending frequency	> 8500 Hz
Design safety Margin	≥ 30%
Maximum DC link voltage	< 240 V
Maximum phase current (peak)	< 8A
Cooling system	Natural air cooled

Table 5.1Proposed UHSM specifications for AMEBA system

5.3 Initial Considerations

Considering the required output power, speed level, and application background, the proposed HP-UHSM topology is selected for the AMEBA. It has a slotless-stator and coreless rotor, as shown in Figures 2.5(b) and 2.4, respectively. The shaft lengths (L_1 , L_2 , and L_3) are kept constant based on the application requirement and bearing housing. In this design, UHS ball bearings are considered to make the shaft length minimum (required for air-bearing) and avoid additional circuit complexity (required for magnetic bearing). For motor cooling, natural airflow cooling is selected in order to make the AMEBA system compact and portable. The stator uses multi-stranded litz wire to minimize the eddy current effect on the copper loss. For winding, toroidal fashion is considered to reduce the end-winding length of the motor. The air-gap length is selected as 0.6 mm. For the PM material, the Sm_2Co_{17} is selected over the Nd - Fe - B because of its excellent thermal and electrical performances. The different possible candidates are selected for stator and sleeve materials, presented in Table-2.5 and Table-2.4. For CBR analysis, the first CBR frequency (ω_{1st}) is calculated and restricted above the rated frequency.

5.4 PCA Analysis and Design Variables

The sensitivity analysis and Spearman correlation coefficient analysis are performed in this design method to determine the appropriate design variables and optimization objectives. Figure 5.2 shows the sensitivity analysis of key design parameters on the optimization outputs. It is observed that the rotor and stator parameters have a considerable dependency, and most design parameters correlate with more than one physics performance. The PM radius affects the PM stress, output torque, rotor temperature, and CBR performance. The manufacturing parameter SIFL has the opposite impact on PM and sleeve stress. The stator thickness (t_{SC}) has the minimum impact on motor performance. The PD is mainly affected by the electrical loading parameters.



Figure 5.2 Sensitivity analysis of design parameters on optimization outputs of the proposed HP-UHSM.

Figure 5.3 shows the Spearman correlation coefficient matrix of different critical output parameters. It shows that the air-friction loss is positively correlated with the PM temperature and PM stress development with a Spearman correlation coefficient of 0.95 and 0.78, respectively. On the other hand, the copper loss is negatively correlated with the PD and the air-friction, which



Figure 5.3 Spearman correlation coefficient matrix of output parameters of the proposed HP-UHSM.

Table 5.2	Input variables	and their	limits for DOE

Input and obtained Parameters	Lower Bound	Upper Bound	Sample Size
PM radius (R_{PM}) [mm]	2.5	5	0.1
Sleeve thickness (<i>t</i> _{SL}) [mm]	0.25	2	0.05
Stator thickness (t_{ST}) [mm]	1.5	5	0.25
Shrink fit length ($\Delta \delta_o$) [µm]	5	40	2
Stack length (<i>L</i>) [mm]	30	60	0.5
Phase current (I_a) [A _{rms}]	2.5	10	0.1
Number of turns (Nc)	10	40	1
Stator inner radius (R_{si}) [mm]	7	20	1

means these are contradictory outputs. Thus, the PCA analysis also shows the importance of Multiphysics coupled optimization with multiple objectives. Initially, the parametric model of the proposed HP-UHSM has sixteen design variables. After the PCA analysis, eight design parameters are selected for motor sizing, these are R_{PM} , t_{SL} , $\Delta \delta_o$, N_C , I_a , R_{si} , t_{SC} , and L. The DOE technique is applied to generate the initial design consideration. Table-5.2 shows the upper limits, lower limits, and sample size of these design variables, which are applied in the DOE to generate the response surface model. The output torque, electrical losses, and winding loss are selected for the optimization objective.

5.5 Multiple Optimization Objectives

In this design optimization, three objectives are considered by analyzing the PCA results in Figure 5.2 and 5.3. These are defined in the ANSYS Workbench as:

> $Ob_1(x) =$ **Seek target**: 38.2 mNm torque at 500,000 rpm $Ob_2(x) =$ **Minimize**: total electrical losses (P_E) $Ob_3(x) =$ **Minimize**: Air - friction loss (P_f)

where $P_E = P_{Cu,cond} + P_{Cu,prox} + P_{Fe} + P_r$. According to the PCA results, the P_f is affected by the rotor's magnetic loading parameters (R_{PM} and L) and it is positively correlated with the $\sigma_{v,PM}$, T_{pm} , and ω_{1st} . On the other hand, the P_E is affected by the stator's electrical loading parameters (N_c and I_a), and it is positively correlated with the T_{wind} and negatively correlated with the PD. Therefore, minimizing the P_E and P_f will directly influence on the $\sigma_{v,PM}$, T_{pm} , N_c , I_a , and ω_{1st} . However, these low losses are contradictory, hence they cannot be minimized together. Here, the Ob_I will achieve the rated power of the motor, whereas the Ob_2 and Ob_3 will minimize the electrical and air-friction losses separately to obtain the optimal RML and SEL.

5.6 Multi-disciplinary Optimization Constraints

Several multidisciplinary design constraints are associated with the AMEBA system, material limitations, and practical aspects, as shown in (3.61). The electromagnetic constraints are set as $J_{den} < 5 \text{ A/mm}^2$ and $B_{p,S} < 1.1 \text{ T}$ by considering the cooling method and stator material properties, respectively. The thermal constraints are applied by considering the PM's energy density ((*BH*)_{max}) and the coil's insulation limit as $T_{PM,max} < 150 \text{ °C}$ and $T_{Wind,max} < 130 \text{ °C}$. The structural constraints are set by considering the rotor material properties as $\sigma_{PM} < UTS_{PM}$ and $\sigma_{SL} < UTS_{SL}$. Where UTS is the ultimate tensile strength limit of the material. The Rotordynamic constraint is from the antenna's bandwidth requirement as $\omega_{1st} > 8.5 \text{ kHz}$. The studied AMEBA setup provides the system constraints, such as active PD > 45 kW/L and $V_{DC_link} < 240 \text{ V}$. Finally, the practical constraints are set as $\Delta \delta_o < 30 \text{ }\mu m$, $t_{SL} > 0.4 \text{ }mm$, $h_g = 0.6 \text{ }mm$, and $t_{St} > 1.5 \text{ }mm$ by considering the manufacturing limitations.

$$\begin{cases} g_1(x) = B_{P_{stator}} < 1.1 T \\ g_2(x) = J_{den} < 5 A/mm^2 \\ g_3(x) = P_{den} > 60 kW/L \\ g_4(x) = T_{W_max} < 130^0 C \\ g_5(x) = T_{PM_max} < 150^0 C \\ g_6(x) = \sigma_{PM} < 100 MPa \\ g_7(x) = \sigma_{SL} < \lambda \sigma_{t,SL} MPa \\ g_8(x) = p_c > 0 MPa \\ g_9(x) = \omega_{1st} > 8500 Hz \\ g_{10}(x) = PD > 45 \frac{kW}{L} \\ g_{11}(x) = V_{DC link} < 240 V \\ g_{12}(x) = \Delta \delta_0 < 30 \ \mu m \\ g_{12}(x) = t_{SL} > 0.4 \ mm \\ g_{12}(x) = t_{SL} > 1.5 \ mm \end{cases}$$
(3.61)

5.7 **Optimization Results**

The optimization is performed using the multi-objective genetic algorithm (MOGA), a variant of the popular genetic algorithm-II based on the controlled elitism concept [76]. It is a guided random searched-based optimization technique, very effective for motor design optimization with multiple objectives and many design variables. It can find the global optimum from multiple objectives and constraints, making it suitable for electric motor design. Eight design variables are considered in this design; the upper and lower bounds of these variables are given in Table 5.2. The optimization is performed in three different cases, the results are summarized as follows:

5.7.1 Case Studies

5.7.1.1 Case 1

First, an attempt is made to design the targeted HP-UHSM using a three-phase winding. The Multiphysics constraints are applied, such as the thermal, stress, CBR frequency, and vibration limits. In this case, no feasible DP is found that satisfies the requirements, especially the output power and PD. Second, the thermo-structural and PD constraints are ignored, and the machine is optimized again using the three-phase winding. In this case, some solutions are found, and the best feasible solution is selected among them. However, it does not meet the desired efficiency, PD, and structural DSM. It has an outer stator radius of 17.7 mm, which is beyond the limit of the AMEBA system's specification. This DP is referred to as M-1.

5.7.1.2 Case 2

First, different multiphase windings are evaluated, and their Multiphysics performances are compared, as shown in Figure 5.4. It is observed that, the adoption of multiphase winding



Figure 5.4 HP-UHSM performances vs. number of phases.

increases the electrical loading in a slotless stator more effectively than the three-phase winding, and it provides an additional degree of freedom to increase the global DSM of the motor. However, it also shows that the output power and other performances do not improve at the same rate as the phase-number increases. Besides, increasing the phase number increases the manufacturing complexity and power electronics losses. Considering these issues, the six-phase winding is selected as the optimal winding configuration for the proposed motor.

Then, all the multi-disciplinary design constraints are applied again, and the HP-UHSM for AMEBA system is optimized using a six-phase winding. In this case, several feasible DPs are found, and the optimal DP is selected from the trade-off between the efficiency and global DSM using a Pareto-front analysis. A trade-off analysis between the Ob2 and Ob3 is performed to obtain a robust and efficient design, as shown in Figure 5.5. As shown, the P_E and P_f conflict with each



Figure 5.5 Trade-off plot and Pareto-front line of the optimization results.



Figure 5.6 Variation of different losses with PM radius at 500 kr/min.

other and the design feasibility is closely correlated with both losses. The figure has five Pareto sets, where the best to worst feasibility is expressed by the blue to red color. The best DP is the one that satisfies all the design constraints and has the desired global DSM with maximum efficiency.

It is observed that the PM radius (R_{PM}) is the most dominant and sensitive parameter in the proposed HP-UHSM topology, especially for machine losses. Figure 5.6 shows the variation of different losses at 500,000 rpm as the R_{PM} changes. A smaller R_{PM} results in a lower air-friction loss on the rotor, but it leads to an excessive copper loss in the coil due to a lower airgap flux density and higher input current, and vice versa. It is also shown that there is a minimum R_{PM} , where the total motor loss is minimal but does not guarantee the desired performance.

Parameters	DP-1	DP-2	
$R_{PM}, R_{SL,o}, R_{S,i}, R_{S,o} \text{ [mm]}$	3.9, 4.6, 11.25, 14	4.15, 4.89, 11, 13.7	
g_m , L, t_{SL} , [mm], $\Delta \delta_o$ [µm]	0.6, 40, 0.7, 20	0.6, 38.1, 0.74, 23	
I_a [A], J_{den} [A/mm ²], N_C , m	3.9, 4.84, 20, 6	3.56, 4.9, 20, 6	
Sleeve and stator material	Titanium alloy and Amorphous core		
Torque at 500 kr/min (mNm)	38.35 (~2 kW)	38.33 (~2 kW)	
Efficiency (%)	94.5 %	94.65%	
Power density with toroidal winding	47 kW/L	49 kW/L	
First UNBF, ω_{1st} (Hz)	9103	9624	
$\sigma_{PM}, \sigma_{SL}, P_c$ (MPa)	83.3, 622, 80.62	101.4, 675, 86.6	
$T_{PM,max}$, $T_{W,max}$ (°C)	124.4 and 110.2	143 and 112	
Global DSM (%)	30.5%	16%	

Table 5.3Inputs and output parameters of selected DPs

Form the pareto-front plot, it is also observed that the maximum efficiency DP is not the best DP. The best DP (DP-1) and the maximum efficiency DP (DP-2) are indicated in the plot, and their important input and output parameters are presented in Table-5.3. As shown, the DP-2 has a maximum efficiency of 94.65% with a PD of ~49 kW/L, but it's global DSM is only 16%, which does not meet the requirements. On the other hand, the DP-1 provides a global DSM of 30.2%;



Figure 5.7 Optimized HP-UHSM models for 3 different cases (not drawn to scale).

while it compromises the efficiency and PD a little compared to the DP-1, it still meets all requirements and constraints of the studied AMEBA system. Hence, the DP-1 is selected as the optimal design in this case. This DP-1 is referred to as M-2.

5.7.1.3 Case 3

Finally, to compare the benefit of the six-phase winding over the three-phase winding, the conventional three-phase winding is applied in M-2 design by keeping all geometry dimensions the same. In this case, the value of N_{PS} , N_{TP} , and I_a are optimized by constraining the maximum current density as M-2. The best DP is selected and referred to as M-3.

5.7.2 Performance Comparison Between Three-phase and Six-phase Windings

Figure 5.7 shows the geometry and the winding configurations of the three designs (M-1, M-2, and M-3) obtained in the case study section. Their design parameters and Multiphysics performances are also presented in Table-5.4 and Table-5.5, respectively.

Parameters	M-1	M-2	M-3
Magnet radius, R ₁ (mm)	4.1	3.9	3.9
Sleeve outer radius, R ₂ (mm)	4.9	4.6	4.6
Coil inner radius, R ₃ (mm)	5.5	5.2	5.2
Stator inner radius, R ₄ (mm)	15.2	11.25	11.25
Stator outer radius, R ₅ (mm)	17.7	14	14
Machine Stack length, L (mm)	40	40	40
Interference-fit length, $\Delta u_o (\mu m)$	22.5	20	20
Number of turns, N _{TP}	30	20	29
Nominal coil diameter, (d _c) (mm)	1.70	1.07	1.21
Number of strands, N _{PS}	175	100	125
Input current, I _a (A)	6.3	3.9	4.9
Slot current density (A/mm ²)	4.41	4.84	4.71

Table 5.4Design parameters of optimized machines (M-1, M-2, and M-3)

 Table 5.5
 Multiphysics performances of the three-phase and six-phase designs

Parameters	M-1	M-2	M-3
Peak back-EMF (V)	160.1	125.8	175.4
Peak input current (A)	6.3	3.9	4.9
Average electromagnetic torque (mNm)	38.2	38.35	32.2
Torque ripple (%)	0.7	< 0.1	0.2
Efficiency (%)	92.49	94.51	93.28
Power density with airgap winding (kW/L)	40.6	64.9	55
Air-friction loss, P_f (W)	84	61.1	62.4
Copper AC loss, $P_{cu_{ac}}$ (W)	55.2	37.8	38.9
Rotor eddy current loss, P_r (W)	~0.2	~0.2	~0.2
Stator core loss, P_{Fe} (W)	2.2	2.2	2.1
Maximum PM temperature (°C)	158.7	124.6	132.4
Maximum Coil temperature (°C)	142	110.2	128
Maximum Axial temperature variation in PM(°C)	52	18.8	38
Structural Design Safety Margin (%)	13%	31%	30%
Undamped CBR frequency, w _{1st} (Hz)	9122	9103	9103

Back-EMF: The design M-2 has a peak back-EMF value of 125.8 V, whereas it is 160 V in M-1 and 175 V M-3. Hence, the three-phase design M-3 has 40% higher voltage stress than the six-phase design M-2. Thus, the six-phase design will require a lower DC-link at UHS operation.

Full-load performance: It is observed that M-2 requires a sinusoidal current of 5.5 A (peak) to produce 38.35 mNm average torque (≈ 2000 W) with an efficiency of 94.51%. Whereas M-1 requires 62% more input current to produce the same amount of torque, and it has 92.5% efficiency at the rated speed. Although M-3 has the same machine geometry as M-2, it produces only 32.2 mNm torque (≈ 1720 W) with 32% higher input current and 1.16% lower efficiency than M-2. The M-2 has a torque ripple (TR) of less than 0.1%. However, with a similar sinusoidal input current, the TR is increased to 0.7% in M-1. This is because of the PM's uneven magnetization due to its asymmetric temperature variation.

Power Density: The M-2 has a PD of 64.9 kW/L, which meets the AMEBA requirement. However, M-1 and M-3 have a PD of 40.6 and 55 kW/L. Thus, the six-phase winding benefits the HP-UHSM with higher efficiency, PD, and lower TR compared to the three-phase.

Thermal performance: At the rated operation, the maximum PM temperature is 158.7 °C in M-1, 124.6 °C in M-2, and 132.4 °C in M-3. The maximum coil temperature is 142 °C, 110.2 °C, and 128 °C in M-1, M-2, and M-3, respectively. Hence, the six-phase design provides a higher thermal safety than three-phase designs by reducing both the coil and PM temperature.

Mechanical Performance: The machine M-2 and M-3 have the same rotor geometry. In the M-2 rotor, the sleeve experiences maximum stress of 621.2 MPa in its inner edge, while the maximum stress on the PM is 83.3 MPa in its center. Both stress values are well below their corresponding limit. However, to maintain the same stress (83 MPa) on the PM of the M-1 rotor, the sleeve experiences maximum stress of 784 MPa. The structural DSM is calculated as a ratio of

the developed stress on a rotor material to the UTS limit of that material. It is found that the sixphase design M-2 has a structural DSM of ~31% at 500,000 rpm, whereas it is only ~13% in the three-phase design M-1, which is 60% lower than M-2. The first CBR frequency of all rotors is above 9.1 kHz.

5.8 Multi-disciplinary Optimization Constraints

The proposed optimization model greatly improves computational efficiency by implementing the hybrid modules and response surface technique. The optimization is converged after 1154 iterations with the 6-phase winding configuration. It took about 4.2 hours for a laboratory desktop of 64-bit, Intel i9, 3.6 GHz CPU with 32 GB RAM. To compare the computational effectiveness, the optimal DP-1 is also simulated by using a fully integrated FEA model in the ANSYS Workbench. Here, a 3D Maxwell, Fluent-CFD, a 3D Steady-State-Thermal, 2D Static-Structural, and 3D Modal modules are used. A fine mesh is used in the critical region, whereas a coarse mesh is used elsewhere. In this case, the simulation model takes about 32 minutes to solve a single DP using the same computer. In contrast, the proposed model takes only ~1 minute to solve the same DP.
CHAPTER VI

PERFORMANCE EVALUATION OF THE PROPOSED HIGH-POWER ULTRA-HIGH-SPEED MACHINE USING FEA SIMULATION

6.1 Introduction

In this section, the optimized HP-UHSM (M-2) is evaluated by using extensive FEA simulations, and the results are compared with the optimization output of Table V to verify the effectiveness of the proposed analysis modules of section IV. In detail, the different loss analysis, efficiency map, stress analysis, bearing selection, and vibration analysis analyses are presented in this section. The optimal design has a 6-phase stator winding, slotless stator, and 2-pole shrink-fitted rotor. The optimal materials are: Amorphous iron for the stator, titanium alloy for a sleeve, and Sm_2Co_{17} is for PM.

6.2 Electromagnetic Analysis

Figure 6.1 shows the FEA electromagnetic model of the proposed HP-UHSM (M-2) in ANSYS Maxwell. It has a 6-phase asymmetric winding with a 30° displacement angle applied between the two 3-phase winding sets. For neutral connection, the dual neutral point is considered, which avoids the zero-sequence current flow and has better DC bus utilization capability. Each phase coil has 40 turns of Litz wire in series. The six-phase input current is applied in the windings as (4.1). The non-linear BH curve of the stator core and PM is shown in Figure 6.2.



Figure 6.1 2D and 3D FEA model of the optimized HP-UHSM with a 6-phase toroidal winding configuration.

$$I_{A} = \sqrt{2} I_{rms} Sin(2\pi f)$$

$$I_{B} = \sqrt{2} I_{rms} Sin\left(2\pi f - \frac{2\pi}{3}\right)$$

$$I_{C} = \sqrt{2} I_{rms} Sin\left(2\pi f - \frac{4\pi}{3}\right)$$

$$I_{D} = \sqrt{2} I_{rms} Sin\left(2\pi f + \frac{\pi}{6}\right)$$

$$I_{E} = \sqrt{2} I_{rms} Sin\left(2\pi f + \frac{\pi}{6} - \frac{2\pi}{3}\right)$$

$$I_{F} = \sqrt{2} I_{rms} Sin(2\pi f + \frac{\pi}{6} - \frac{2\pi}{3})$$
(4.1)

First, the back-EMF is simulated at 500,000 rpm with no excitation current ($I_{rms} = 0$) and presented in Figure 6.3(a). The back-EMF is almost pure sinusoidal due to the slotless stator and cylindrical PM. The machine has a peak back-EMF of 125 V, and the back-EMF constant is calculated as 2.5 × 10⁻⁴ V/(r/min). Then, the winding and PM temperature is set to 110 °C and ... °C for full load simulations, and the rated current of Figure 6.3(b) is applied. Figure 6.4 shows that the machine develops an average electromagnetic torque of 38.24 mNm, when a 3.9 A (RMS)



Figure 6.2 The non-linear BH curve of (a) Sm_2Co_{17} and (b) Amorphous *Metglas-2605SA*.

sinusoidal input current is applied in the six-phase winding. The motor has negligible torque ripple (<0.1%), and the torque constant is calculated as ~9.8 mNm/A_{rms}. The mutual interaction (MI) between the two sets of three-phase winding is calculated using FEA simulation. A DC current is supplied to ABC winding set ($I_a = 1$ A, $I_b = -0.5$ A, $I_c = -0.5$ A) and DEF winding set is disconnected. Both the sleeve and PM are removed to ensure zero flux density in the rotor. The MI is calculated by means of the ratio between mutual inductance and self-inductance. Figure 6.5 shows the mutual interaction in the proposed HP-UHSM is 67%. This is rational because the ABC winding set shares the slot of DEF winding.

Figure 6.6 shows the flux density distribution of a low-power UHSM (100W) and the proposed HP-UHSM (2kW) generated by the rated stator current only. It is shown that the maximum flux density in the low-power UHSM is 0.014 T, whereas it is 0.14 T in HP-UHSM. Therefore, the SEL of the proposed HP-UHSM contributes around ten times more flux density on coils and stator than the low-power UHSM. This impact has been considered in both the torque and loss calculations. Figure 6.7 shows the distribution of the flux density and flux lines of the



Figure 6.3 6-phase back-EMF at 500,000 rpm and (b) rated six-phase input current at 500,000 rpm.



Figure 6.4 Electromagnetic torque of the proposed HP-UHSM at the rated condition.



Figure 6.5 Mutual inductance interaction calculation between two winding sets.



with stator current excitation only

Figure 6.6 Flux density distribution of (Left) 100W and (Right) 2000 W UHSM, generated with the rated stator current only.



Figure 6.7 Flux density distribution and flux lines at rated load condition.

proposed machine at the rated condition. The motor has a maximum flux density of 0.61 T in both the core and PM.

Figure 6.8 shows the efficiency map of the machine with the 6-phase winding, where the efficiency is calculated as (4.2):

$$\eta = \frac{P_{out}}{P_{out} + P_{wind} + P_{cu,Cond} + P_{cu,Prox} + P_{Fe} + P_r} \times 100\%$$
(4.2)



Figure 6.8 Efficiency map of the proposed HP-UHSM.



Figure 6.9 Loss distribution of proposed HP-UHSM at rated condition.

As shown, the motor has a maximum efficiency of 96% from 100 to 300,000 rpm, and it reduces as the speed increases. At 500,000 rpm, the efficiency is 94.5%. Figure. 6.9 shows the loss distribution of the proposed motor at the rated condition. Also, Figure 6.10 shows the variation of electrical and air-friction losses with the fundamental frequency. It is observed that both losses increase exponentially with the fundamental frequency. However, due to the slotless amorphous



Figure 6.10 Losses vs. operating frequency of the proposed HP-UHSM.

stator, the core loss is limited to only 2.2 W at the rated frequency. The air-friction loss is the most dominant at 500,000 rpm, and it is calculated as 60 W. The total copper loss at 500,000 rpm is calculated 53.6 W, where the AC proximity loss is 38 W, and DC ohmic loss is 15.6 W. The PM eddy current loss is only 0.2 W only.

6.3 Thermal Analysis

A 3-D FEA model, shown in Figure 6.11, is used to analyze the temperature distribution of the proposed HP-UHSM. The two-way coupling between the electromagnetic solver and the thermal solver is used for this study. Different losses (copper loss, stator core loss, rotor eddy current loss, air-friction loss) are obtained from the electromagnetic solver and imported as temperature load to the thermal model's corresponding materials. In FEA-thermal model analysis, these losses are converted into heat sources in terms of heat rate per unit volume ($q_v = P_{loss}/Volume$) and heat flow per unit area ($q_s = P_{loss}/Area$). The dominated loss coefficients for different regions are shown in Table 6.1 and Table 6.2. The heat rate (internal heat source) is caused by the stator winding loss, stator core loss, and rotor/sleeve eddy current loss. On the other hand, the heat flow (heat-flux source) is generated by the air-friction loss on the airgap and nonairgap rotor surface. The thermal conductivity of different materials of the proposed HP-UHSM is shown in Table 6.3.

Table 6.1Heat flow of HP-UHSM due to air-friction loss

Region Name	Loss (W)	Heat Area (mm ²)	Heat Flow (W/mm ²)
Rotor Surface/Sleeve	60	1156.1	5.2E-02

Table 6.2Heat rate of HP-UHSM due to electromagnetic losses

Region Name	Loss (W)	Heat Volume (mm ³)	Heat Rate (W/mm ³)
Stator winding	53.6	21886.2	2.45E-03
Stator Core	2.2	8725.8	2.52E-04
Sleeve	0.2	747.7	2.67E-04



- A Core loss in Stator (as internal heat generation in volume)
- B Copper loss in Winding (as internal heat generation in volume)
- C Air-friction loss on rotor air-gap surface (as heat flux on surface)
- D Eddy loss in rotor (as internal heat generation in volume)
- E Air-friction loss on rotor non-airgap surface (as heat flux on surface)
- F-Rotor Airgap convection
- G-Rotor non-airgap convection
- H Rotor end-face convection
- I-Coil convection
- $J-Stator \ core \ convection$

Ambient temperature is 25 °C. "Bonded" type connection is used between parts.

Figure 6.11 FEA thermal analysis model in ANSYS Steady-State-Thermal.

Materials	Unit (W/mm.ºC)
Titanium (Sleeve)	0.009
Sm ₂ Co ₁₇ (Magnet)	0.01
Amorphous Metglas (Stator core)	0.013
Polyethylene (Insulation)	0.0004
Copper (Winding)	0.4
Air	2.42E-05

Table 6.3Thermal conductivity of different active materials of the proposed HP-UHSM

Heat transfer by means of conduction and convection is considered in this study. The heat convection coefficients for the different regions are calculated using the analytical equation derived in [60], [61]. The boundary condition is applied according to the actual motor cooling system. The natural air-cooling system is considered in the prototype with an ambient temperature of 25° C.

Figure 6.12 shows the temperature distribution of the HP-UHSM at rated condition. The maximum temperature is 128.3 °C, which occurred in the PM. The air-friction loss is the dominant factor of this temperature. Note that the PM axial temperature distribution is not constant; rather, it increases from the edge to the center along the axial direction. Such uneven temperature distribution can cause the PM to uneven magnetization, leading the rotor to unwanted torque-pulsation and vibration. The maximum PM axial temperature variation in this design is 18 to 20 °C. Figure 6.12 (bottom) also shows that the proposed quasi-3D LPTM can estimate the PM axial temperature variation with an accuracy of 97.2%.

In the HP-UHSM, the coil operating temperature is also critical, and it is limited to 120 °C only due to the natural air-cooling system. The coil temperature is influenced by both electrical losses and air-friction loss. At the rated condition, the coil temperature is 110 °C, and the stator core temperature is 109 °C. All these temperature values are within the desired limit.



Figure 6.12 (Top) temperature distribution of the proposed HP-UHSM at the rated loading condition, (bottom) PM axial temperature variation using FEA and quasi-3D thermal model.

Therefore, the HP-UHSM has a 32% thermal design safety margin at the rated condition.

Also, the thermal analysis results of the proposed analytical model match the FEA results with an

error of only ~3%.

6.4 Structural Analysis

The stress distribution of the rotor is investigated using the 3-D structural FEA. Figure 6.13 shows the 2D and 3D structure FEA models. The necessary boundary conditions are considered, and optimal interference-fit (20 μ m) is applied in the contact zone of PM and sleeve. The von Misses stress equivalent (VMES) criterion is used to check the fracture in the brittle material (Sm_2Co_{17}) and the yield failure in the ductile material (Titanium), where the VMES is defined as equation (3.51). The maximum rotor temperature (128 °C) is obtained from the thermal model and applied in the structural model to calculate the material's thermal expansion.

Figure 6.14 shows the AMEBA rotor's radial displacement due to the interference-fit, thermal, and UHS rotational effect. At a standstill, the PM is compressed by 2.15 μ m because of the interference-fit implementation. But this compression turns into an expansion of 4.68 μ m at 500,000 rpm due to the centrifugal force and thermal expansion. On the other hand, at a standstill, the outer rotor radius is expanded by 16.6 μ m, increasing by 7.2 μ m at 500,000 rpm. It is also evident that the high working temperature significantly influences the rotor's displacement. As shown in Figure 6.14, at 500 000 r/min, the outer magnet radius expands by 4.68 μ m, and the sleeve inner radius expands by 4.63 μ m due to the 130 °C working temperature. However, because of the advantage of both material's similar CTE, the resultant interference-fit length remains ~19.7 um. As a result, the constant contact pressure is sustained in the interference zone, ensuring a continuous torque transfer from magnet to shaft throughout the full operating speed.

Figure 6.15 shows the stress distribution of the proposed rotor at a standstill (0 rpm) and the rated speed (500,000 rpm) condition using a 2D model.



Figure 6.13 Structural FEA model of the proposed HP-UHSM.



Figure 6.14 Radial displacement of HP-UHSM (N = $25 \degree C \& T = 130 \degree C$) at 500,000 rpm.

Figure 6.16 shows the PM stress variation of the proposed HP-UHSM. At a standstill, the PM only experiences the compressive stress due to the interference fit implementation. In this design, it is only 80 MPa through the whole magnet, which is well below the compressive strength limit of Sm_2Co_{17} (800 MPa). With the increased rotational speed, a centrifugal force generates, which attenuates the compressive force. At 352,000 rpm, the compressive force becomes zero, i.e.,



Figure 6.15 Stress distribution of the proposed HP-UHSM: (Left) 0 rpm, (Right) 500,000 rpm.



Figure 6.16 Stress (VMS) distribution in the PM of HP-UHSM.



Figure 6.17 Stress (VMS) distribution in the sleeve of the HP-UHSM.

the centrifugal force is equal to and opposite to the compressive force. Then, a further increase in the rotational speed rapidly generates the tangential and radial stress in the magnet's origin. At the rated speed, the maximum VMES in the PM origin is 83 MPa, which satisfies the magnet strength limit considering a 30% safety factor. Furthermore, this VMES stress turns into a compressive force of 57 MPa at the magnet outer radius.

The sleeve stress distribution of the proposed HP-UHSM is shown in Figure 6.17. At a standstill, because of the static contact pressure, the sleeve experiences compressive stress of 80 MPa in the radial direction and tensile stress of 485 MPa in the tangential direction, which results in a VMS of 538 MPa at the contact zone. As speed increases, the tensile stress in the sleeve increases due to the developing centrifugal force. At 500,000 rpm, the sleeve tensile stress is increased to 590 MPa, resulting in a maximum VMS of 620 MPa at the contact zone, which is below the tensile stress limit of the titanium (950 MPa) considering the 35% safety factor.

Therefore, the proposed HP-UHSM has a structural DSM of 31% at 500,000 rpm. Also, the structural analysis results of the proposed analytical model match the FEA results with an error of only 2%.

6.5 Rotordynamic Analysis

6.5.1 Undamped Natural Frequency Analysis

The modal simulation is performed using 3D FEA under a free-free boundary condition to estimate the AMEBA rotor's UNFs. Figure 6.18 shows the first four UNFs and their deformation mode shapes. The first two lower frequencies are rigid body frequencies, generating a lateral (1 Hz) mode shape and a conical (5 Hz) mode shape. The third and fourth frequencies cause the 1st order bending mode (9012 Hz) and 2nd order bending (17053 Hz) shape. Therefore, it is confirmed that the AMEBA rotor has no CBR frequency below 8333 Hz. These FEA UNFs match the results of the proposed Rotordynamic analytical model with an error of ~2%.

6.5.2 Selection of Guide Bearing Stiffness

The use of guide bearings directly influences the undamped natural frequencies of the rotor. Figure 6.19 shows the variation of the first four natural frequencies with the different bearing stiffness values. It is observed that the 1st lateral mode frequency increases as the bearing stiffness increases, but when the bearing stiffness reaches 100 MN/m, it is not changing anymore. A similar phenomenon is observed for 2nd, 3rd, and 4th natural frequencies, but they are saturated at different bearing stiffness. It is worth mentioning that applying an excessively stiff guide bearing may lead the rotor to a serious 1st mode stability issue [77].



Figure 6.18 Deformation mode shape of the proposed rotor at different undamped natural frequencies: (a) lateral mode (1 Hz), (b) conical mode (5 Hz), (c) 1st order bending mode (9012 Hz), and (d) 2nd order bending mode (17053 Hz).

In the HP-UHSM, the design and selection of bearing depend on various factors such as the maximum operating speed, mechanical load characteristics, available bearing housing area, bearing loss, bearing temperature, operation hours, and bearing environment. In literature, three types of bearing are used in the UHSM: ball bearing, air bearing, and magnetic bearing. Ball bearings are always the first choice for a rotating machine because of their simplest design and high robustness. However, it causes high friction loss in the UHS operation [78]. Air bearings are non-contact frictionless bearing which uses air pressure to levitate the rotor. This bearing can



Figure 6.19 Natural frequencies vs guide bearing stiffness.

operate at UHS with minimum friction losses, but the maximum speed is limited by the instability issue [79]. It increases the total shaft length, which affects the rotor's natural frequency significantly. Magnetic bearing uses magnetic forces produced by the permanent magnet and copper windings to levitate the rotor [80]. It is also a contactless bearing suitable for high-speed operation, but its use is limited due to additional control circuitry and complex feedback control.

The miniature air bearings have a lower stiffness value of around 1×10^6 N/m, the stiffness of magnetic bearing depends on the tuning, and ball bearings have the highest stiffness value, approximately 1×10^7 N/m [81], [82]. In this study, a pair of ball bearing having a radial stiffness of 10×10^6 N/m is selected for the proposed HP-UHSM considering the robust operation, compact AMEBA transmitter, control complexity, and availability.

6.5.3 Campbell Diagram Analysis

After selecting the bearing stiffness value, the Campbell diagram of the studied rotor can be generated using ANSYS FEA. Figure 6.20 shows the Campbell diagram of the studied rotor,



Figure 6.20 Campbell diagram of HP-UHSM. [S-stable, US-unstable, CSP- critical speed point].

considering the boundary condition, the bearing stiffness, the gyroscopic effect, and the rotational velocity effect. A synchronous whirl line is drawn in the diagram to obtain critical speed points. The intersection of the natural frequency curve and the synchronous line are defined as critical speed points (CSP). The first four critical speed points are indicated in the figure. Due to the implementation of guide bearing stiffness, the rigid body modes are moved from 1 Hz to 2,806 Hz and 5 Hz to 5,956 Hz. The first bending frequency is shifted from 9,012 Hz to 11,250 Hz, and the second bending is also shifted from 17,015 Hz to 21,003 Hz. Consequently, the rated speed point (500,000 rpm or 8,333 Hz) falls between the 2nd and 3rd critical speed points. However, there is a 28% separation margin (SM) between the 2nd critical speed point. Also, the first critical speed point is at 2,806 Hz, which ensures the wide bandwidth ULF communication of the AMEBA transmitter.

With the increase of rotational speed, the applied gyroscopic moment weakens the system stiffness of the backward whirl (BW) and hardens the system stiffness of the forward whirl (FW),

which splits each natural frequency into two modes (FW & BW). A high separation between the FW and BW is not accepted because it will limit the rotor's operation in between them. Figure 6.20 also shows that all these modes are stable, and the difference between the FW and BW mode of the first two critical frequencies is negligible, which confirms an excellent overall system stiffness and stability.

6.5.4 Unbalance Response Analysis

Unbalance harmonic force at critical resonances is one of the major vibration sources of the UHS rotating shaft. An unbalance harmonic response analysis is performed to investigate the vibration amplitude, air-gap clearance, and restricted frequency bandwidth of the AMEBA rotor due to its unbalance characteristics. For the designed rotor, the permissible residual unbalance can be calculated by ISO standard-1940 [83] as (4.3):

$$U_{max} = 1000 \left(\frac{G \times M}{N}\right)$$
 g.mm (4.3)

where *G* is the balance quality grade, which is 0.4 mm/s for the UHS rotor, *M* is the rotor mass (kg), and *N* is the rotating speed (rad/s). In this study, the 1st order bending mode of the AMEBA rotor is considered for the unbalance response analysis because it is the nearest critical bending frequency point where the highest deformation has occurred. Figure 6.21 shows the 1-D beam diagram of the 1st order bending mode shape and different observation point locations on the rotor. The calculated unbalance mass is applied on the 3rd point, which is the rotor center, as shown in the figure. Figure 6.22 shows the frequency response function (FRF) result of the unbalanced



Figure 6.21 1st order bending frequency and different observation points for unbalance response analysis.



Figure 6.22 Unbalance response analysis result: (Frequency Response Function).

response analysis at different rotor points. The acceptable vibration displacement (V_{allow}) of the AMEBA rotor can be roughly calculated using API-610 standard [84] as (4.4):

$$V_{allow} = 25.4\sqrt{(12000/n)} \tag{4.4}$$

Figure 6.22 shows that, at the rated speed, the rotor vibration level is well below the allowable limit (12.2 μ m). Also, the rotor maintains the minimum clearance in the airgap. However, at the 3rd and 4th critical speeds, the vibration amplitude becomes excessively high (more than few millimeters), and the rotor fails to maintain the minimum mechanical clearance in the

airgap, which will result in a structural breakdown of the rotor. Hence, this proposed HP-UHSM should never pass these critical bending frequencies (3rd and 4th). The FRF result also shows that the vibration amplitude at the 1st and 2nd critical speed points reaches the allowable limit, but their restricted frequency bandwidth is very small. The maximum one is only 180 Hz from 2800 to 2980 Hz. Therefore, the AMEBA rotor should not operate at these frequency bandwidths, and these frequencies should pass quickly and carefully during continuous operation.

Finally, the global DSM is calculated as 30% using the critical FEA performances of all physics. Therefore, it is concluded that the proposed analysis model of chapter IV can accurately estimate the essential performances of HP-UHSM.

CHAPTER VII

PROTOTYPING AND EXPERIMENTAL VALIDATION

7.1 Introduction

Due to the unconventional geometry, the development of the proposed HP-UHSM is different from the conventional motor designing process. It needs a special manufacturing process for building the rotor, stator, winding, and assembly. By this time, commercial prototyping of the HP-UHSM is not available in the industry. Hence, the proposed HP-UHSM prototype in being built in the Power Electronics and Energy System (PEES) lab at Mississippi State University.

In addition, the testing of UHSM is difficult due to the unavailability of testing equipment such as shaft coupler, encoder, and power electronics, especially at 500,000 rpm. In this study, a cascaded single-shaft rotor is developed to test the proposed HP-UHSM.

7.2 Prototyping Proposed HP-UHSM

7.2.1 Rotor Prototype

The proposed rotor does not have any steel shaft through the rotor center, laminated rotor core, and visible PM. A miniature UHS rotor can be developed by using two processes: i) 3D printing and ii) CNC machining technique. The additive manufacturing technique (AMT) is widely used for 3D rotor printing. AMT has the advantage of easy prototyping, possible to design a complex geometry, and capable of rapid production. However, AMT is not suitable for the studied HP-UHS rotor prototyping because AMT has a high dimension tolerance, which hinders the appropriate interference-fit implementation. A proper interference-fit between the magnet and



Figure 7.1 The AMEBA rotor development using the shrink-fit technique.



Figure 7.2 Induction heater with graphite cruciate and Ceramic Container.

sleeve is mandatory in the HP-UHS rotor to limit the PM stress and ensure an efficient torque transfer from the magnet to the shaft. On the other hand, the CNC machining technique has a lower dimension tolerance, but it is comparatively cumbersome and costly.



Figure 7.3 Rotor parts and assembly of the proposed HP-UHSM.

The proposed AMEBA rotor has a cylindrical Sm_2Co_{17} PM inside a retaining sleeve and shaft parts made of titanium alloy (Grade-V). It is built by using the shrink-fit technique, as shown in Figure 7.1. The titanium parts are designed using CNC machining with a tolerance precision of \pm 0.005 mm. The cylindrical PM (Sm_2Co_{17}) is diametrically magnetized. The sleeve was heated up to 340° C using an induction heater, and the PM was cooled down to -190° C using the liquid nitrogen (LN) to obtain a 20 µm interference-fit between them. Figure 7.2 shows a graphite crucible with a ceramic container used in a 1500 Watts induction heater to heat the titanium to 340° C. The magnet temperature can be reached to $-190^\circ C$ by keeping it in liquid nitrogen for 1-2 minutes. During this shrink fitting, attention must be taken so that the magnet surface has no LN frost to avoid unwanted corrosion. Shaft parts are also installed using the shrink-fit technique. A metallic adhesive is used in the joint between the shaft parts and sleeve to improve the bonding. Figure 7.3 shows different rotor parts and the assembled rotor of the proposed HP-UHSM. A pair of customized UHS ball bearings are used, which have a nominal operating speed up to 450,000 rpm for at least 15 hours (with oil). Finally, the rotor-balancing is done by scrubbing at different points on the rotor surface. The proposed rotor has an axial length of 72 mm including the shaft parts.

7.2.2 Stator Prototype

In this study, a three-phase (M-3) and six-phase (M-2) slotless stator are prototyped. Figure 7.4 shows a hollow cylinder stator core made of Amorphous iron also named Metglas 2605SA1. The high-temperature polyimide electrical tape is applied to the stator core to insulate it.

The windings for both stators are implemented in a toroidal fashion. Figures 7.5 and 7.6 show the winding pattern of the three-phase and six-phase winding, respectively. The three-phase winding has 29 turns in a coil; hence each phase has 58 turns in series. Each turn consists of 125 strands of 40 AWG magnet wire, forming a diameter of 1.17 mm. The six-phase winding is implemented using an asymmetric configuration as Figure 6.1, where the displacement angle between two winding sets is 30°. Each coil has 20 turns, resulting in 40 turns per phase in series. Each turn consists of 100 strands of 40 AWG magnet wire and has a diameter of 1.06 mm. In a 3-phase wounding set, 120-degree phase displacement is applied to create a balance stator field. A full-pitch distributed winding is applied in this design. Both coils are served using Poly-Nylon insulation (Class-F) to withstand the phase voltage. A PT100 RTD sensor is used in the winding to measure the coil temperature. Figure 7.7 shows the stator prototype of the six-phase (M-2) and three-phase (M-3) HP-UHSM.

The casing is made of Aluminum by using CNC machining. The side parts are created so that the air can easily flow inside the rotor. It also helps to measure the coil temperature using a



Figure 7.4 Slotless stator core made of Amorphous Metglas iron sheet.



Figure 7.5 Three-phase stator winding pattern applied in the M-3 prototype.



Figure 7.6 Six-phase stator winding pattern applied in proposed HP-UHSM (M-2) prototype.



Figure 7.7 (a) Three-phase stator and (b) six-phase stator prototype using slotless stator and toroidal winding.



Figure 7.8 Prototype: Six-phase HP-UHSM (b) three-phase HP-UHSM, and (c) front view.



Figure 7.9 Motoring test bench of the proposed HP-UHSM.

thermal imager. Figure 7.8 shows the full motor prototype with aluminum casing. Note that, the same rotor is used in both machines (M-2 and M-3). Figure 7.9 shows the UHS test bench with the proposed six-phase machine. It is used for the motoring experiment. The test bench does not have any resonance frequency below 15 kHz.



6-phase (M-2) 3-phase (M-2)

(b)

Figure 7.10 (a) Cascaded rotor for dynamo testing, and (b) Dynamo setup of the proposed HP-UHSM.

7.2.3 Dynamo Prototype

A cascaded motor-generator dynamo setup is built to perform the loading operation of the HP-UHSM. Here, the proposed 6-phase machine (M-2) is mechanically coupled with a 3-phase UHSM (M-3) using a single shaft. Figure 7.10 shows the dynamo test bench and the cascaded rotor. The dynamo rotor has two magnets (the same size as the optimized rotor), separated by a titanium middle part and a total axial length of 140 mm. The length of the dynamo rotor is almost twice of the proposed rotor, hence it's 1st UNBF is decreased to 2382 Hz. This dynamo setup is used for the back-EMF and loading operation.

7.3 Experimental Analysis of HP-UHSM

7.3.1 Impulse Hammer Test

Accurate prediction of the AMEBA rotor's natural frequencies is crucial. Hence, an experimental modal analysis of the proposed rotor prototype (M-2) is performed to accurately predict the AMEBA rotor's natural frequencies. The impulse hammer test technique is used in this study. Figure 7.11 shows the impulse hammer test experimental setup of the assembled AMEBA rotor. In this experiment, a tip-changeable impulse hammer is used to excite the rotor mechanically, and an IEPE acceleration sensor is used to measure the rotor's frequency response. The sensor has a measurement range of up to \pm 500 g (pk), broadband resolution of 0.004 g (RMS), and a frequency range from 1 to 20,000 Hz. A signal-conditioner module is used to power the sensor. It also amplifies the sensor output and sends it to the oscilloscope. The sensor is attached to the rotor surface using quick bond gel, and a low-attenuating non-stiff media supports the rotor.

Two testing methods are used in the experiment: the roving hammer method (excitation point changes and response sensor fixed) and the roving sensor method (excitation point fixed and response sensor changes position). For each case, different hammer tips are used and found very similar results in every test.

Figure 7.12 shows the frequency response function (FRF) of the assembled AMEBA rotor when a mechanical excitation is applied using an impulse hammer. The plot is obtained by performing the FFT of the acceleration sensor's output signal. In the plot, the first two rigid body modes are not separable due to their low amplitude. However, two separable peaks are visible in the spectrum between 0 to 20,000 Hz, which confirms the first and second bending modes are at 9,248 Hz and 17,125 Hz, respectively. These experimental results are compared with the FEA



Figure 7.11 Impact hammer test setup of AMEBA rotor prototype.



Figure 7.12 Frequency spectrum result of the impulse hammer test.

Bending modes	Proposed model	Measured	Error (%)
1 st UNBF (Hz)	9103	9248	1.6%
2 nd UNBF (Hz)	18024	17125	5%

Table 7.1Comparison between the measured and calculated undamped natural bending
frequencies (UNBFs) of proposed rotor prototype

results in Table-7.1. The experimental results show an excellent agreement with the simulation results with an error of 1.7%, which is acceptable because a complete free-free boundary condition is not possible in practice. Note that the mechanical excitation is kept limited to a maximum of 20,000 Hz to avoid the PM mechanical breakdown during the hammer hitting. Therefore, the 1st order UNBF of the proposed rotor is measured at 9248 Hz. Thus, it is confirmed that the rotor has no critical UNBF below 8.3 kHz.

7.3.2 Machine Parameters

The electrical parameters of the stator prototype are measured using an LCR meter and compared with the simulated value obtained from FEA. The stator has a phase resistance (DC) of 0.17 Ω , including the external coil resistance. This value can be further dropped significantly by using an airgap winging such as the Skewed, cup-shaped, self-supporting winding. Due to more coil turns per phase, the three-phase winding has 47% higher phase resistance (DC) than the six-phase winding. For the same reason, the six-phase machine has a phase inductance of 69 μ H, 38% lower than the three-phase design. The inductance value of the proposed HP-UHSM is considerably lower than the conventional high-speed machine due to the slot-less stator, lower coil turns, and lower stator pole number. Therefore, it may need to use an external inductor to remove the switching harmonics of the SVPWM. However, a higher coil inductance can reduce the power

Parameters	Six-phase (FEA)	Six-phase (Experiment)	Three-phase (FEA)	Three-Phase (Experiment)
Stator phase resistance, $R_s(\Omega)$	0.155	0.172	0.231	0.25
Stator phase inductance, L_s (μ H)	67.2	69.4	110.1	112
Magnet flux linkage Ψ_{pm} (mVs)	2.308	2.3	3.098	3.09

Table 7.2Comparison between the measured and FEA Electrical parameters of the proposed
HP-UHSM

factor of the machine. Hence an optimal external inductance must be selected to ensure the desired operation. The PM flux-linkage of the three-phase and six-phase machine are measured as 3.09 mVs and 2.3 mVs. The Measured values are compared with the FEA value as shown in Table 7.2. Both results show very close agreement.

7.3.3 Back-EMF Performance

Then, the dynamo test is performed using the setup of Figure 7.10 to measure the back-EMF of the proposed HP-UHSM. Note that the first UNBF of the cascaded dynamo rotor is at 2382 Hz (~144,000 rpm) due to its higher axial length (140 mm). Hence, all dynamo tests of this study are kept limited to a maximum speed of 120,000 rpm to avoid any catastrophic system failure. The full experimental setup of the proposed HP-UHSM is shown in Figure 7.13.

Figure 7.14 shows the no-load back-EMF test results of the prototype. The measured results are also compared with the FEA results. As expected, the measured back-EMF is sinusoidal and closely matches the FEA result. The back-EMF constant can be calculated from the terminal voltage vs. speed curve as 2.5×10^{-4} V/ (r/min), equal to the FEA result.

Figure 7.15 and Table 7.3 compare the phase-to-neutral back-EMF of phase-A for both machines. The three-phase machine has 40% higher voltage stress than the six-phase machine. The



Figure 7.13 Full experimental setup of the proposed HP-UHSM.



Figure 7.14 (Top) phase-A back-EMF at 120,000 rpm, (bottom) back-EMF versus speed.



Figure 7.15 The measured back-EMF of both HP-UHSMs at 50,000 rpm.

Table 7.3	Comparison of phase to neutral back-EMF between the six-phase and three-phase
	HP-UHSM.

	60,000 rpm		120,000 rpm		
	Three-phase	Six-phase	Three-phase	Six-phase	
FEA	21V	15 V	42 V	30 V	
Measured	21.4 V	15.25 V	42.5 V	30.4 V	
Error	1.9%	1.7%	1.4%	1.3%	

back-EMF constant of the three-phase machine is calculated as $3.5 \times 10^{-4} \text{ V}(\text{r/min})^{-1}$. These values show good agreement with the FEA results of 6.3(a) with an error of less than 2%.

7.3.4 Reaction Torque Performance

The reaction torque is measured by connecting external variable resistors at the output terminal of the targeted machine in the cascaded motor-generator setup of Figure 7.10. To drive the motor, customized GAN-FET-based six-phase and three-phase inverters are developed, as shown in Figure 7.16. In literature, various control algorithm has been developed for high-speed machine [6], [12]. This study implements a sensor-less field-oriented control (FOC) algorithm in the Texas Instrument (TI) DSP module for speed control, as shown in Figure 7.17.


Figure 7.16 Customized six-phase GaN inverter development for 2000 W 500,000 rpm HP-UHSM



Figure 7.17 Sensor less FOC control applied for motoring operation.

The reaction torque is calculated using $T_e = (\frac{m}{2})i_r \Psi_{pm}$, where Ψ_{pm} is the PM flux linkage calculated from back-EMF analysis and i_r is the peak current through the resistor. Note that the reaction torque is measured at low speed, hence the air friction and eddy current loss are ignored.



Figure 7.18 (Top) Phase-A input current at 120,000 rpm, (bottom) reaction torque vs. phase current (RMS).



Figure 7.19 The measured reaction torque of three-phase HP-UHSMs at 120,000 rpm.



Figure 7.20 The input current of both HP-UHSMs at 60,000 rpm motoring operation

Figure 7.18 shows the input current waveform, and the reaction torque results, measured at 120,000 rpm. The input current contains a small amount of lower-order harmonics due to PWM switching technique. The torque constant of the proposed HP-UHSM is calculated as ~9.82 mNm/A_{rms} .

Fig. 7.19 shows the reaction torque of torque of the three-phase machine. The three-phase machine has a torque constant \sim 6.7 mNm/A, which is 32% lower than the six-phase machine. These values match with the FEA results of Table-5.5.

The motoring operating of both machines is tested at different operating speeds by considering the rotor-inertia and friction-torque as the effective machine load. In this case, since both machines have the same rotor and bearing, the loading condition is also the same for both machines. Figure 7.20 shows the input current (Phase-A) of both machines operating at 60,000 rpm. It is observed that the three-phase machine draws 34% more input current compared to the six-phase machine for the same loading condition.

7.3.5 Thermal Performance

Figure 7.21 shows the thermal performance of the proposed HP-UHSM at different rotational speeds with the friction load only. The coil temperature is measured at 5 minutes

intervals using the thermal imager and pt100 sensor. As shown, the coil temperature first increases sharply and then steadies after \sim 30 minutes. The measured steady-state coil temperature is around 59° C at 120,000 rpm and 70° C at 250,000 rpm. In FEA, these values are 56.2° C and 66° C, respectively. The difference is because of the bearing loss, which has not been considered in the FEA analysis. Nevertheless, the temperature profile is matched closely.

Table-7.4 presents the comparison of thermal performance (winding temperature) between the three-phase and six-phase machine. Both machines are operated at 60,000 rpm and 120,000 rpm with rotor-inertia and friction-torque as operating load. The testing result shows that the threephase winding experiences higher temperature than the six-phase machine in both operating speeds. Also, the winding temperature of the three-phase machine increases rapidly when the input current (or speed) increases. The experimental result closely validates the FEA result with an acceptable error.

7.3.6 Efficiency Estimation

The efficiency of the dynamo setup is measured by comparing the input power of the 6phase motor and the output power (absorbed by external resistors) of the 3-phase motor. Figure 7.22 shows the measured and predicted (FEA) efficiency of the dynamo testing at a torque of 20 mNm. As shown, the efficiency is low at low-speed operation because the output power is very small due to the low voltage. It is also shown that the efficiency decreases as the rotational speed increases, mainly due to an increase in the eddy current loss and air-friction loss of the long dyno





Temperature at 120,000 rpm

Temperature at 250,000 rpm



Figure 7.21 Measured coil temperature of the proposed HP-UHSM at different speeds: (top) using thermal imager (bottom) using pt100 sensor.

 Table 7.4
 Comparison of coil temperature between the three-phase the six-phase HP-UHSM

Winding Temperature	60,000 rpm (3-D FEA)	60,000 rpm (Measured)	120,000 rpm (3-D FEA)	120,000 rpm (Measured)
Three-phase HP-UHSM	54°C	56° <i>C</i>	72° <i>C</i>	75°C
Six-phase HP-UHSM	50° <i>C</i>	53°C	66° <i>C</i>	68°C

rotor. The test results show a good match with FEA, but the difference increases at high-speed operation due to more bearing losses.

7.3.7 Dynamic Performance

In Figure 7.23, the speed-tracking performance of the proposed motor is tested at the ELF frequency range. The machine is operated at 1000 Hz. Then a step command of 50 Hz and 100 Hz are applied in the reference speed at 2 seconds and 6 seconds, respectively, as shown in Figure 7.23. This scenario realizes the AMEBA system's dynamic operation similar to Figure 5.1 at the ULF condition. It is observed that the proposed machine tracks the speed step commands less than 0.5 seconds, resulting in a dynamic of ~200 Hz/s. Note that the rotor is designed to perform 12 Hz/s at the rated condition, which is well below 200 Hz/s.

Finally, the proposed six-phase HP-UHSM is tested at ultra-high-speed using the motoring setup of Figure 7.10 to validate the rotor assembly and its structural integrity. The bearing and air-friction torques are considered as a load in this condition. The rotor was successfully driven up to 350,000 rpm without any structural breakdown. Figures 7.24 show the temperature of the proposed motor at 350,000 rpm. At this condition, the efficiency is calculated as 92.2%. The testing speed was limited to 350,000 rpm to avoid ball-bearing failure. In the future, the air-bearing will be implemented in the proposed motor to prevent bearing failure and friction losses at UHS operation.

7.4 State-of-the-art Performance Comparison

The performance of the optimized 6-phase HP-UHSM has been compared with benchmark motors to justify the effectiveness of the proposed design method. For benchmarking, four different motors have been selected from four existing design methodologies, presented in Table-4.1. The performance comparison is summarized in Table-7.5. The proposed design methodology



Figure 7.22 Measured and FEA efficiency of the dynamo setup.



Figure 7.23 Speed tracking test of the proposed HP-UHSM (realizing the AMEBA operation).



Figure 7.24 Temperature of the proposed six-phase HP-UHSM at 350,000 rpm.

can increase the output power of 500,000 rpm UHSM to 2000 W, whereas the maximum rated power is only 300 W in benchmark designs. The proposed method also increases the motor efficiency from 88.9% to 94.5%, with a simultaneous increase in power density from 20 kW/L to 47 kW/L. The Multiphysics integration feature of the optimization model provides a 30% global DSM in the proposed motor, while the highest global DSM of the benchmark models is 22%. Due to the integration of the Rotordynamic analysis module into the optimization model, the proposed motor does not have any CBR frequency below the rated speed, which was one of the critical design aspects for the targeted application. On the other hand, the 1st CBR frequency of the benchmark designs is below their rated speed. It is also observed that when the operating speed and rated power increase, the back-EMF voltage increases significantly in the existing design methods. However, the back-EMF of the proposed HP-UHSM is only 125 V (peak) due to the benefit of the multiphase toroidal winding. It also increases the thermal DSM by reducing the coil temperature.

Parameters	Base-1 [67]	Base-2 [69]	Base-3 [43]	Base-4 [14]	Proposed Design
Output power (W)	100	125	138	300	2000
Base speed (rpm)	500,000	1200,000	500,000	500,000	500,000
Number of phases	3	3	3	3	6
Efficiency (%)	88.9	72*	82.4	86.2	94.5
Power density (kW/L)	20	17	18	16	47
Structural DSM (%)	55	22	48	46	30
Thermal DSM (%)	22	25*	12	18*	32
1 st CBR frequency (Hz)	5204	6505	5204	4331	9103
Global DSM (%)	22	22	12	17	30
Terminal voltage (V)	16	90 V	33	45	125

Table 7.5Performance comparison of the proposed HP-UHSM and existing UHSMs.

* Estimated performance using available data

Therefore, it is concluded that the proposed Multiphysics design method can effectively address the new design challenges of HP-UHSM and can design an efficient, robust, and portable HP-UHSM for emerging applications.

CHAPTER VIII

CONCLUSION AND FUTURE RESEARCH SCOPE

8.1 Conclusion and Summary of the Proposed Study

Low-power UHSM has been studied widely, and promising solution has been developed for various applications. Recently, the emerging applications such as AMEBA system and fuel cell compressors require both high-power and ultra-high speed for efficient operation. However, there are several technical challenges in increasing the power of UHSM, including (i) critical resonances of the UHS rotor, (ii) uneven temperature distribution, and (iii) weak electromagnetic interaction between the slotless stator and rotor. Thus, the power rating of most UHSM has been very limited until recently. In this dissertation, a new Multiphysics design methodology has been proposed to design high-power UHSM for emerging applications by overcoming such technical challenges. The main analysis and contributions of this study are summarized as follows:

In chapter 1, the applications, prospects, and state-of-the-art of UHSM are presented. This study shows that increasing the output power of HP-UHSM (especially >500,000 rpm) can immediately improve the performance of many existing applications and enable various emerging applications that were not previously possible. However, in state-of-the-art, the maximum power of such UHSM is limited to only ~100 W due to several critical limitations.

In chapter 2, the critical design constraints that prevent increasing the power of UHSM are investigated and mathematically modeled to be integrated into the optimization model. These include rotor CBR, asymmetric temperature variation, effective SEL in the slotless stator, thermal effect on rotor stress, and accurate calculation of air-friction loss.

In chapter III, five physics analysis modules are developed to address the critical design challenges (investigated in chapter 2) of HP-UHSM. These are an electromagnetic module with multiphase winding, CFD-based air-friction loss analysis module, quasi-3D lumped thermal analysis module, temperature integrated structural module and Rotordynamic module of CBR frequency calculation.

In section IV, these multi-disciplinary models are systematically integrated using a cosimulation technique to consider the mutual influences among Multiphysics performances during the optimization. This integrated optimization model defines multi-disciplinary design constraints and multiple optimization objectives based on application requirements. The spearmen correlation coefficient analysis is used to determine the objective functions and design variables effectively.

In chapter V, a 2 kW 500,000 HP-UHSM is optimized for AMEBA application, and its Multiphysics performances are evaluated in chapter VI. This is the highest power-rated motor at this speed range. The optimization is carried out using the multi-objective genetic algorithm. The Pareto-front analysis is performed to obtain an efficient design without compromising the global DSM, ensuring the design's resilience at ultra-high-speed operation. It is observed that, unlike a conventional machine, increasing the SEL in the slotless UHSM using the three-phase winding does not increase the output power effectively. Because it increases the effective air-gap length and reduces the electromagnetic interaction between the rotor and slotless stator. On the other hand, increasing the SEL using a higher phase number allows lower input current and coil turn, resulting in an efficient power improvement in the slotless UHSM. It also provides an additional

degree of freedom to increase the power of UHSM with a high DSM. Hence, a six-phase winding topology is proposed in this dissertation to design HP-UHSM effectively.

The effectiveness of the proposed optimization model is verified by extensive FEA simulation using the ANSYS Workbench. Also, the proposed HP-UHSM prototype and its dynamo setup are built in the laboratory and tested experimentally in chapter VII. It is shown that the proposed HP-UHSM can deliver 2 kW output power at 500,000 rpm with 94.5% efficiency and an active power density of 47 kW/L. The proposed six-phase winding provides 15% higher output power than the conventional 3-phase winding. The first order UNBF of the proposed rotor is 9012 Hz, ensuring no CBR in the full operating region. The motor provides a 30% global DSM at 500,000 rpm.

The experimental results agree with the FEA results with a relative error of 1 to 5% and the proposed HP-UHSM satisfies all the requirements of the AMEBA system. Therefore, the proposed Multiphysics design method can be implemented to design HP-UHSM for other emerging applications, where high output power, CBR frequency, design safety, and portability are important factors.

8.2 Future Work

The followings are the possible future work where the findings of this research can be further extended:

8.2.1 Integration of Magnetic Bearing in the Proposed HP-UHSM

The proposed design method can define multi-disciplinary design constraints associated with both machine and application requirements. In this dissertation, the proposed optimization method is applied to the PM-based UHSM. However, the proposed design method can also be applied to other machine topologies. The switch reluctance machine (SRM) is another suitable



Figure 8.1 Proposed HP-UHSM geometry with magnetic bearing configuration.

candidate for UHSM, as shown in Figure 8.1. Hence, the proposed optimization method can be implemented on the SRM.

8.2.2 Implementation of the Proposed Design Method in Switch Reluctance Machine

The proposed design method has the capability to define multi-disciplinary design constraints, associated with both machine and application requirements. In this dissertation, the proposed optimization method is applied on the permanent magnet based UHSM. However, the proposed design method can be applied to other machine topologies as well. The switch reluctance machine (SRM) is another suitable candidate for UHSM, as shown in Figure 8.2. Hence, the proposed optimization method can be implemented on the SRM.



Figure 8.2 High-speed switched reluctance machine topology.

8.2.3 Apply the Proposed Design Method to Optimize other HP-UHSM

Although the proposed machine is rated for 2 kW 500,000 rpm, the proposed design method can be easily applied to scale up both the speed and power. Designing an HP-UHSM at 1 million rpm can be studied for spindle application. Similarly, apart from the AMEBA system, the proposed design method can be applied to design UHSM for other emerging applications such as fuel cell systems, dental handpieces, and rotating mirror scanners, where high output power, high DSM, and high efficiency are key factors.

8.2.4 Investigation of the Acoustic Noise

In the studied application, there was no acoustic noise requirement or constraint for the AMEBA system. However, acoustic noise is another limiting factor of UHSM. A Multiphysics acoustic analysis can be studied to predict and mitigate the noise level of the proposed HP-UHSM, especially at full-load operation.

8.2.5 Torque Measurement

The experimental torque measurement of UHSM is a critical challenge. Because there is no commercial torque sensor at the targeted speed range. In this dissertation, the reaction torque is measured by using a cascaded rotor and at a low speed. However, the torque of UHSM can be accurately measured by using a high-precision piezoresistive force sensor. A similar study can be done in the proposed motor to measure the torque at UHS.

REFERENCES

- J. S. Glickstein, J. Liang, S. Choi, A. Madanayake and S. Mandal, "Power-Efficient ELF Wireless Communications Using Electro-Mechanical Transmitters," *IEEE Access*, vol. 8, pp. 2455-2471, 2020.
- [2] M N, S.P., Tok, R.U., Fereidoony, F. et al. Magnetic Pendulum Arrays for Efficient ULF Transmission. Sci Rep 9, 13220 (2019).
- [3] S. Mandal, Jarred Glickstein, A. Sehirlioglu et al. Energy-Efficient ULF/VLF Transmitters Based on Mechanically Rotating Dipoles. IEEE Moratuwa Engineering Research Conference. May 2017.
- [4] S. Gong, Y. Liu, and Y. Liu, "A Rotating-Magnet Basesd Mechanical Antenna (RMBMA) for ELF-ULF Wireless Communication," Progress In Electromagnetics Research M, Vol. 72, 125-133, 2018.
- [5] "CT-15-150 Data sheet," http://www.celeroton.com/fileadmin/user_upload/produkte/kompressor/datasheets/Datash eet-CT-15-150.pdf
- [6] Cheng Gong, "Design and Control of Ulra-High Speed Switched Reluctance Machines Over 1 million RPM," Ph.D. dissertation, Georgia Institute of Technology, USA, 2019.
- [7] R. H. Jansen and K. L. McLallin, "G2 Flywheel Module Operated at 41,000 rpm," 2005.
- [8] P. Tsao, M. Senesky, and S. R. Sanders, "An integrated flywheel energy storage system with homopolar inductor motor/generator and high-frequency drive," IEEE Transactions on Industry Applications, vol. 39, pp. 1710-1725, 2003.
- [9] Wang, Wensen. "Design of high-speed flywheel motor/generator for aerospace applications." (2004).
- [10] A. Tuysuz, C. Zwyssig, and J. W. Kolar, "A novel motor topology for high-speed micromachining applications," IEEE Transactions on Industrial Electronics, vol. 61, pp. 2960-2968, 2014.
- [11] K. Moskvitch, "InNum6ers Solar Impulse 2 [News Briefing]," in *Engineering & Technology*, vol. 10, no. 5, pp. 18-19, June 2015,

- [12] Christof Zwyssig, "An Ultra-High-Speed Electrical Drive System," Ph.D. dissertation, ETH Zurich, Switzerland, 2008.
- [13] J. Szydlo, "High speed depth optical scanner," Ph.D. dissertation, Ecole Polytechnique Federale De Lausanne, Lausanne, Switzerland, 2000.
- T. Baumgartner, R. M. Burkart and J. W. Kolar, "Analysis and Design of a 300-W 500 000-r/min Slotless Self-Bearing Permanent-Magnet Motor," in *IEEE Trans. on Ind. Electron.*, vol. 61, no. 8, pp. 4326-4336, Aug. 2014.
- [15] T. Mashimo, S. Okazaki, and S. Shibazaki, "Ultra-centrifuge apparatus to generate a strong acceleration field of over 1 000 000 g at a high temperature in condensed matter," Review of Scientific Instruments, vol. 67, no. 9, pp. 3170–3174, 1996.
- [16] Online 03/02/2022, https://www.celeroton.com/en/application-areas/pneumatics.html
- [17] A. Binder and T. Schneider, "High-speed inverter-fed AC drives," in Proceedings of International Aegean Conference on Electrical Machines and Power Electronics (ACEMP '07), 2007, pp. 9–16.
- [18] J. Oyama, T. Higuchi, T. Abe, K. Shigematsu, and R. Moriguchi, "The development of small size ultra-high-speed drive system," in Proceedings of Power Conversion Conference (PCC '07), 2007, pp.1571–1576.
- [19] High speed PM brushless dc motor/generator. Calnetix. http://www.calnetix.com/.
- [20] PCB drilling products. Westwind Air Bearings. http://www.westwind-airbearings.com/.
- [21] PCB drilling product information. Fischer-Precise Group. http://www.precise.de/.
- [22] L. Zhao *et al.*, "A Highly Efficient 200 000 RPM Permanent Magnet Motor System," in *IEEE Transactions on Magnetics*, vol. 43, no. 6, pp. 2528-2530, June 2007,
- [23] Antivachis M, Dietz F, Zwyssig C, Bortis D and Kolar JW (2021) Novel High-Speed Turbo Compressor With Integrated Inverter for Fuel Cell Air Supply. Front. Mech. Eng 6:612301.
- [24] ATE System, 2016. [Online]. Available: http://www.ate-system.de/
- [25] "PCB Overview," http://www.westwind-airbearings.com/pcb/overview.html.
- [26] C. Zwyssig, S. D. Round and J. W. Kolar, "An Ultrahigh-Speed, Low Power Electrical Drive System," in *IEEE Transactions on Industrial Electronics*, vol. 55, no. 2, pp. 577-585, Feb. 2008.

- [27] Online 03/02/2022 https://www.celeroton.com/en/products/motors.html
- [28] C. Zwyssig, J. W. Kolar and S. D. Round, "Megaspeed Drive Systems: Pushing Beyond 1 Million r/min," in *IEEE/ASME Transactions on Mechatronics*, vol. 14, no. 5, pp. 564-574, Oct. 2009.
- [29] F. R. Ismagilov, N. Uzhegov, V. E. Vavilov, V. I. Bekuzin and V. V. Ayguzina, "Multidisciplinary Design of Ultra-High-Speed Electrical Machines," IEEE Trans. on Energy Convers., vol. 33, no. 3, pp. 1203-1212, Sept. 2018.
- [30] Flyur R. Ismagilov, Viacheslav Ye. Vavilov, and Valentina V. Ayguzina, "Basics of Highspeed Electrical machines," *in Intechopen*, [Online]. Available: https://www.intechopen.com/chapters/62857
- [31] C. Gong, S. Li and T. G. Habetler, "Rotor Dynamic Analysis of Ultra-high Speed Switched Reluctance Machines over 1 Million rpm," 2018 IEEE Energy Conversion Congress and Exposition (ECCE), 2018, pp. 1704-1709.
- [32] Lipo, T. A.. Introduction to AC Machine Design. United States, WisPERC, 2004.
- [33] Dajaku, Gurakuq and Dieter Gerling. "Design of Permanent Magnet Machines for Hybrid Vehicles." (2007).
- [34] T. Lubin, S. Mezani and A. Rezzoug, "2-D Exact Analytical Model for Surface-Mounted Permanent-Magnet Motors With Semi-Closed Slots," in *IEEE Transactions on Magnetics*, vol. 47, no. 2, pp. 479-492, Feb. 2011.
- [35] M. -S. Lim, J. -M. Kim, Y. -S. Hwang and J. -P. Hong, "Design of an Ultra-High-Speed Permanent-Magnet Motor for an Electric Turbocharger Considering Speed Response Characteristics," in *IEEE/ASME Transactions on Mechatronics*, vol. 22, no. 2, pp. 774-784, April 2017.
- [36] J. Oyama, T. Higuchi, T. Abe, K. Shigematsu and R. Moriguchi, "The Development of Small Size Ultra-High Speed Drive System," 2007 Power Conversion Conference -Nagoya, 2007, pp. 1571-1576.
- [37] New England wire technology: Litz & Winding Wires," Accessed on: Feb 6, 2022. [Online]. Available: https://www.newenglandwire.com
- [38] E.E. Ward, H. H[°]arer, Preliminary investigation of an inverter-fed 5-phase induction motor, *Proc. IEE* 116 (6) (1969) 980–984.
- [39] E. Levi, F. Barrero and M. J. Duran, "Multiphase machines and drives-Rev-isited," in *IEEE Trans. on Ind. Electron*, vol.63, no.1, pp.429-432, Jan.2016

- [40] Radionov A. A., Pirogova N. S., Taranenko P. A. Calculative and experimental analysis of natural and critical frequencies and mode shapes of high-speed rotor for micro gas turbine plant. Procedia Engineering, Vol. 129, 2015, p. 997-1004.
- [41] Yang, W.; Liang, M.; Wang, L.; Yuan, H. Research on Unbalance Response Characteristics of Gas Turbine Blade-Disk Rotor System.J. Vibroengineering 2018, 20, 1676–1690.
- [42] C. Gong, S. Li and T. G. Habetler, "Rotor Dynamic Analysis of Ultra-high Speed Switched Reluctance Machines over 1 Million rpm," 2018 IEEE Energy Conversion Congress and Exposition (ECCE), Portland, OR, USA, 2018, pp. 1704-1709.
- [43] F. R. Ismagilov, I. Khayrullin, V. Vavilov, and A.M. Yakupov, "Method of designing High-Speed generators for the biogas plant," Int. J. Renewable Energy Res., vol. 6, no. 2, pp. 447–454, Apr. 2016.
- [44] Y. Chen, J. Shen, and Z. Fang, "Topology and preliminary design of slotless brushless dc motor," in Proceedings of IEEE International Electric Machines and Drives Conference, 1997, pp. WB2/7.1–WB2/7.3.
- [45] P. Ragot, M. Markovic, and Y. Perriard, "Analytical determination of the phase inductances for a brushless dc motor with faulhaber winding," in Proceedings of 42nd IEEE Industry Applications Society Annual Meeting (IAS '07), 2007, pp. 1538–1543.
- [46] N. Bianchi, S. Bolognani, and F. Luise, "Potentials and limits of high-speed PM motors," IEEE Transactions on Industry Applications, vol. 40, no. 6, pp. 1570–1578, Nov.–Dec. 2004.
- [47] S.-M. Jang, S.-S. Jeong, D.-W. Ryu, and S.-K. Choi, "Comparison of three types of PM brushless machines for an electro-mechanical battery," IEEE Transactions on Magnetics, vol. 36, no. 5, pp. 3540–3543, 2000.
- [48] S. Ruoho, T. Santa-Nokki, J. Kolehmainen and A. Arkkio, "Modeling Magnet Length In 2-D Finite-Element Analysis of Electric Machines," in *IEEE Transactions on Magnetics*, vol. 45, no. 8, pp. 3114-3120, Aug. 2009
- [49] P. Asef, R. B. Perpiñà, M. R. Barzegaran, A. Lapthorn and D. Mewes, "Multiobjective Design Optimization Using Dual-Level Response Surface Methodology and Booth's Algorithm for Permanent Magnet Synchronous Generators," in *IEEE Transactions on Energy Conversion*, vol. 33, no. 2, pp. 652-659, June 2018.
- [50] Q. Chen, G. Xu, G. Liu, W. Zhao, L. Liu and Z. Lin, "Torque Ripple Reduction in Five-Phase IPM Motors by Lowering Interactional MMF," in *IEEE Trans. on Ind. Electron*, vol.65, no. 11, pp. 8520-8531, Nov.2018.

- [51] A. G. Yepes, J. Doval-Gandoy and H. A. Toliyat, "Improvement in DC-Link Utilization With Reduced Current and Torque Deterioration for Five-Phase Drives by Combination of Circulating-Current Filters and Simple Carrier-Based PWM Based on Closed-Form Expressions," in *IEEE Trans on Ind. Electron.*, vol. 68, no. 2, pp. 960-971, Feb. 2021.
- [52] J. Paredes, B. Prieto, M. Satrústegui, I. Elósegui and P. González, "Improving the Performance of a 1-MW Induction Machine by Optimally Shifting From a Three-Phase to a Six-Phase Machine Design by Rearranging the Coil Connections," in *IEEE Trans. on Ind. Electron.*, vol. 68, no. 2, pp. 1035-1045, Feb. 2021.
- [53] V. I. Patel, J. Wang, D. T. Nugraha, R. Vuletić and J. Tousen, "Enhanced Availability of Drivetrain Through Novel Multiphase PM Machine Drive," in *IEEE Trans. on Ind. Electron.*, vol. 63, no. 1, pp. 469-480, Jan. 2016.
- [54] Z. Ji, S. Cheng, Y. Lv, D. Wang, W. Sun and X. Li, "The Mechanism for Suppressing High-Frequency Vibration of Multiphase Surface Permanent Magnet Motors via PWM Carrier Phase Shifting," in *IEEE Trans on Power Electron.*, vol. 36, no. 9, pp. 10504-10513, Sept. 2021.
- [55] A. Cervone, M. Slunjski, E. Levi and G. Brando, "Optimal Multi-Harmonic Current Injection Strategy for an Asymmetrical Nine-Phase PMSM," 2020 International Symposium on Power Electronics, Electrical Drives, Automation and Motion (SPEEDAM), 2020, pp. 787-792.
- [56] C. Zwyssig, S. Round, and J. Kolar, "Analytical and experimental investigation of a low torque, ultra-high speed drive system," in Conference Record of the 2006 IEEE Industry Applications Conference Forty-First IAS Annual Meeting, 2006, pp.1507-1513.
- [57] K. Tanimoto, K. Kajihara, and K. Yanai, "Hybrid ceramic ball bearings for turbochargers." SAE Transactions: Journal of Materials & Manufacturing, vol. 109, pp. 763–775, 2000.
- [58] P. Holman. Heat transfer. Mechanical engineering series. McGraw-Hill, 1989. Gunnar Kylander. Thermal modelling of small cage induction motors. Ph.D. dissertation, Dept. of Electric Power Eng., Chalmers University of Technology, Goteborg, Sweden, 1995.
- [59] Gunnar Kylander, "Thermal modeling of small cage induction motors" Ph.D. Thesis, Chalmers University of Technology, 1995.
- [60] Joseph. P. H., "Thermal Analysis of a Permanent Magnet Assisted Synchronous Reluctance Motor Using Lumped Parameter Thermal Modeling," M.Sc. dissertation, University of Akron, USA, 2017.

- [61] J. Wang, F. Wang, and X. Kong, "Losses and thermal analysis of high-speed PM machine," in Proc. Joint Int. Conf. Power Syst. Technol.; IEEE Power India Conf. (POWERCON'08), Oct. 2008, pp. 1–5.
- [62] S. Timoshenko and J. Goodier, Theory of elasticity. New York, USA: McGraw-Hill, 1970.
- [63] D. K. Hong, B. C. Woo, J. Y. Lee and D. H. Koo, Ultra-high speed motor supported by air foil bearings for air blower cooling fuel cells, IEEE Trans. Magn., 48 (2)(2012) 871-874.
- [64] N. Ganatra and R. C. Ratel, Witness testing of API 610 centrifugal pumps and API 611 steam turbines, Proc. of the 27th International Pump Users Symposium, 71-79.
- [65] D. Hong, B. Woo and D. Koo, "Rotordynamics of 120 000 r/min 15 kW Ultra-High-Speed Motor," in *IEEE Transactions on Magnetics*, vol. 45, no. 6, pp. 2831-2834, June 2009.
- [66] J. D. Hartog, "Mechanical vibrations," New York: McGraw-Hall Book Company, 1947.
- [67] J. Luomi, C. Zwyssig, A. Looser and J. W. Kolar, "Efficiency Optimization of a 100-W 500 000-r/min Permanent-Magnet Machine Including Air-Friction Losses," in *IEEE Trans* on Ind. Applications vol. 45, no. 4, pp. 1368-1377, July-aug. 2009.
- [68] D. Krähenbühl, C. Zwyssig, H. Weser and J. W. Kolar, "A Miniature 500 000-r/min Electrically Driven Turbocompressor," in *IEEE Trans. on Ind. Applications*, vol. 46, no. 6, pp. 2459-2466, Nov.-Dec. 2010.
- [69] C. Gong, S. Li and T. Habetler, "High-Strength Rotor Design for Ultra-High Speed Switched Reluctance Machines," in *IEEE Transactions on Industry Applications*, vol. 56, no. 2, pp. 1432-1442, March-April 2020.
- [70] C. Gong, S. Li, T. Habetler and P. Zhou, "Acoustic Modeling and Prediction of Ultrahigh-Speed Switched Reluctance Machines Based on Multiphysics Finite Element Analysis," in *IEEE Trans. on Industry Applications*, vol. 57, no. 1, pp. 198-207, Jan.-Feb. 2021.
- [71] N. Uzhegov, E. Kurvinen, J. Nerg, J. Pyrhönen, J. T. Sopanen and S. Shirinskii, "Multidisciplinary Design Process of a 6-Slot 2-Pole High-Speed Permanent-Magnet Synchronous Machine," in *IEEE Trans. on Ind. Electrons.*, vol. 63, no. 2, pp. 784-795, Feb. 2016.
- [72] B. Koo, J. Kim and K. Nam, "Halbach Array PM Machine Design for High Speed Dynamo Motor," in *IEEE Transactions on Magnetics*, vol. 57, no. 2, pp. 1-5, Feb. 2021, Art no. 8202105.
- [73] S. -Y. Im, S. -G. Lee, D. -M. Kim, G. Xu, S. -Y. Shin and M. -S. Lim, "Kriging Surrogate Model-Based Design of an Ultra-High-Speed Surface-Mounted Permanent-Magnet Synchronous Motor Considering Stator Iron Loss and Rotor Eddy Current Loss," in *IEEE Transactions on Magnetics*, vol. 58, no. 2, pp. 1-5, Feb. 2022, Art no. 8101405.

- [74] X. Sun, Z. Shi, G. Lei, Y. Guo and J. Zhu, "Multi-Objective Design Optimization of an IPMSM Based on Multilevel Strategy," in *IEEE Trans. on Ind. Electron.*, vol. 68, no. 1, pp. 139-148, Jan. 2021.
- [75] DARPA. A Mechanical Based Antenna (AMEBA). Accessed: Dec. 19, 2016. [Online]. Available: https://govtribe.com/opportunity/federal-contract-opportunity/a-mechanicalbased-antennaameba-hr001117s0007.
- [76] W. Zhao, X. Wang, C. Gerada, H. Zhang, C. Liu and Y. Wang, "Multi-Physics and Multi-Objective Optimization of a High Speed PMSM for High Performance Applications," in *IEEE Transactions on Magnetics*, vol. 54, no. 11, pp. 1-5, Nov. 2018, Art no. 8106405.
- [77] G. Du, W. Xu, J. Zhu and N. Huang, "Effects of Design Parameters on the Multiphysics Performance of High-Speed Permanent Magnet Machines," in *IEEE Transactions on Industrial Electronics*, vol. 67, no. 5, pp. 3472-3483, May 2020.
- [78] C. Zwyssig, J. W. Kolar, and S. D. Round, "Megaspeed drive systems: Pushing beyond 1 million r/min," IEEE/ASME Transactions on mechatronics, vol. 14, pp. 564-574, 2009.
- [79] M. Salehi, H. Heshmat, J. F. Walton II, and M. Tomaszewski, "Operation of a mesoscopic gas turbine simulator at speeds in excess of 700,000 rpm on foil bearings" Proc. ASME Turbo Expo 2004, Power for Land, Sea, and Air, Vienna, Austria, June 14-17, 2004, Paper GT2004-53870.
- [80] A. Looser and J. W. Kolar, "An active magnetic damper concept for stabilization of gas bearings in high-speed permanent-magnet machines," IEEE Transactions on Industrial Electronics, vol. 61, pp. 3089-3098, 2014.
- [81] S. Li, Y. Li, W. Choi, and B. Sarlioglu, "High-speed electric machines: Challenges and design considerations," IEEE Transactions on Transportation Electrification, vol. 2, pp. 2-13, 2016.
- [82] X. Xie, "Comparison of Bearings: For the Bearing Choosing of High-speed Spindle Design," Dept. of Mechanical Engineering, U. of Utah, 2003.
- [83] ISO 1940-1, Mechanical vibration balance quality requirements of rigid rotors part 1: determination of permissible residual unbalance.
- [84] API Standard 611, General-purpose steam turbines for petroleum, chemical, and gas industry services, 5th edition, Washington, D.C. (2004).
- [85] M. K. Islam, S. Choi, Y. -K. Hong and S. Kwak, "Design of High-Power Ultra-High-Speed Rotor for Portable Mechanical Antenna Drives," in IEEE Transactions on Industrial Electronics, vol. 69, no. 12, pp. 12610-12620, Dec. 2022.

- [86] M. K. Islam, K. N. Tasnim, S. Choi, S. Kwak and Y. -K. Hong, "Performance Evaluation and Comparison of Three-Phase and Six-Phase Winding in Ultrahigh-Speed Machine for High-Power Application," in IEEE Transactions on Industrial Electronics, vol. 70, no. 5, pp. 4570-4582, May 2023.
- [87] M. K. Islam, K. N. Tasnim, S. Choi, S. Kwak and A. Arafat, "Designing High-Power Ultra-High-Speed Motor Using a New Multiphysics Multi-Objective Optimization Method for Mechanical Antenna Applications," in IEEE Access, vol. 10, pp. 106305-106323, 2022.
- [88] M. K. Islam, K. N. Tasnim, S. Choi and P. Singh, "Comparison of Three-Phase and Six-Phase High-Power Ultra-High-Speed Machine for Portable Mechanical Antenna," 2022 IEEE Applied Power Electronics Conference and Exposition (APEC), 2022, pp. 167-174.
- [89] M. K. Islam and S. Choi, "Multiphysics Optimization Model to Design High-Power Ultra-High-Speed Machine for Portable Mechanical Antenna Application," 2021 IEEE Energy Conversion Congress and Exposition (ECCE), 2021, pp. 4624-4631.
- [90] M. K. Islam and S. Choi, "Modeling and Design of a 6-Phase Ultra-High-Speed Machine for ELF/VLF Wireless Communication Transmitter," 2021 IEEE Applied Power Electronics Conference and Exposition (APEC), 2021, pp. 1062-1069.
- [91] M. K. Islam and S. Choi, "Rotordynamic Analysis of 500 000 r/min 2 kW Ultra-High-Speed Machine for Portable Mechanical Antenna," 2021 IEEE International Electric Machines & Drives Conference (IEMDC), 2021, pp. 1-7.
- [92] M. K. Islam, K. N. Tasnim, S. Choi, "Design of High-Efficiency Ultra-High-Speed Motor for Fuel-Cell Air Compressor Using an Amorphous Stator with Multiphase Winding" 2023 IEEE Applied Power Electronics Conference and Exposition (APEC), 2023.
- [93] M. K. Islam and S. Choi, "Analysis of Axial Temperature Variation Effect on the Performance of Five-Phase Permanent Magnet assisted Synchronous Reluctance Motor," 2020 IEEE Energy Conversion Congress and Exposition (ECCE), Detroit, MI, USA, 2020, pp. 1107-1114.