Synchronous Reluctance Machine (SynRM) in Variable Speed Drives (VSD) Applications

Theoretical and Experimental Reevaluation

REZA - RAJABI MOGHADDAM

Doctoral Thesis
Stockholm, Sweden 2011
Abstract

This thesis is comprehensively dedicated to the theoretical and experimental reevaluation of the Synchronous Reluctance Machine (SynRM). The thesis critically examines the important research that has been done on this subject since 1923 up to present time. A selection of these works that has been published in the literature can be found in the reference. These works have deeply contributed to the progress of this study and thesis. Hopefully, the work presented in this thesis will further contribute to extend the knowledge on this subject.

The thesis background and project motivations and scientific contributions are presented. A simple approach to derive the SynRM main characteristics and behavior is followed. The reluctance concept, the SynRM vector model, the main nonlinearity sources in the SynRM, a brief developing history of different rotor structures of the SynRM, a short comparison between SynRM and Induction Machine (IM), a new operating diagram of the SynRM, different operating conditions of the SynRM, a simple parameter estimator and the saturation effect are discussed.

An introduction to an evaluation of the different control strategies in SynRM is given. The performance of the SynRM is calculated in a characterization test. In this test the machine current angle for a certain load torque at a certain speed is varied and the machine performances are measured and also calculated at almost thermal steady-state conditions. By this means, different control strategies can be achieved and compared while the machine load condition is fixed.

Finding suitable rotor geometry for the SynRM has been a subject for major investigation since 1923 till now. This thesis will investigate the interior barrier (magnetic insulation layer) rotor structure of the SynRM using the Finite Element Method (FEM) based sensitivity analysis. The main goal is to search for the most important geometrical parameters of the rotor that affect the machine torque capability. Finding a simple and general rotor barrier shape is another target.

The Permanent Magnet assisted SynRM (PMaSynRM) is studied by means of the FEM. Different possible rotor structures, basic machine concept and model and potential improvements in the machine performance in comparison to the corresponding SynRM are explained and presented. The main aim here is to address accurately, qualitatively and quantitatively the main characteristics of such a machine by investigating its design using FEM.

A rough design method was first used to optimize a high performance SynRM rotor. The method is based on general rules that are governing the anisotropic structure of the SynRM rotor behavior. A heat-run test has been done on a prototyped SynRM and its corresponding IM and Interior Permanent Magnet (IPM) Machine to investigate the potential of the SynRM, under variable speed drive (VSD) supply conditions. This thesis gives the state of-art based on these measurements on the prototype SynRM and benchmarks its performance.

The main behavior and characteristics of an anisotropic structure, suitable for high performance SynRM rotor geometry design, is distinguished and
discussed. This issue is based on the combination of the already existing concepts and utilizes a previous advanced conceptual theory for anisotropic structure modeling that analytically explains the SynRM rotor anisotropic structure behavior. In this thesis, the carefully selected general rotor shape and some optimum distribution rules from analytical anisotropy theory are used to develop a novel FEM-aided fast rotor design optimization procedure for SynRM. The present study shows that by implementing this method the total number of geometries that must be modeled using FEM is around 10 and independent of the stator and rotor shapes.

Torque ripple is definitely an important issue in SynRM design similar to the IM. Torque ripple minimization of SynRM is discussed in this thesis. A method for ripple reduction in SynRM suitable for and compatible to the torque maximization procedure is introduced in this thesis. The decoupling between stator and rotor structure during the torque ripple minimization is the main goal. This can be achieved e.g. by the development of a general method that minimizes the ripple independent of the stator structure, specially the slots number of the stator, and the number of barriers in the rotor and rotor slots number. The torque ripple and interconnection to iron losses are briefly discussed as well.

Based on these design tools, a design that is a compromise between the final machine’s performance and simplicity of the rotor structure is studied as the improved machine design in this thesis. The fine tuned most promising design is prototyped and its performance compared with its corresponding IM, by measuring their performance through heat-run tests under variable speed supply operation.

The optimized machine design to achieve an optimum performance of the SynRM rotor geometry will be discussed in this thesis with some new ideas regarding the shape of the flux lines in the solid rotor. Naturally, to have an anisotropic structure the q-axis flux must somehow be blocked as much as possible and simultaneously the d-axis flux must flow smoothly. One possibility to achieve this is to align the barrier edges along the d-axis natural flux lines in the solid rotor. Implementing the new rotor general shape will help to further automate the design procedure, reduce the finite element modeling time and also improve the machine performance, compared to the previous designs. The optimized machine design procedure is evaluated by measurements. For this purpose a prototype of the final optimized machine design SynRM is manufactured. The performance of this machine is measured and compared with the improved machine design SynRM with the same machine structure.

The effect of the number of poles on SynRM performance is discussed in this thesis. Firstly, for each number of poles rotor geometry is optimized. Then, optimized machine performances have been compared over a wide speed range suitable for conventional VSD applications.

In this thesis some of the most important secondary effects in SynRM are briefly studied. Skew and torque quality, the possible effects of alternative voltage or current source supplies on torque and iron losses, the start-up and short-circuit locked rotor tests performed on the standard IM and the
prototype SynRM and the effect of eccentricity are described and investigated.

An overview comparison between IM and SynRM is given based on the distribution of losses for these machines at similar operating conditions. For this purpose, a high performance rotor structure for SynRM with standard sizes of 3kW, 15kW and 90kW is designed and discussed. The thermal performance of the SynRM in steady-state conditions as well as hot-spots in the machine is discussed by analyzing the measured machine temperatures. A detailed picture regarding the thermal performance of the SynRM machine is presented in this thesis. For this purpose, infra-red cameras, temperature sensors of type PT100 and infra-red, are used in different parts of the machine, rotary and stationary together with simulation and analysis of iron losses to provide a better picture that highlights the importance of this issue and experimentally points out some advantages and disadvantages of the SynRM in comparison to the IM from a thermal performance point of view.

A full scale performance evaluation of the SynRM in comparison to its counterpart the IM is given. All IM and SynRM motors in this thesis have the same standard stator structure for each size. The MTPA control strategy is also used in the measurements. Finally, all reported measurements in this thesis are summarized and analyzed.

Keywords:

Synchronous Reluctance Machine (SynRM), Torque, Torque Ripple, Design, Optimization, FEM, Control, Standard Size, Measurement, Performance Comparison, Induction Machine (IM), Interior Permanent Magnet Machine (IPM), Variable Speed Drive (VSD), General Purpose (GP), Field Oriented Control (FOC), Direct Torque Control (DTC), Heat Run, Test, Thermal, Operation Diagram, Circle Diagram, Field Weakening (FW), Maximum torque per Ampere (MTPA), Maximum Power Factor (MPF), Maximum Torque per kilo-Volt Ampere (MTPkVA), Maximum Efficiency (ME), Maximum Torque per Volt (MTPV), Reluctance, Saliency, Losses Distribution, Efficiency, Comparison, Benchmark, Barrier, Refinement, Bearing, Temperature, Lifetime, Saturation, Slot, Insulation Ratio, Slot Pitch, Eccentricity, Lock Rotor Current, Start up Transient, Skew, Pole Number, Size, Permanent Magnet Assisted Synchronous Reluctance Machine (PMaSynRM), Permanent magnet (PM), Compensation, Balance Compensation, Simulation, Power Capability, Inverter, Inverter Size, Permeance, Sensitivity Analysis.
Sammanfattning

Denna avhandling är dedikerad till den teoretiska och experimentella omvärderingen av den Synkrona Reluktans Maskinen (SynRM). I avhandlingen analyseras kritiskt den viktigaste forskningen som har gjorts på detta område sedan 1923 fram till nutid. Ett urval av relevanta verk som har publicerats i litteraturen kan hittas i referensen. Dessa verk har djupt bidragit till denna studie och avhandling. Förhoppningsvis kommer arbetet som presenteras i denna avhandling att bidra ytterligare till utökad kunskap inom detta område.

Avhandlingens bakgrund, projektets motiv och vetenskapliga bidrag presenteras först följt av ett enkelt sätt att härleda SynRMs viktigaste egenskaper och beteenden. Vidare diskuteras reluktanskonceptet, SynRM vektor modell, de viktigaste källorna till olinjäritet i SynRM, en kort utvecklingshistoria av olika rotorstrukturer i SynRM, en kort jämförelse mellan SynRM och Induktion Maskinen (IM), ett nytt operativsystem diagram över SynRM, olika driftstillstånd för SynRM, en enkel parameter estimator och mätningseffekt.


Hitta en lämplig rotor geometri för SynRM har varit föremål för stora utredningar sedan 1923 till nutiden. Denna uppsats kommer att undersöka olika typer av inre barriärer (magnetisk isoleringslager) hos rotorstrukturen i en SynRM med hjälp av känslighetsanalys baserat på Finita Element Metoden (FEM). Huvudmålet är att söka efter de viktigaste geometriska parametrarna på rotorn som påverkar maskinens vridmoment. Att hitta en enkel och generell form för rotor barriärerna är ett annat mål.

Permanentmagneten som assisterar SynRM (PMaSynRM) studeras med hjälp av FEM. Olika möjliga rotorstrukturer, grundläggande maskinkoncept och modeller och potentiella förbättringar av maskinens prestanda i jämförelse med motsvarande SynRM presenteras och förklaras. Det huvudsakliga syftet här är att beskriva korrekt, kvalitativt och kvantitativt de viktigaste egenskaper hos maskinen genom att undersöka konstruktionen med hjälp av FEM.


De huvudsakliga egenskaperna hos en anisotropisk struktur, lämpliga för högpresterande SynRM rotorgeometri design, identifieras och diskuteras. Denna fråga är baserad på en kombination av redan existerande begrepp och använder en tidigare avancerad konceptuell teori för anisotropisk struktur mo-
dellering som analytiskt förklarar SynRM rotorn anisotropiska strukturers beteende. I denna avhandling har den noga utvalda allmänna rotorformen och en del optimeringsregler om fördelning från analytisk anisotropisk teori använts för att utveckla en ny och snabb FEM-baserad rotoerdesign optimering för SynRM. Den aktuella studien visar att genom att tillämpa denna metod är det totala antalet geometrier som måste modelleras med FEM cirka 10 och oberoende av stator och rotor strukturererna.

Momentrippel är definitivt en viktig fråga för SynRM design precis som för IM. Momentrippel minimeringen av SynRM kommer att diskuteras i denna avhandling. En metod för rippel minskning av SynRM lämplig och kompatibel med vridmoments maxiimering kommer att presenteras i denna avhandling. Frikopplingen mellan stator - och rotorstrukturererna under momentrippel minimering är huvudmålet för undersökningen. Detta kan uppnås t.ex. genom utveckling av en generell metod som minimerar rippel oberoende av statorns struktur, speciellt antal spår i statorn, och antal barriärer i rotorn och antal spår i rotorn. Momentripplets koppling till järnförluster diskuteras kortfattat också.

Baserat på dessa designverktyg, har en design studerats som är en kompromiss mellan den slutliga maskinens prestanda och enkelhet i rotorns konstruktion, och presenterats som en maskin med en förbättrad design. Efter fininställning av den mest lovande designen har prototyper tillverkats och deras prestanda jämförts med motsvarande IM, genom värmepröv med variabelt varvtal.


Effekten av antalet poler på SynRM prestanda diskuteras i denna avhandling. För det första, för varje poltal optimeras en rotor geometri. Sedan har de optimerade maskinernas prestanda jämförts över ett brett varvtalsområde lämpligt för konventionella VSD applikationer.

I denna avhandling har några av de viktigaste sekundära effekterna i SynRM kortfattat studerats. Snedning och vridmoments kvalitet, de möjliga effekterna av alternativa spännings- eller ström källor på vridmoment och järnförluster, start- och låst rotor tester som utförts på standard IM och prototyp SynRM och effekten av excentriciteten beskrivs och undersöks.

En översiktlig jämförelse mellan IM och SynRM ges som bygger på dis-
tribution av förluster i dessa maskiner vid liknande driftsförhållanden. För detta ändamål har högpresterande rotorkonstruktioner av SynRM för standard storlekar 3kW, 15kW and 90kW utformats och diskuterats. Den termiska prestandan av en SynRM i stationärt tillstånd och hot-spots i maskinen diskuteras genom att analysera de uppmätta temperaturerna i maskinen. En detaljerad bild som visar SynRM maskinens termiska prestanda presenteras. För detta ändamål har infraröda kameror, temperaturgivare av typ PT100 och infraröd, använts i olika delar av maskinen, roterande och stationära, som tillsammans med simulering och analys av järnförluster ger en bättre bild och understryker vikten av denna fråga och experimentellt pekar ut några fördelar och nackdelar med SynRM i jämförelse med IM från en termisk prestanda synpunkt.

En fullskalig utvärdering av prestanda för SynRM i förhållande till sin motpart IM ges för olika optimerade storlekar. Alla IM och SynRM maskiner i denna avhandling har samma standard stator struktur för varje maskinstorlek. MTPA kontrollstrategi används också i mätningarna. Slutligen, alla rapporterade mätningar i denna avhandling sammanfattas och analyseras.
http://ganjoor.net/moulavi/shams/ghazalsh/sh1825/
To my beloved

Taraneh

and because of my parents who their talents have never been realized due to the Iran-Iraq war and life difficulties.
http://ganjoor.net/moulavi/shams/ghazalsh/sh1085/
Preface

This thesis is submitted to the Royal Institute of Technology (KTH), school of electrical engineering, Sweden in partial fulfillment of the requirements for the degree of Doctor of Philosophy (Ph.D.) in Electrical Engineering.

The research in Synchronous Reluctance Machine (SynRM) started for my part in September 2006 as my M.Sc. project subject at KTH. The first 5 months are included in my M.Sc. thesis work, the research results are summarized in a report [16], which was then followed up by a Ph.D project for 4 years.

The main purpose with a project like this is to promote new technologies such as Synchronous Reluctance Machines (SynRM) with a more competitive capability compared to the standard Induction Machine (IM) in terms of performance in Variable Speed Drives (VSD) operation for General Purpose (GP) applications. It is shown that such promotion can be achieved by the development of a suitable and fast design technique for the SynRM machine and then carrying out a full scale performance comparison between conventional IM of standard size and range and its counterpart which is the optimized SynRM machine.

It is my hope that this thesis can be of help and inspiration to engineers who work with research and development of electrical machines and to people who study possible new solutions for high efficient electrical machines. Apart from this thesis and report, results from the project have been published in a number of papers and articles listed in the introduction. These publications cover only a part of the results due to the time consuming procedure of writing journal and conference publications and limited project time.

Acknowledgments

I would like to thank my supervisors Chandur Sadarangani and Freddy Magnussen for help and constructive advices. Special thanks is given to Freddy Magnussen as well, because he was the inspirer of this project and project leader for 4 years, also thanks to Heinz Lendenmann who took over as project leader afterwards for one year. I also want to thank Ingo Stroka, Rahul Kanchan and Jere Kolehmainen for measurements, and Matti Mustonen and his team for providing the programmed inverters for SynRM operation. I am thankful to Joan Soler, Åke Andersson, Juni Ikäheim which and their teams for providing me with standard IMs data and prototyping the SynRM rotors.

I would also like to acknowledge ABB, who have through my superiors Christer Ovren, Heinz Lendenmann and Robert Chin financially supported my thesis work. I am indeed indebted to them for their support.

I am grateful to my parents and brothers who are the main motivation for me to follow my study and research towards Ph.D.; this is an uncountable and small attempt to realize their wish, as their talents could never been realized due to the
Iran-Iraq war and life difficulties.

Finally, it is my pleasure to thank my happy, beautiful and kind beloved Taraneh for her support and endless love. I would like to present her with this music: baiseteraganam, I am with stars. Listen to this here with Parvin (singer) or here with M. Esfehaani (singer).²

Resumé

Reza Rajabi Moghaddam was born in 1974 and received his diploma in mathematics, physics and chemistry from Ayandesazan High School (Mashhad, Iran) in 1993. He received B.Sc. and M.Sc. in electrical power engineering from Sharif University of Technology (Tehran, Iran) and the Royal Institute of Technology (KTH, Stockholm, Sweden) in 1997 and 2007, respectively.

Synchronous Reluctance Machine (SynRM) design for variable speed drives application was the subject of his MSc final project which took 5 months (full time). The characteristics of an anisotropic rotor structure and a synchronous reluctance machine performance and design based on this concept were fully investigated in this project. A sensitivity analysis was also performed together with the development of an optimization procedure (combined analytical and finite element method, FEM) suitable for high performance SynRM design. This study came up with some promising designs for prototyping, measurements and verification which was later processed as part of the Ph.D. studies.

During 1997 - 2005, the author worked in different industries in Iran as an electrical engineer in areas such as distribution system (MV and LV) design, lighting design, cubicle construction and design, installation (power plants, GIS-HV substation, etc.), offshore installation, field electrical engineer and consultant. Since 2006, he has been working at ABB Corporate Research in Västerås, Sweden, as research engineer in various development projects. Simultaneously, he started his study as Ph.D. student at KTH. This thesis summarizes his research work during this period.

His interests include electrical machines and drives with electrical machine design orientation.

Reza - Rajabi Moghaddam

27 May 2011
6 Xordad¹ 12780 Hezareh-Bareh² (HB) (1390 Hejri-Khorshidi)

¹Refer to Xordad see website: http://en.wikipedia.org/wiki/Iranian_calendars.
# Contents

## 1 Introduction

1.1 Background on the significance of the subject .......................... 1
   1.1.1 Electrical energy consumption and general purpose applica-
         tion (GP-A) ........................................... 2
   1.1.2 Electrical machine types .................................. 3
   1.1.3 Efficiency classes ....................................... 5
   1.1.4 Electrical machines operation and comparison between DOL
         and VSD systems ....................................... 6

1.2 Motivation and goal of the thesis ....................................... 7
   1.2.1 SynRM efficiency estimation based on IM efficiency .......... 8
   1.2.2 Challenges ........................................... 10

1.3 Organization of the thesis ............................................ 13

1.4 Publications and Scientific Contributions .......................... 17

## I SynRM: an Overview

## 2 SynRM basic principles

2.1 SynRM Theory ................................................... 22
   2.1.1 Reluctance concept ...................................... 23
   2.1.2 SynRM model and equivalent circuit ........................ 24
   2.1.3 SynRM main performances ................................. 25
   2.1.4 SynRM operation diagram ................................... 28
   2.1.5 Nonlinearities in SynRM and magnetization characteristic .. 30

2.2 SynRM rotor realization techniques ................................. 34
   2.2.1 Rotor geometry classification and development history .... 35
   2.2.2 SynRM and IM’s performance comparison .................... 37

2.3 SynRM basic control concepts .................................. 39
   2.3.1 Field weakening .......................................... 41
   2.3.2 Simple parameter estimator ................................ 43
   2.3.3 Overload capacity ....................................... 44
2.4 Evaluation of different control schemes .......................... 45
2.4.1 Description of the calculation method for SynRM−1−OptM characterization ......................................... 46
2.4.2 Characterization of the SynRM−1−OptM, analysis of calculation results ....................................................... 48

3 Permanent Magnet Assisted SynRM (PMaSynRM) 55
3.1 Brief basics on PMaSynRM ........................................... 56
3.2 Nature of PMaSynRM .................................................. 56
3.3 Analysis methods overview ............................................ 62
3.4 FEM study ............................................................... 65
3.5 Different approaches to the balance compensation of SynRM at MTPA ......................................................... 72
3.6 PMaSynRM machines performance and characteristic ............................................................. 76
3.7 Conclusion ............................................................. 79

4 SynRM: suitable barrier’s shape 83
4.1 A general barrier’s shape proposal .................................. 83
4.2 SynRM with one barrier ................................................ 85
4.2.1 Insulation ratio in the q-axis ...................................... 85
4.2.2 Radial position and the q-axis insulation ratio .............. 87
4.2.3 Insulation ratio in the d-axis ...................................... 88
4.2.4 Barrier leg angle ...................................................... 89
4.2.5 Optimum q-axis barrier positioning ............................ 90
4.3 One barrier geometry analysis conclusion ......................... 90

5 SynRM: Initial machine design 93
5.1 Rough rotor design concept of the SynRM .......................... 93
5.2 Airgap length ............................................................ 94
5.3 Rib dimension .......................................................... 95
5.4 Heat-run test on SynRM, IM and IPM .................................. 96
5.4.1 Torque capability .................................................... 97
5.4.2 Efficiency ............................................................ 97
5.4.3 Temperature rise .................................................. 99
5.4.4 Lifetime ............................................................. 100
5.4.5 Power ................................................................. 100
5.4.6 Power factor ......................................................... 101
5.4.7 Speed and load effect ............................................. 101

II SynRM Design with Multi-BARRIER Structure 103

6 Torque and Power Factor Optimization 105
6.1 Objective facts: the nature of the problem .......................... 107
CONTENTS

6.1.1 Suitable rotor arrangement and parameterization in micro-
scop ic term ........................................ 108
6.1.2 Parameterization in macroscopic term .................. 108
6.1.3 Optimization strategy and methodology ............. 108
6.2 Optimization of multiple flux barriers geometry .......... 111
  6.2.1 Insulation ratio in q-axis .......................... 111
  6.2.2 Insulation ratio in d-axis .......................... 112
  6.2.3 Proposed method evaluation and validity ............. 112
6.3 Number of barriers sensitivity analysis ..................... 113
  6.3.1 Number of barriers effect .......................... 114
  6.3.2 Number of layers effect .......................... 115
6.4 Objective facts: effect of electrical parameters on optimization . . . 116
  6.4.1 Example of optimization at one electrical operating point and
       effect of $k_{wq}$ on (Torque, IPF) and $(L_d - L_q, L_d/L_q)$ ........ 116
  6.4.2 Electrical parameters sensitivity analysis ............ 117

7 Torque Ripple Optimization 125
  7.1 Optimization strategy and methodology .................. 126
  7.2 Torque ripple minimization of multiple flux barrier geometry .... 127
  7.3 Other parameters affecting the optimization .............. 131
    7.3.1 Barriers permeance and governing rule sensitivity analysis
           with respect to ripple .......................... 131
    7.3.2 Independent torque and torque ripple optimization sensitivity
           analysis ........................................ 132
    7.3.3 Torque ripple and iron losses in SynRM ............. 132

8 SynRM: Improved machine design, fine tuning, validation and
     measurements 135
  8.1 Best rotor .......................................... 136
    8.1.1 Torque ........................................ 136
    8.1.2 Torque Ripple ................................... 137
  8.2 Fine tuning .......................................... 139
    8.2.1 Effect of eliminating the cut-off barrier ............. 139
    8.2.2 Rounding effect ................................. 140
    8.2.3 Ribs effect ..................................... 143
    8.2.4 Mechanical refinement ............................ 143
  8.3 Heat-run test on SynRM and IM ........................ 144
    8.3.1 Overall measured performances of the SynRM and the IM
           machines ....................................... 145
    8.3.2 Overall measured performances of the system and inverter . . 146
III Possible Improvements

9 SynRM: Optimized machine design

9.1 General theory ........................................ 150
  9.1.1 Field in the solid rotor and reluctance concept ........ 150
  9.1.2 Effect of shaft ................................... 151
  9.1.3 Analytical approach ................................ 151

9.2 Design procedure based on the field lines in solid rotor .... 152
  9.2.1 General rotor arrangement .......................... 152
  9.2.2 End point’s angles and rotor slot pitch ................ 153
  9.2.3 Barriers sizing and positioning ...................... 154

9.3 Simulation and comparison ................................ 156
  9.3.1 Step by step design and optimization example .......... 156
  9.3.2 Result comparison and discussion ...................... 157

9.4 Heat-run test on SynRM (improved and optimized machine designs)
   and IM .................................................. 159

10 SynRM: Pole number effect ................................ 161

10.1 Background, methods and tools .......................... 161
10.2 Design and optimization .................................. 163
10.3 Performance comparison ................................... 164
  10.3.1 Constant torque condition .......................... 166
  10.3.2 Constant temperature rise condition .................. 170
10.4 Final comments ......................................... 170

11 Secondary effects in SynRM ................................ 173

11.1 Optimum skew ......................................... 174
  11.1.1 Optimal skew angle and skewing steps analysis ....... 174
  11.1.2 Torque and ripple comparison before and after skew .... 175
  11.1.3 Comparison of different skewing calculation techniques .. 176

11.2 Supply effect .......................................... 177
  11.2.1 SynRM synchronization with voltage source .......... 180
  11.2.2 Effect of voltage and current sinusoidal sources on iron losses 182

11.3 Flux variation inside the rotor segments and iron losses .... 185

11.4 Expected SynRM behavior at start-up and short circuit locked rotor
   conditions ................................................. 189
  11.4.1 SynRM behavior at start-up .......................... 191
  11.4.2 SynRM behavior in short-circuit locked-rotor operation .. 191
  11.4.3 Short-circuit locked-rotor tests on IM and SynRM .......... 193

11.5 SynRM with eccentricity ................................ 196
  11.5.1 Electro-magnetic forces in SynRM with eccentricity ...... 197
  11.5.2 Total electro-magnetic forces in SynRM with eccentricity .. 197
  11.5.3 Iron losses in SynRM with eccentricity ................ 200
## IV Verification by Measurements on SynRM 201

### 12 Validation by measurements on SynRM 203

12.1 Introduction ..................................... 203

12.1.1 Loss distribution and power capability of IM - SynRM concepts 204

12.2 Design optimization of rotor geometry, 90kWM machine ............ 205

12.2.1 Promising designs and optimization .................................. 206

12.2.2 Mechanical stress calculation and ribs dimensioning ............ 209

12.2.3 Fine tuning ................................................. 210

12.3 Prototypes iron sheets ........................................... 212

12.4 An introduction to thermal performance of SynRM .................... 213

12.4.1 Infra-red picture of SynRM and IM under operation .......... 213

12.4.2 SynRM detailed different parts temperatures ....................... 214

12.5 Full scale performance evaluation of SynRM ....................... 219

12.5.1 SynRM and IM performance comparison at 1500 rpm .......... 219

12.5.2 SynRM and IM performance comparison, all measurements summary .................................................. 223

### 13 Conclusion and Future Work 225

### V Attachments 227

IM, SynRM and IPM Benchmarking 229

Bibliography 233

List of Figures 246

List of Tables 251

Index 253
Chapter 1

Introduction

This thesis is comprehensively dedicated to the theoretical and experimental reevaluation of the Synchronous Reluctance Machine (SynRM). The thesis critically examines the important research that has been done on this subject since 1923 [2] up to present time. A selection of these works that has been published in the literature can be found in [1] - [142]. These works have deeply contributed to the progress of this study and thesis. Hopefully, the work presented in this thesis will further contribute to extend the knowledge on this subject.

1.1 Background on the significance of the subject

Earth is the only well-known planet to mankind that has supported the evolution of life for more than 3.9 billion years\(^1\). During this time it has experienced some of the biggest natural disasters and the most difficult geological eras. Through all these, it has continuously provided suitable habitats and ecosystems for living creatures and specially through the last 3 million years for intelligent life evolution and mankind. Now the pollution and specifically \(CO_2\) emission [119] due to human activities are without any doubt affecting the performance of the earth ecosystem. In a sense, human activities push the edge of a new geological era in earth’s history. On the other hand, humans are still struggling to provide food, accommodation and shelter for a normal life for all people around the world by putting more pressure on natural resources such as water, air and soil by using an increasing amount of energy and developing plans on a national scale without considering the global effect. It is not entirely convincing to the author that even this simple task is going well and to the benefit of all the earth’s population, as well. Is it possible that humans with their activities destroy the habitability of the earth so that even they themselves can not survive? One thing is however sure and that is that earth will survive, the history of earth can prove that, albeit, with or without humans. Nobody knows

\(^{1}\)Refer to TV series: Planet Earth or Planet Earth, Earth Story and Earth: The Power of the Planet or Earth: The Power of the Planet.
the consequence of the earth system’s response and how powerful it will be and the question is, can humans survive from earth’s response? However, one urgent action has to be taken and that is to use the resources on earth in a more productive and efficient manner.

1.1.1 Electrical energy consumption and general purpose application (GP-A)

Energy is the key for creating the power for humans but it must be used effectively as it affects the environment on earth. Of course it has been the major factor even in human society conflicts and wars, as well. The rational step for reducing the destructive effect of humans on nature could be to further optimize their activities and utilize the produced energy in a more effective way by continuing to be innovative. Electrical energy provides this opportunity for humans to produce and transfer huge amount of energy to far and further distances and even makes habitable the inhabitable environments for living on earth. Producing and using electrical energy with higher efficiency can have a big impact on regulating the negative effects of human activities on the earth’s ecosystem.

The consumed electrical energy for different applications in Sweden is shown in Figure 1.1 [84] and [140]. The global figure will be very similar. Electrical motor systems consume/convert more than 60 % of the total electrical energy, half of this energy is consumed/converted by electrical motor systems that run simply a fan, pump or compressor. These are the most common applications of electrical machines and are the so-called General Purpose Application (GP-A). The GP-A are mostly bases for the so-called HVAC systems operation, where HVAC stands for Heating, Ventilation and Air-Conditioning. Typical task for HVAC is to control

![Figure 1.1: Consumed/converted electrical energy for different applications in Sweden [84], see also [119].](image-url)
1.1. BACKGROUND ON THE SIGNIFICANCE OF THE SUBJECT

pressure, flow, temperature or liquid level [119].

1.1.2 Electrical machine types

Induction Machine (IM) [33] traditionally is the most stronghold technology that has been used in HVAC-GP applications since its invention by Nicola Tesla for more than 160 years. The most common IM is the Alternating Current (AC) three phase, low voltage (LV), 4-pole, continuous duty, totally enclosed, fan cooled, asynchronous squirrel cage motor, see Figure 1.2. Generally, it is estimated that about 90 % of all electrical machines are of this motor type, see Figure 1.3, because of their low price, simple network connection or inverter drive, good availability, simple construction and high reliability [4].

IM in GP applications mostly belongs to the rotary, radial flux, AC and asynchronous class of electrical machines that utilizes the rotating field concept, discovered by Tesla, for torque production by connecting to the AC supply, see Figure

---

Figure 1.2: Different motor type classification [4] and [141].

Figure 1.3: Different motor type application in Sweden [84] and [140].
1.2. This machine provides torque by the slip concept so that the rotor rotates with a small slip frequency which is lower than the synchronous speed of the stator field. By this means, the machine can start from stand-still up to the nominal speed by connecting its terminal to the AC supply directly, the so-called direct on line (DOL) start. Also, it can provide any torque up to nominal torque by self-adjusting the speed of the shaft automatically and with a small change in its speed, see [33]. This features of the machine comes with a drawback as it generates losses in the rotor windings or casted squirrel cage that reduces the machine efficiency. Performance improvement of this machine has been achieved by manufacturers over the last decades by increasing the power density and taking into account the environmental aspects, as well.

Due to the introduction of the inverters in the market in the 1980’s some other technological possibilities for GP-A motors have come up to light. Inverters are interesting due to decreasing semiconductor costs and system energy savings. One of the solutions is using Permanent Magnet (PM) AC machines in GP-A, this provides a promising solution towards an ideal machine. The PM machine is a synchronous machine that can have PM material in the rotor and runs at synchronous speed, see Figure 1.2. This machine can have a similar stator as the IM. The torque production mechanism is based on the interaction between the PM flux and the stator rotating field. The main problem is the machine cost and the availability of the PM material, which is normally Neodymium-Iron-Boron (NeFeB) and is manufactured from one of the rare earth materials, Neodymium (Ne).

The Synchronous Reluctance Machine (SynRM) [2], uses the reluctance concept for torque production. It is an AC motor that preferably uses sinusoidally distributed windings in the stator, like in traditional induction motors or field-wound synchronous motors, and is consequently fed with sinusoidal waveform currents, see Figure 1.2. The rotor in its simplest form is made of iron only, with cleverly distributed air barriers so as to achieve a high inductance saliency ratio. In this form, the motor has very poor starting capability when line connected, but needs a controlled frequency converter for reliable operation. The synchronous reluctance motor was developed particularly in the 1960’s as a line-start synchronous AC motor for use in applications with synchronous speed requirements [24]. Such motors were for instance used in the textile industry, and fed by a common voltage source. The motors were obviously not individually controlled and there were no tough requirements on efficiency. The motor performance in terms of power density or efficiency was poor, but it was a cost effective motor which operated at synchronous speed. A lot of academic research was carried out on the synchronous reluctance motor and its control in the 1990’s. The main contributors to the understanding of the motor characteristics are probably professor Vagati and professor Fratta from Politecnico di Torino in Italy [54].

Apart from the synchronous reluctance motor there is also another motor type that relies entirely on the magnetic reluctance principle regarding torque generation. It is called switched reluctance motor, see Figure 1.2. It is a common mistake to confuse the synchronous reluctance motor with the switched reluctance motor,
which in fact is a completely different type of motor. While the synchronous reluctance motor uses a traditional stator concept similar to industrial induction motors, the switched reluctance motor has salient poles in both the stator and the rotor. The switched reluctance motor uses concentrated windings in the stator and is excited by currents with rectangular wave shapes. The electromagnetic models that are valid for the permanent magnet motor and the synchronous reluctance motor do not apply for the switched reluctance motor. Instead, non-linear parameterization of the motor must be applied [133]. The rotor experiences a large alternating flux component during normal operation. The motor is usually quite noisy due to its stepping nature, whereas the synchronous reluctance motor (SynRM) is as silent as a normal induction motor. In order to reduce the noise in low speed applications, the switch reluctance motor is often equipped with more than three phases. The motor is normally used in high speed aerospace applications due to its robustness. Vibrations are often dampened out at high speed, so noise is not a problem in aerospace application. Moreover the motor and the drive are designed as one system in such applications, so the complex and specific control of the switched reluctance motor is not a major concern. In industrial application however, where the motor and the drive are sold separately, the switched reluctance motor is not a preferred candidate. The switched reluctance motor treatment is out of the scope of this thesis.

1.1.3 Efficiency classes

Concerns on energy efficiency pushes the electrical machine’s technology to further improvement of the standard IMs performance, see Figure 1.4. This is an important

![Standard Efficiency Limits (% for 4-pole IM @ 50 Hz)](image)

Figure 1.4: IEC 60034-30 standard efficiency classes for 4-pole IM DOL operation @ 50 Hz.
CHAPTER 1. INTRODUCTION

step that increases the machine performance which is one of the major components in drive systems for industrial applications such as HVAC-GP. Such an effort has increased the manufacturers willingness to provide better and better machines over the last 100 years. Another solution that can provide better machines is to implement different technologies such as PM and SynRM machines, because possibility to improve the design of IM is already very saturated and further improvements is more or less related to material science to provide for example better iron sheets with lower losses and higher saturation level. In this sense, alternative available technologies specially for Variable Speed Drive (VSD) operation of electrical machines, are capturing more and more attention nowadays.

1.1.4 Electrical machines operation and comparison between DOL and VSD systems

Optimal operation of electrical motors can be understood from the context of the system operational demands. Normally these machines are part of an electro-mechanical system. Optimal performance of the system is closely connected to the system output parameter performance which is normally a non-electrical parameter such as flow, pressure etc. This is achieved by utilizing a suitable control of the system input parameters which, normally is influenced by an electrical parameter. Therefore, suitable operational control is an important issue for electro-mechanical systems driven by electrical machines. It is dependent of the required control performance, system cost and energy saving.

Electrical machines provide different control opportunities for motor driven systems. One simple and well-known control is on/off operation of the motor. In this case when the system control parameter goes below a minimum level the motor is turned on and it is turned off when it exceeds the maximum level. When the system operates at its nominal operating point, the system efficiency is normally high. However, at partial load condition the numbers of turn on/off increases and the efficiency is reduced. Stepwise control is another method of controlling an electro-mechanical motor system. This is useful when the system is large. In this case the system consists of several small units working in parallel for providing the controlled parameter variation demands. Each sub-system is controlled in a similar manner to on/off control. This solution can potentially increase the installation cost. The output parameter can be controlled in steps. If continuous control is needed then one of the sub-systems can be controlled in VSD operation which increases the cost further. Another control can be achieved by using mechanical means for continuous control of the output parameter, e.g. a mechanical valve, where the on/off and stepwise controls can not be used. In this case the drawback is the high energy losses in the drive system, because normally the electrical motor is directly connected to the grid and operates with full nominal power. This type of control is analogous to the case when a car is run at full power of the engine while the speed is controlled by its brakes! VSD operation of the system has been an alternative solution since the 1980s when power electronic based inverters were introduced. In
1.2 Motivation and goal of the thesis

As is discussed, the VSD operation of motor systems can save energy as well as provide better and optimal control over the process. The IM can also be redesigned this condition an inverter runs the electrical motor with variable speed by means of electrical control that is implemented in the inverter. The system cost can be high in this case as well. However, this method reduces the power loss by providing just the required power for the process. It also brings the opportunity to optimally control the motor-drive system [58] and [119].

VSD operation of the motor driven system not only reduces the system losses but also provides this opportunity to use alternative motor technologies that do not have the starting capability of the IM, such as PM and SynRM machines. The energy saving in a VSD drive in comparison to the DOL drive system can be schematically shown based on Figure 1.5. In this figure pump number 1 is DOL and pump number 2 is VSD driven. Point A in Figure 1.5 (right) diagram represents the nominal operating point of the system. If the required flow rate $Q$ is reduced to 60 % of the nominal flow rate, point C, then in the case of the DOL drive system it is achieved by throttling the valve and consequently the operating point of the pump will be at point B in this diagram. In this case extra losses occur over the valve corresponding the extra head from point C to point B. However, in the case of VSD operation, the machine speed is reduced to 80 % of its nominal value, which crosses the system curve at 60 % of the nominal flow rate at point C. In such a condition the required power and provided power by the machine will be very close to each other and without the extra losses over the valve. As is obvious, a large amount of energy can be saved by VSD operation of the pump.

Figure 1.5: DOL and VSD controls comparison of a pump driven by electrical motor in a pipeline system with head, see also [58] and [119].
suitably to cope with this type of application because the starting torque is not required any more in VSD. Another possibility is to use other types of machines such as the SynRM. In this case the machine rotor circuit is eliminated and extra gain in system energy efficiency can be achieved. In the SynRM the rotor cage does not exist therefore, the losses in the machine are reduced and consequently the winding temperature is reduced as well. Specially, the SynRM is interesting because the torque capability of this machine for a certain current can be as high as in the IM [23]. This means that for the same power dissipation the SynRM can deliver more shaft power than an IM of the same size, as well. These issues are deeply discussed in this thesis.

1.2.1 SynRM efficiency estimation based on IM efficiency

The cage elimination effect on the SynRM performance in comparison to the IM can be estimated by using a simple approach. When an induction motor runs at no-load, its rotor rotates in synchronism with the stator magnetic field. When the motor is loaded, the rotor rotates slower than the stator magnetic field, so-called asynchronous operation. The speed difference is called slip, and the slip increases with load. The slip is defined as:

\[ s = \frac{n_s - n_r}{n_s}, \]  

where \( n_s \) is the synchronous speed and \( n_r \) is the rotor speed. The rotor losses of an induction motor, e.g. the dominating joule losses in the cage, the iron losses in the core and the friction losses, can be expressed as a function of the output power \( P_{IM}^{out} \) and the slip, as given by:

\[ P_{slip} = \frac{s}{1 - s} \cdot P_{IM}^{out}. \]  

This slip loss component does not exist in a SynRM, since it is a synchronous motor, and is the most characteristic loss difference between the two motor types. The efficiency of an induction motor can be expressed as:

\[ \eta_{IM} = \frac{P_{IM}^{out}}{P_{IM}^{in}}, \]  

where \( P_{IM}^{in} \) is the input power of the induction motor. If a synchronous reluctance motor is run at the same working point as in Equation (1.3), and it is assumed that the only loss difference between the motor types is the slip loss component, then the efficiency of the synchronous reluctance motor can be expressed:

\[ \eta_{SynRM} = \frac{P_{IM}^{out}}{P_{IM}^{in} - P_{slip}}. \]
1.2. MOTIVATION AND GOAL OF THE THESIS

Figure 1.6: The slip of standard induction motors (IE1) at rated load (catalog values) and the estimated efficiency difference at rated load, between synchronous reluctance motors and induction motors with four poles, using Equation (1.5).

The efficiency of the synchronous reluctance motor can also be expressed as a function of the efficiency of the induction motor and the slip, if Equations (1.2), (1.3) and (1.4) are combined, which can be written:

\[ \eta_{SynRM} = \frac{\eta_{IM}}{1 - \eta_{IM} \cdot \frac{s}{1-s}}. \]  

(1.5)

It is now possible to estimate the efficiency of a well-designed synchronous reluctance motor, just by knowing the efficiency and the slip data of the induction motor, when similar stators are used for the motors. The estimated efficiency difference between a synchronous reluctance motor and an induction motor, with equal 4-poles stators and using catalog values for the induction motor and Equation (1.5), is given in Figure 1.6. The IM slip values are shown in this figure as well. Since the slip decreases with increasing motor size, the relative rotor loss component also decreases for the induction motors, resulting in a smaller efficiency difference at higher rated power. Consider that in such an operation, the SynRM temperature will be lower than the IM in both the machine windings as well as the shaft and bearings. This issue will be discussed deeply in this thesis using measurements as background material. These improvements can be directly utilized to reduce the size of the SynRM machine for the same delivered power as the IM with higher reliability due to lower temperatures in the critical parts, such as windings and
bearings. Consequently, the required material for production is reduced as well as the production cost. This can increase productivity of the raw material that are used in electrical machines.

A potential drawback of the synchronous reluctance motor compared to the induction motor is that it needs, under certain circumstances, a larger inverter, because of its slightly lower power factor. This is especially sensitive for high speed applications, where the inverter cost dominates over the motor cost. The permanent magnet motor does not have this drawback, since its power factor is typically even higher than that of the induction motor.

1.2.2 Challenges

The challenge is to show that such a high performance from a well-designed SynRM is achievable. Three different approaches for SynRM design are discussed in this thesis. The goal is to design the machine taking into consideration the important facts and parameters and simultaneously reducing the design steps and computation time for the optimization. Afterward, measurements are used for validation and for further investigations of achievable performances of the SynRM in comparison to its counterpart IM for the most common IEC standard power range from 3 to 90 kW. For this purpose, firstly, the design procedures are developed and then secondly, they are used to design high performance SynRM rotors for three different standard IM stators with nominal power range of 3, 15 and 90 kW at 1500 rpm. In this thesis these machines are referred to as 3kWM, 15kWM and 90kWM machines, respectively. The performance of these prototyped SynRMs and their counterpart IMs with the same stator are measured and compared by running heat-run tests on each machine at similar operating conditions. These tests will give a clear picture regarding achievable performance of the SynRM in comparison to the IM.

The challenge in developing the design solution for SynRM can be shown by summarizing the measured performance comparison between well-optimized and non-well-optimized solutions of the SynRM for 90kWM machines. Two SynRM rotor designs are compared here. First, a rough optimized machine, its performance and detailed design can be found in [134], which is referred to as the Initial Machine, $\text{SynRM IniM}$, in this thesis, see its rotor structure in Figure 1.7 (left and middle). Another rotor is designed for this machine by using the developed design method in this thesis referred to as the Optimized Machine of the SynRM, $\text{SynRM OptM}$. The rotor structure of this machine is shown in Figure 1.7 (right). Both machines have the same standard stator structure as the corresponding IM with nominal values of 90 kW power at 1500 rpm.

Advantages of the $\text{SynRM IniM}$ rotor in comparison to the $\text{SynRM OptM}$ are as follows:

Cleverly, the radial and tangential ribs are combined together by introducing some intermediate ribs, as is shown in Figure 1.7 (middle), and this reduces the
1.2. MOTIVATION AND GOAL OF THE THESIS

total number of ribs required for the mechanical stability of the rotor. The rotor structure is mechanically robust.

The cut-off barrier airgap span (equivalent angle $\approx 6 \times$ stator slot pitch) always covers the slots of one phase of the stator winding. The corresponding teeth of these slots are not active in conducting the d-axis flux. Therefore, the q-axis flux is blocked without disturbing the d-axis one. The rotor slot pitch is almost constant and this is effective in torque ripple reduction.

The insulation ratio in the q-axis, see chapter 4, is around 0,83, which is a little above the optimum value (0,5 – 0,7), see chapter 12.

Disadvantages of the SynRM IniM rotor in comparison to the SynRM OptM are as follows:

The insulation (air) is not distributed optimally between barriers. The barrier’s radial position in the q-axis ($Y_{qk}$ parameter, see chapter 4) is not suitably chosen, and the segments widths are not kept constant. Consequently, the first segment iron is not used optimally and the last segment is stressed by a high flux density value.

The amount of insulation in the d-axis is higher than in the q-axis. Eliminating the tangential ribs increases the torque ripple. The active stress on each rib is mostly of the cross-stress (cutting) type instead of the pulling type. This is due to the positioning of the ribs in the tangential direction instead of the radial direction.

The torque ripple is not acceptable in the SynRM IniM, especially when 72 slots are used in the stator and the resultant stator magneto motive force (MMF) curve shape is very close to sinusoidal. The large span of the cut-off barrier affects the torque ripple and contributes to more friction.

Figure 1.7: (left) and (middle) initial machine, SynRM IniM, see [134], and (right) optimized machine, SynRM OptM, which is obtained by using the developed design method in this thesis. SynRM rotor structures are for 90kW M machine. The corresponding standard IM nominal power is 90 kW at 1500 rpm. The flux density in (left) figure varies from 0 – 2,5 T, not calibrated, for colors from black to white for MTPA controlled operation at 130 kW.
CHAPTER 1. INTRODUCTION

Table 1.1: Heat-run test measurements on SynRM Initial Machine, SynRM IniM [134], Optimized Machine, SynRM OptM in this thesis and IM machines, 90kW machine.

<table>
<thead>
<tr>
<th>Ref. IM Pout at 1500 rpm [kW]</th>
<th>90</th>
<th>90</th>
<th>90</th>
<th>90</th>
<th>90</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operation Type</td>
<td>DOL</td>
<td>DOL</td>
<td>DOL</td>
<td>DOL</td>
<td>VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>1482</td>
<td>1500</td>
<td>1500</td>
<td>1501</td>
<td>1500</td>
</tr>
<tr>
<td>fs [Hz]</td>
<td>50</td>
<td>50</td>
<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>1.213</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>ns: no. of cond. / slot</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
</tr>
<tr>
<td>cs: winding connection</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>231</td>
<td>231</td>
<td>254</td>
<td>231</td>
<td>257</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>160</td>
<td>192</td>
<td>176</td>
<td>168</td>
<td>161</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>4960</td>
<td>5390</td>
<td>4680</td>
<td>3570</td>
<td>3458</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>1644</td>
<td>2954</td>
<td>2264</td>
<td>1827</td>
<td>1587</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>600</td>
<td>600</td>
<td>600</td>
<td>600</td>
<td>600</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>2716</td>
<td>1836</td>
<td>1816</td>
<td>1143</td>
<td>1271</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>579</td>
<td>572.6</td>
<td>572.5</td>
<td>573.2</td>
<td>573</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>89.9</td>
<td>90.0</td>
<td>89.9</td>
<td>90.1</td>
<td>90</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>94.8</td>
<td>95.3</td>
<td>94.6</td>
<td>93.7</td>
<td>93.5</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>110.7</td>
<td>132.8</td>
<td>133.9</td>
<td>116.7</td>
<td>124.4</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>94.8</td>
<td>94.3</td>
<td>95.1</td>
<td>96.2</td>
<td>96.3</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0.856</td>
<td>0.718</td>
<td>0.706</td>
<td>0.802</td>
<td>0.751</td>
</tr>
<tr>
<td>Efficiency \cdot PF1</td>
<td>0.811</td>
<td>0.677</td>
<td>0.671</td>
<td>0.772</td>
<td>0.723</td>
</tr>
<tr>
<td>1 / (Efficiency \cdot PF1) [*]</td>
<td>1.23</td>
<td>1.48</td>
<td>1.49</td>
<td>1.30</td>
<td>1.38</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1,ns=1)</td>
<td>1.81</td>
<td>1.49</td>
<td>1.63</td>
<td>1.70</td>
<td>1.78</td>
</tr>
<tr>
<td>Inverter relative size [%]</td>
<td>100.0</td>
<td>110.8</td>
<td>120.8</td>
<td>105.1</td>
<td>112.2</td>
</tr>
<tr>
<td>Housing Temp. Rise [K]</td>
<td>36</td>
<td>43</td>
<td>37</td>
<td>29</td>
<td>25</td>
</tr>
<tr>
<td>Rotor Tuch Temp. Rise [K]</td>
<td>97</td>
<td>76</td>
<td>63</td>
<td>47</td>
<td>43</td>
</tr>
</tbody>
</table>

The iron is not optimally distributed between the iron segments, as well. The amount of the insulation in the q-axis is higher than the optimum value although the ribs are eliminated but the power factor is still poor, this issue will be investigated later. The number of rotor layers (number of the iron segments + number of the insulation barriers) is 10, which is high.

The measurements on SynRM IniM prototype do not show improvement performance when compared to the IM, see Table 1.1.

The performance of these machines are compared with the IM by heat-run test. The test results are summarized in Table 1.1. Clearly, the SynRM design is an important issue. Without proper design this machine cannot compete with the corresponding IM. The expected efficiency improvement for SynRM 90kW/M machine
at 90 kW is around 1.5% – units, see Figure 1.6. However, the SynRM IniM does not show any improvement, while the SynRM OptM does. How this is achieved is the main topic of this thesis.

The comparison between SynRM OptM and IM measured performances show that SynRM machine can provide the same shaft power as IM but with 50 K lower rotor temperature, 6 K lower winding temperature and 18 K lower bearings temperature and simultaneously operate at higher efficiency, by around 1.5% – units. The torque capability of the IM and SynRM are very close, see row T/I in Table 1.1. These come at a slightly higher cost in apparent power required for the SynRM by 5%.

1.3 Organization of the thesis

This thesis contains an introduction, chapter 1, and five parts. The introduction presents an outline of the thesis, the thesis background and project motivations and scientific contributions. Each part includes several chapters, which are described briefly here. Each chapter has an introduction itself that mainly describes the literature review and briefly the contents of the chapter, as well.

Part I provides an overview on SynRM machines that includes chapters 2, 3, 4 and 5. The contents of this part are as follows:

Chapter 2: SynRM basic principles.

The purpose of this chapter is to present a simple approach to derive the Synchronous Reluctance Machine (SynRM) main characteristics and behavior. The reluctance concept, the SynRM vector model, the main nonlinearity sources in the SynRM, a brief developing history of different rotor structures of the SynRM, a short comparison between SynRM and IM, a new operating diagram of the SynRM, different operating conditions of the SynRM, a simple parameter estimator and the saturation effect are discussed in this chapter.

An introduction to an evaluation of the different control strategies in SynRM is given. The performance of the SynRM is calculated in a characterization test. In this test the machine current angle for a certain load torque at a certain speed is varied and the machine performances are calculated at thermal steady-state conditions. By this means, different control strategies can be achieved and compared while the machine load condition is fixed.

Chapter 3: PMaSynRM.

The PMaSynRM is studied by means of the finite element method (FEM). Different possible rotor structures, basic machine concept and model and potential improvements in the machine performance in comparison to the corresponding SynRM are explained and presented. The main aim here, is not to have a classified design procedure approach and optimization, which needs more study and time,
but to address accurately, qualitatively and quantitatively the main characteristics of such a machine by investigating the design using FEM.

Chapter 4: SynRM: an investigation around suitable shapes of the barrier.

Finding a suitable rotor geometry for the SynRM has been a subject for major investigation since 1923 till now. This chapter will investigate the interior barrier (magnetic insulation layer) rotor structure of the SynRM using the Finite Element Method (FEM) based sensitivity analysis. The main goal is to search for the most important geometrical parameters of the rotor that affect the machine torque capability! Finding a simple and general rotor barrier shape is another target. The simplest rotor that has just one barrier will be used in the investigation.

Chapter 5: SynRM: Initial machine design.

A rough design method has been used to optimize a high performance SynRM rotor. The method is based on general rules that are governing the anisotropic structure of the SynRM rotor behavior. This machine will be studied to demonstrate the effect of airgap length and the dimension of the ribs on its performance. A heat-run test has been done on a prototyped SynRM and its corresponding IM and Interior Permanent Magnet (IPM) Machine to investigate the potential of the SynRM, under variable speed supply (VSD) conditions and to compare its performance to its counterparts, the IM and the IPM machines. This chapter gives the state-of-art based on measurement on the first prototype SynRM and benchmarks its performance.

Part II presents a novel method for SynRM design with multi-barrier structure that includes chapters 6, 7 and 8. The contents of this part are as follows:

Chapter 6: Torque and power factor optimization of the SynRM.

The main behavior and characteristics of an anisotropic structure, suitable for high performance SynRM rotor geometry design, is distinguished and discussed here. This issue is based on the combination of the already existing concepts and utilizes a previous advanced conceptual theory for anisotropic structure modeling that analytically explains the SynRM rotor anisotropic structure behavior. In this chapter, the carefully selected general rotor shape and some optimum distribution rules from analytical anisotropy theory are used to develop a novel FEM-aided fast rotor design optimization procedure for SynRM. The present study shows that by implementing this method the total number of geometries that must be modeled (FEM) is around 10 and independent of the stator and rotor shapes. The optimum number of barriers that gives the best rotor structure with minimum complexity is studied in this chapter as well. Another goal in this chapter is to investigate the influence of the electrical parameters on the final shape of the optimized SynRM’s rotor geometry.

Chapter 7: Torque ripple optimization.
1.3. ORGANIZATION OF THE THESIS

Torque ripple and other secondary effects such as rotor iron losses, vibration and noise, are definitely important issues in SynRM design similar to the IM. Torque ripple minimization of SynRM will be discussed in this chapter. A method for ripple reduction in SynRM suitable for and compatible to the torque maximization procedure, that is discussed in chapter 6, will be introduced in this chapter. The decoupling between stator and rotor structure during the torque ripple minimization is the main goal. This can be achieved e.g. by the development of a general method that minimizes the ripple independent of the stator structure, specially the slots number of the stator, and the number of barriers in the rotor or rotor slots number. Furthermore, it will be shown that torque maximization and torque ripple minimization can be achieved independently in the SynRM design. The torque ripple and interconnection to iron losses is briefly discussed in this chapter as well.

Chapter 8: SynRM: Improved machine design, fine tuning, validation and measurements.

Based on the design tools that are discussed in chapters 6 and 7, a design that is a compromise between the final machine’s performance and simplicity of the rotor structure, is studied as the improved machine design in this chapter. The fine tuned most promising design is prototyped and its performance compared with its corresponding IM, by measuring their performance through heat-run tests under variable speed supply operation.

Part III presents possible improvements for SynRM design with multi-barrier structure that is discussed in part two. This part includes chapters 9, 10 and 11. The contents of this part are as follows:

Chapter 9: SynRM: Optimized machine design.

The optimized machine design to achieve an optimum performance of the synchronous reluctance machine (SynRM) rotor geometry will be discussed in this chapter with some new ideas regarding the shape of the flux lines in the solid rotor. Naturally, to have an anisotropic structure the q-axis flux must somehow be blocked as much as possible and simultaneously the d-axis flux must flow smoothly. One possibility to achieve this is to align the barrier edges along the d-axis natural flux lines in the solid rotor. Fortunately, this shape can be expressed by a simple mathematical equation using N. E. Joukowski airfoil potential function and thus can be used for optimization purposes. Implementing the new rotor general shape will help to further automate the design procedure, reduce the finite element modeling time and also improve the machine performance, compared to the previous designs. The optimized machine design procedure is evaluated by measurements. For this purpose a prototype of the final optimized design SynRM machine is manufactured. The performance of this machine is measured and compared with the improved design SynRM machine, see chapter 8, with the same machine structure.

Chapter 10: SynRM: Pole number effect.
CHAPTER 1. INTRODUCTION

The effect of the number of poles on SynRM performance, is discussed in this chapter. Firstly, for each number of poles a rotor geometry is optimized by methods that have been discussed in chapter 9. Then, optimized machine performances have been compared at constant torque condition over a wide speed range suitable for conventional variable speed drive (VSD) applications. Separately, a comparison at constant temperature rise and constant speed condition is also performed and presented.

Chapter 11: Secondary effects in SynRM.

In this chapter some of the most important secondary effects in SynRM are briefly studied. Skew and torque quality, the possible effects of alternative voltage or current source supplies on torque and iron losses, the start-up and short-circuit locked rotor tests performed on the standard IM and the prototype SynRM and the effect of eccentricity are described and investigated.

Part IV presents a full scale verification of the theoretical findings through measurements on SynRM for 3kW,M, 15kW,M and 90kW,M machines. A summary of the thesis is also presented. This part includes chapters 12 and 13. The contents of this part are as follows:

Chapter 12: Validation by measurements.

This chapter contains an introduction and four important studies. Introduction in section 12.1 gives an overview comparison between IM and SynRM based on the distribution of losses for these machines at similar operating conditions.

The design optimization is discussed in details for SynRM 90kW,M machine in section 12.2. The final prototyped iron sheet of this machine can be found in section 12.3.

The thermal performance of the SynRM in steady-state conditions as well as hot-spots in the machine are discussed in section 12.4 by analyzing the measured machine temperatures. A detailed picture regarding the thermal performance of the SynRM machine is presented in this section. For this purpose, infra-red cameras, temperature sensors, PT100 and infra-red are used in different parts of the machine, rotary and stationary to provide a better picture that highlights the importance of this issue and experimentally points out some advantages and disadvantages of the SynRM in comparison to the IM from a thermal performance point of view.

In section 12.5 a full scale performance evaluation of the SynRM in comparison to its counterpart the IM is given for different nominal power. All IM and SynRM machines in this section have the same standard stator structure for each of 3kW,M, 15kW,M and 90kW,M machines. The almost MTPA control strategy is also used in the measurements presented in this section. Finally, all reported measurements in this thesis are summarized and analyzed in this section.

Chapter 13: Conclusion and future work.

The thesis is concluded in this chapter and some future works are presented.
Part V includes the bibliography and index of the thesis.

1.4 Publications and Scientific Contributions

The work presented in this thesis has come up with three publications up to now as follows:

  This conference paper summarizes the chapter 2 contents of this thesis.

  This journal paper summarizes the chapters 2 and 5 contents of this thesis.

  This conference paper summarizes the chapter 4 contents of this thesis.

In addition to these publications the scientific contributions of this thesis can be summarized as follows:

- Developing a unique operation diagram for SynRM machine, see Figure 2.6 in chapter 2, which can be used for any kind of machine such as IM, IPM and PM as well.

- Developing a fast torque/power factor, see chapters 6 and 9, and torque ripple/iron losses, see chapter 7, optimization procedure of the SynRM rotor structure using FEM based method.

- Evaluating different control strategies for SynRM, see chapter 2.

- Investigating the effect of introducing permanent magnet in the barriers of the SynRM from different and new perspective and evaluating the machine performance improvement. Such a machine is called Permanent Magnet assisted Synchronous Reluctance Machine, see chapter 3.
• Investigating the effect of machine pole number on the final performance of the SynRM with standard size, see chapter 10.

• Comparing full scale overall machine performance (theoretically and experimentally) between IM, IPM and SynRM, see Table 1.1 in chapter 1, Figure 2.11 in chapter 2, Figure 3.14 in chapter 3, Table 5.1 in chapter 5, Table 8.3 in chapter 8, Table 9.2 in chapter 9, Figure 10.5 in chapter 10 and the whole of chapter 12.

• Developing a fast torque ripple evaluation method for skewed machine, see chapter 11.

• Investigating the flux variation inside the rotor segments of the SynRM and the iron losses in the rotor as well as effect of source supply (voltage or current) on the machine performance. See chapter 11.

• Investigating transient effects in SynRM at start-up and locked rotor conditions. See chapter 11.
Part I

SynRM: an Overview
Chapter 2

SynRM basic principles

The purpose of this chapter is to present a simple approach to derive the Synchronous Reluctance Machine (SynRM) main characteristics and behavior.

First of all, an overview regarding the reluctance concept is discussed, then a machine model based on Park’s equations will be derived, which uses some modeling concepts for major machine characteristics such as iron losses, leakages and reluctance behavior.

Consequently based on the machine vector model some suitable equations that describe the machine performances such as torque, power factor and magnetizing current and airgap flux relations in steady-state will be derived. A simple magnetization model will be introduced. The main nonlinearity sources in the machine behavior and their effect on machine magnetizing inductances will be investigated. Briefly, a history of the development of different rotor geometry structures and comparison will be presented. A short comparison between SynRM and Induction Machine (IM) will be done to give the reader a feeling about the expected performance of SynRM.

In this chapter a new operating diagram is presented that demonstrates the behavior of the machine main performance parameters, such as torque, current, voltage, frequency, flux, power factor and current angle, all in one graph [17]. This diagram can be more effective than the well-known circle diagram that is used in e.g. [15], for analyzing SynRM. Different operating conditions, which are achievable under current control of the machine, are briefly developed and presented using this quite new operation diagram of SynRM [16], [17].

The power factor (PF) of the SynRM is fundamentally dependent on the saliency ratio (ξ) defined as the ratio of the d-axis over the q-axis inductances, regarding the SynRM’s saliency and PF refer to [5], [9], [15], [17] and [32]. Furthermore, it will be theoretically shown that estimation of the machine saturation level is possible in real-time by using the machine power factor calculation or measurement, under steady-state operation conditions [17]. The saturation effect [3], [10], [11], [17] and [31] is also discussed and demonstrated by Finite Element Method (FEM)
The SynRM, for its torque production, utilizes the reluctance concept and rotating sinusoidal Magneto Motive Force (MMF), which is produced by the traditional IM stator. The reluctance torque was discovered early and it can be traced back to before 1900, [2] - [4]. The first theoretical and technological attempt to realize the SynRM was made by Kostko in 1923 [2], the rotor’s schematic is shown in Figure 2.1.

SynRM under closed-loop control can easily be controlled and operated, due to the completely new possibilities which are achieved through the development of power electronic based drives [5] - [13]. Thus the drawback of this machine under direct operation and supply, especially stability and start up torque, can be overcome. Both field oriented (FOC) [3], [9] and [10] and direct torque control (DTC) or similar methods [5] - [7] have been presented in the literature for SynRM operation. One of the simple field oriented control possibilities is based on the machine current angle control [3], [5] and [8] - [18].

It has been shown that the SynRM under closed-loop control has some advantages compared to the IM, which is the powerful industrial competitor to the
2.1. SYNRM THEORY

SynRM, when variable speed operation with high efficiency is demanded [2], [3], [15], [16] and [19] - [29]. Simplicity and adaptability in production [27] and operation [7], [29], higher efficiency and torque density, higher overload capacity [3] and lower rotor temperature are some of the SynRM advantages in comparison to the IM.

These advantages make the SynRM technology attractive even in high speed applications [3], [27] and [28] or as generators [30]. The most important disadvantage of this machine is its poor power factor (PF), which leads to an over-sizing of the inverter [7], [19]. Looking for a solution for the poor power factor of the SynRM, introduces a new kind of SynRM assisted with permanent magnets. This new possibility is known as Permanent magnet assisted SynRM (PMaSynRM) [7], [15], [29] and [31].

2.1.1 Reluctance concept

The main idea can be explained using Figure 2.2 [16]. In this figure object $a$ with an isotropic magnetic material has different (geometric) reluctances in the d-axis and the q-axis while the isotropic magnetic material geometry in object $b$ has the same reluctance in all directions. A magnetic field $\psi$ which is applied to the anisotropic object $a$ produces torque if there is an angle difference between the d-axis and the field ($\delta \neq 0$). It is obvious that if the d-axis of object $a$ is not aligned with the field, it will introduce a field distortion in the main field, see field solution in Figure 2.2 (right). The main direction of this distortion field is aligned along the q-axis of object $a$.

In the SynRM field $\psi$ is produced by a sinusoidally distributed winding in a

![Image](image.png)

Figure 2.2: An object with anisotropic geometry $a$ and isotropic geometry $b$ in a magnetic field and reluctance torque production mechanism [16] and [17].
slotted stator and it links the stator and rotor through a small airgap, exactly as in a traditional IM. The field is rotating at synchronous speed, \( \omega_s \) and can be assumed to have a sinusoidal distribution. In this situation there will always be a torque which acts to reduce the potential energy of the whole system by reducing the distortion field in the q-axis, \( \delta \rightarrow 0 \). If \( \delta \), the load angle, is kept constant then electromagnetic energy will be continuously converted to mechanical energy. The stator current is responsible for both the magnetization (main field), and the torque production which is trying to reduce the field distortion, this can be done by controlling the current angle of the machine [3].

### 2.1.2 SynRM model and equivalent circuit

The general equations describing a conventional wound field synchronous machine are known as Park’s equations [3]. The SynRM can be modeled with these equations as well. However, in this case the field and damper winding equations have to be eliminated from the Park’s equations. Thus, referring to Figure 2.1, the resultant SynRM vector equations in the dq-axis (synchronous reference frame) can be written as follows (amplitude invariance) [5]:

\[
v = e + R_s i_s, \quad (2.1)
\]

\[
e = \frac{d\lambda}{dt} + j\omega \cdot \lambda. \quad (2.2)
\]

The machine vector single line diagram based on these equations is shown in Figure 2.1, \( R_c \) represents the total iron losses of the machine at the operating point.

In Equations 2.1 and 2.2, \( v \) is the machine’s terminal voltage vector, \( \lambda \) is the stator flux, \( R_s \) is the winding resistance, \( i_s = i + i_c \) is the stator current vector, \( \omega \) is the reference frame electrical angular speed and \( e \) is the stator back-EMF (EMF).

The stator flux \( \lambda \) according to the magnetization current \( i \) can be defined as follows when the slotting effect is neglected [3]:

\[
\lambda \cong \lambda_d(i_d, i_q) + j \cdot \lambda_q(i_d, i_q)
\cong L_d(i_d, i_q) \cdot i_d + j \cdot L_q(i_d, i_q) \cdot i_q. \quad (2.3)
\]

In Equation 2.3, \( L_d = L_{sl} + L_{dm} \) is the d-axis inductance and \( L_q = L_{sl} + L_{qm} \) is the q-axis inductance, where \( L_{dm}(i_d, i_q) \) and \( L_{qm}(i_d, i_q) \) are the corresponding airgap linkage flux inductances and \( L_{sl} \) is the total winding leakage and almost free of saturation [33]. The magnetizing inductances are not free of saturation and cross-coupling [3], [5], [30] and [31], see e.g. simulated dq-flux of a SynRM by FEM shown in Figure 2.3. The stator winding is assumed to be sinusoidally distributed and as a result the flux harmonics in the airgap contribute only to an additional term in the stator leakage inductance [3], [33].
2.1. SYNRM THEORY

$\lambda_d$ & $\lambda_q$ [Vs Peak] as function of d- and q-axis current

Figure 2.3: Simulated SynRM fluxes under saturation condition, FEM [17].

After the magnetizing inductances, the most important parameters of the SynRM are: the machine saliency ratio $\xi(i_d, i_q)$ that is defined according to Equation (2.4), machine load angle $\delta$, current angle $\theta$, torque angle $\beta$ and internal power factor angle $\phi_i$. These angles, as shown in Figure 2.1, are interconnected together by Equation (2.5) [16], [17].

$$\xi(i_d, i_q) = \frac{L_d(i_d, i_q)}{L_q(i_d, i_q)}$$

$$\theta = \beta + \delta, \quad \frac{\pi}{2} + \delta = \theta + \phi_i.$$  \hspace{1cm} (2.4)

The main magnetic and electric parameters of the SynRM, using the machine model based on Equations (2.3) - (2.5), are interconnected together through Equation (2.6).

$$-\frac{1}{\tan(\theta + \phi_i)} = \tan \delta = \frac{\lambda_q}{\lambda_d} = \frac{L_q i_q}{L_d i_d} = \frac{1}{\xi} \tan(\theta).$$ \hspace{1cm} (2.6)

2.1.3 SynRM main performances

Machines terminal values can now be evaluated based on the machine model in section (2.1.2). The stator electro motive voltage at steady-state, using Equation (2.3), can be calculated according to Equation (2.7).
The machine internal power factor is defined in Figure 2.1. If Equation (2.6) is introduced in that definition, IPF can be evaluated according to Equation (2.8) by some standard trigonometric manipulation [16].

\[ IPF = \cos(\phi_i) = \sin\beta = \cos\left(\frac{\pi}{2} + \delta - \theta\right). \]  

(2.8)

The electro-magnetic interconnection between \( \theta \) and \( \delta \) according to Equation (2.6) can be introduced into Equation (2.8), this gives Equation (2.9).

\[ IPF = -\cos(\theta + \tan^{-1}(\xi \cdot \cot \theta)) < \sin \theta. \]  

(2.9)

The internal power factor based on Equation (2.9) for different \( \xi \) and as function of \( \theta \) is shown in Figure 2.4 (left). IPF has a maximum according to Equation (2.10), Maximum Torque per kVA (MTPkVA).

\[ \tan \theta = \sqrt{\xi} \quad \Leftrightarrow \quad IPF|_{\text{max. or MTPkVA}} = \frac{\xi - 1}{\xi + 1}. \]  

(2.10)

An important conclusion from Equation (2.9) and Figure 2.4 (left) is that IPF is always less than \( \sin \theta \) at any operating point. This equation shows that IPF strongly depends on the operating point \( \theta \) as well as the machine \( \xi \). Equation (2.9) can be solved for \( \xi \) as a function of machine IPF and \( \theta \), which gives Equation (2.11).

\[ \xi = -\tan \theta \cdot \tan \left(\theta + \cos^{-1}(IPF)\right). \]  

(2.11)
2.1. SYNRM THEORY

In Figure 2.4 (right) based on Equation (2.11), machine $\xi$ as a function of $\theta$ and IPF as parameter is shown as well, which shows that a SynRM with IPF > 0.9 has to have a $\xi$ of almost 20. This saliency ratio is not practically feasible [32].

The SynRM torque relation has been derived and analyzed in different literature, comprehensively it can be found in [3], [5], [16] and [17]. Two conditions can be considered: constant current that gives Equation (2.12) and constant flux that gives Equation (2.13).

$$T_{ag} = \frac{3p}{2} (L_d - L_q) \cdot I^2 \cdot sin(2\theta),$$  

(2.12)

$$T_{ag} = \frac{3p}{2} \left( \frac{1}{L_q} - \frac{1}{L_d} \right) \cdot \left( \frac{E}{\omega} \right)^2 \cdot sin(2\delta),$$  

(2.13)

These relations demonstrate strong duality between on one hand the stator current $I$ (rms of $i$) and $\theta$ pair and on the other hand the back-EMF $E$ (rms of $e$) / $\omega$ or $\lambda$ and $\delta$ pair.

Introducing interconnection relation between $\theta$ and $\delta$ according to Equation (2.6) into the torque Equations (2.12) and (2.13) give new equations for torque according to Equation (2.14) and (2.15) ($p$ is pole number) [16].

$$T_{ag} = \frac{3p}{2} (L_d - L_q) \cdot I^2 \cdot sin \left( 2 \cdot tan^{-1}(\xi \cdot tan\delta) \right),$$  

(2.14)

Figure 2.5: SynRM (left) constant flux and (right) constant current trajectories, in torque - current angle space, ideal.
\[ T_{ag} = \frac{3p}{2} \left( \frac{1}{L_q} - \frac{1}{L_d} \right) \cdot \left( \frac{E}{\omega} \right)^2 \cdot \sin \left( 2 \cdot \tan^{-1} \left( \frac{\tan \theta}{\xi} \right) \right). \] (2.15)

Similar to IPF, the SynRM torque as function of \( \theta \) and at constant current, Equation (2.12), and constant flux, Equation (2.15), conditions is demonstrated in Figure 2.5 (right and left, respectively). The SynRM torque under constant current condition based on Equation (2.12) is maximum when Equation (2.16) is satisfied. This represents the Maximum Torque Per Ampere (MTPA) control strategy of SynRM, see Figure 2.5.

\[ \tan \theta = 1 \quad \text{or} \quad \tan \delta = \frac{1}{\xi} \quad \Leftrightarrow \quad \text{MTPA}, \quad (2.16) \]

\[ \tan \theta = \xi \quad \text{or} \quad \tan \delta = 1 \quad \Leftrightarrow \quad \text{MTPV}. \quad (2.17) \]

The torque under constant flux condition based on Equation (2.15) is maximum when Equation (2.17) is satisfied. This represents the Maximum Torque Per Volt (MTPV) control strategy of SynRM, similarly, see Figure 2.5, [3], [8], [9], [10] and [11]. If the machine airgap performance according to Equations (2.3), (2.9), and (2.12) or (2.15) is evaluated for each operating point \((I, E, \omega)\) the electrical behavior at the machine terminals in steady-state can be determined by using Equation (2.1) and the vector diagram in Figure 2.1.

### 2.1.4 SynRM operation diagram

If the correspondent graphs of all three conditions: constant current, constant flux (Equations (2.12) and (2.15)) and machine IPF Equation (2.9) are combined together the results will be a new single operation diagram (OpD) for SynRM that is shown in Figure 2.6 under hypothesis of ideal conditions, among the strongest are constant inductances, \(L_d\) and \(L_q\). This operation diagram demonstrates the behavior of the machine’s main performance parameters, such as torque, current, voltage, frequency, flux, power factor and current angle, all in one graph. The diagram can easily be used to describe different control strategies, possible operating conditions, both below and above rated speed, etc. The saturation effect is shown in Figures 2.3 and 2.7 and will be studied later. Other operating conditions of SynRM such as constant d-axis current can be introduced into this diagram as well.

#### Possible operating points and Different control strategies

Possible operating points of SynRM are shown by points A – G in Figure 2.6. Different control strategies are also shown by some dashed vertical lines in this figure. The SynRM OpD clearly shows that the machine can provide certain demanded
2.1. SYNRM THEORY

Figure 2.6: New Operation Diagram (OpD) of SynRM using constant current and constant flux family graphs together with the machine power factor, in torque, power factor and current angle space, (see Figure 2.1) [16] and [17], under ideal conditions.
torque $T_0$, as shown in Figure 2.6, at certain speed $\omega$, by choosing different operation strategies. Points $A, G$ and $C$ represent Maximum Torque Per Ampere (MTPA), Maximum Power Factor or Maximum Torque Per kilo Volt-Ampere (MTPkVA) and Maximum Torque Per Volt (MTPV), respectively [3], [5], [8] - [11] and [15]. Operating the machine in any of these conditions produces the same torque $T_0$.

Field weakening

Points $D$ and $B$ with respect to point $A$ have the same stator current. These points represent the upper limit of the machine operating points in field weakening. The known field weakening strategies are discussed in [1], [3], [8] - [15], [29], [34], [36], [38], [46], [47], [57], [93], [94], [123], [130] and [131]. If MTPA is used for below and up to base speed operation, e.g. point $A$, then in one strategy, proposed by Lipo et al., the machine current trajectory above base speed and in constant power region, due to the flux reduction, starts from $A$ and moves to $D$ [3], [12], and for another strategy, proposed by Vagati et al., it passes point $D$ and moves to $B$ [12], [13], [29], [34], pointed out in Figure 2.6.

Consider that in this situation the inverter current, $I_0$ and flux, $\lambda_0$ limits are reached with assumption that $(\lambda_0, I_0) = (\lambda_A, I_A)$. The machine losses and saturation will have major effect on the machine field weakening range in the constant power speed region (CPSR) [17]. Especially when the voltage drop over the machine resistance becomes comparable to the back-EMF mainly the maximum speed will be pushed to lower speeds [34].

2.1.5 Nonlinearities in SynRM and magnetization characteristic

The flux in the d-axis can not be considered as a linear function of the current, while in the q-axis this is applicable with an acceptable accuracy. Generally there are two major side effects that can affect these assumptions: the cross-coupling effect between the d- and q-axis, and the stator slotting effect [3], [14] and [35].

The general flux equations can be written according to Equation (2.18). However in this report Equations (2.3) and (2.7) will be used for the analysis and theoretical calculations.

$$
\lambda_{dm} = \lambda_{dm}(i_d, i_q, \vartheta), \\
\lambda_{qm} = \lambda_{qm}(i_d, i_q, \vartheta).
$$

In Equation (2.18), $\vartheta$ is the rotor position angle with reference to the stator and it represents the effect of the stator slot on main magnetizing inductances. The stator slots effect on stator leakage inductances in d- and q- axis is disregarded.

Here it is assumed that all machine inductances except the magnetization inductances $L_{dm}$ and $L_{qm}$ can be modeled as constant lumped elements, i.e. stator leakage, $L_{sl}$, inductances in d- and q-axis are equal and constant, and the side
effects of saturation, slotting and cross-coupling on these elements are disregarded. Therefore the effect on the total stator terminal flux, \( \lambda = \lambda_d + j \cdot \lambda_q \) from the main sources of nonlinearity in SynRM, which are saturation, slotting and cross-coupling, can just be modeled by the behavior of the airgap flux linkage, \( \lambda_m = \lambda_{dm} + j \cdot \lambda_{qm} \).

**Saturation effect**

A typical saturation effect on d- and q-axis fluxes in SynRM is shown in Figure 2.3. Saturation has a major effect on MTPA, dashed vertical line passing A and MTPkVA, line DG, but a minor effect on MTPV, line BC in Figures 2.5 and 2.6. This issue is studied in [10], [11], [16] and [31]. The SynRM performance has been simulated by FEM to investigate the major effects of saturation. Results are summarized in Figure 2.7, all values are expressed in per-unit with respect to point A, MTPA (the actual values are divided by the values in A).

Due to saturation, the MTPA \( \theta \) is higher than 45°, which can be derived from Equation (2.12). As is shown in Figure 2.7 (c) increasing the stator current shifts the optimum MTPA (pu) point to a higher \( \theta \), because the main saturation effect that is affecting the d-axis inductance has to be somewhat compensated by reducing the d-axis current for a given stator current. This can be achieved if \( \theta \) becomes larger than 45° [16] and [17]. At constant current operation, Equation (2.12) or equivalently the term \((L_d - L_q) \cdot \sin2\theta\) must be maximized for a certain stator current and saturation effects at MTPA, by taking into account both saturation and cross-saturation in the d- and q-axis flux. The saturation compensation causes variation in MTPA \( \theta \), which continues to increase with increasing stator current, the MTPA \( \theta \) here at nominal current is 61°, see Figure 2.7 (c).

The machine torque (pu) under constant flux (pu) operation and saturation involvement is demonstrated in Figure 2.7 (d). Increasing the flux generally increases saturation in the machine pole flux path, so \( \xi \) is reduced at higher flux. On the other hand, the MTPV point is reached if Equation (2.15) or equivalently the factor \((1/L_q - 1/L_d) \cdot \sin(2 \cdot \tan^{-1} (\tan\theta/\xi))\) is maximized for certain stator flux. The first reluctance term is not so sensitive to saturation because \( L_d \gg L_q \), but \( \theta \) has to be reduced in order to keep the second term maximum when \( \xi \) is reduced for higher fluxes. Considering also that the trigonometric function in the second factor is not so sensitive to the variation of the variables, one can infer that the \( \theta \) reduction for MTPV is not so evident up to nominal flux (see Figure 2.7 (d)). It is reduced from unsaturated values around 83° to 80° for nominal flux.

The simulated SynRM IPF behavior under saturated condition is shown in Figure 2.7 (b) for different stator currents (pu). Actually, changing the stator current has insignificant effect on the IPF. The reason can be explained through Figure 2.4, which represents the effects of current angle and saliency ratio on IPF in ideal conditions. Calculation results by FEM show that increasing the current angle from zero to 90°, for all stator currents up to nominal, increases the machine saliency ratio. If this fact, increase of \( \xi \) with \( \theta \), which considers saturation and cross-coupling, is depicted in Equation (2.9), it is obvious that the power factor
behavior will be very close to the diagrams in Figure 2.7 (b) and will also not be sensitive to the current.

The expected MTPA is increased almost linearly with current, but the desired MTPV does not increase with flux as expected with reference to Figure 2.7. According to Figure 2.6, if the machine is run (using suitable current) with the same nominal flux at MTPA, see flux at point A in Figure 2.6, for MTPV the expected
torque increases by the factor $1/2 (\xi + 1/\xi)$, point $E$ in Figure 2.6, in comparison to the nominal torque. This factor for the prototyped machine in Figure 2.7 (e) is around four, but Figure 2.7 (d) shows that with nominal MTPA flux, this factor is around two to three under saturated condition. Factor four can be reached if the machine flux is increased by 40%, see MTPV point for flux 1.4 in Figure 2.7 (d) (at this point the torque is 4.3 p.u.).

Higher current angle at MTPA and lower torque at MTPV for certain $(\lambda_0, I_0)$ reduce the field weakening range in the constant power speed region. Remember that the power factor can be considered load independent in this region. Of course, the base point (MTPA) power factor will increase due to the saturation effect at higher current angle (see also [10]).

Cross coupling effect

The dependency of each axis flux to another axis current in Equation (2.18) expresses another nonlinear effect in SynRM: Cross-coupling or cross-magnetization. Particularly the dependency of $\lambda_{dm}$ on $i_q$ can create the well known armature-reaction effect, i.e. demagnetization of the d-axis flux due to a large q-axis current [14]. A typical cross-coupling effect is shown for a machine with 50 A stator nominal current in Figure 2.7 (a).

The cross-coupling effect is mainly due to the shared iron part of the rotor between d- and q-axis, also the rotor ribs increase this effect [36]. Cross-coupling also effectively reduces the q-axis flux. A typical effect of cross-coupling on machine inductances is measured and modeled in [32] and [35]. Both saturation and armature-reaction effects reduce the machine torque by decreasing the d-axis inductance. The cross-coupling has a considerable effect on the performance of the sensorless control system of the machine and should therefore be taken into account when developing sensorless control systems for SynRM [31].

Slotting effect

The slotting effect in SynRM is modeled in Equation (2.18) by the dependency of magnetization inductances to rotor position $\vartheta$. This issue is deeply discussed in [3] and [37]. There are two extreme situations when the rotor is rotating one stator slot pitch. The first position is when stator teeth and rotor segments are in phase. In this case the total reluctance of the flux path is minimum and therefore $L_d$ is at its maximum value. The second position is when rotor changes to the situation when stator teeth and rotor segments are out of phase. Now the total airgap reluctance that is maximized and therefore $L_d$ is at its minimum. Similar behavior for the q-axis inductance is discussed in [37]. When the rotor is in the situation that the rotor segments share half of the stator teeth and slot then applying eg. a d-axis flux causes some q-axis flux also. This shows an interconnection effect between d- and q-axis inductances that is caused by the stator slots [37].
The change of inductances due to the rotor position firstly produces torque ripple and secondly high flux fluctuation deep inside the rotor segments and consequently iron losses in the rotor body [3], [38] and [39]. If the slotting effect during the rotor design stage is not attended to then the iron losses in the rotor can be comparable with the rotor copper losses in an equivalent IM [39]. The torque ripple reduction can be achieved effectively (acceptable for traditional IM applications) by adopting the skewing technique [37]. Torque ripple can not be completely eliminated by skewing, because, as was discussed before there is always an interaction between the d- and q-axis flux due to the effects of stator slotting [14] and [37]. Despite skewing, the design of especially the rotor slot pitch has a significant contribution to the reduction of torque ripple and can therefore be used to minimize the ripple. Some design idea regarding this can be found in [3], [40], [41], [42] and [43].

2.2 SynRM rotor realization techniques

Mainly there are three different types of SynRM with anisotropic rotor structures, see Figure 2.8. The salient pole rotor as the first possibility is made by removing some iron material from each rotor in the transversal region, see Figure 2.8 (a). In the axially laminated rotor, which is the second type of SynRM, the laminations (iron) are suitably shaped at each pole and insulated from each other using electrically and magnetically passive materials (insulation) and the resulting stacks are connected through pole holders to the central region, to which the shaft is connected, see Figure 2.8 (b). In the third type of rotor the laminations are punched in the traditional way. Thin ribs are left when punching, thus the various rotor segments are connected to each other by these ribs, Figure 2.8 (c).
2.2. SYNRM ROTOR REALIZATION TECHNIQUES

2.2.1 Rotor geometry classification and development history

A brief history of alternative rotor geometries can be useful to understand SynRM, see Figure 2.9. The rotor in Figure 2.9 (a) is obtained by removing of material from a conventional induction motor rotor, either by a milling operation after casting the cage, or by punching before casting the cage. Rotors of this type (synchronous induction motors) have a simple construction, but the saliency ratio is too small to give competitive performance [32] and [44].

Figure 2.9 (b) shows the salient pole construction, like a conventional salient pole synchronous motor with the windings removed. An unsaturated inductance ratio of about 3 has been reported for this kind of rotor, decreasing to about 2.5 under load. No value of saliency ratio higher than 3.8 has been reported [32]. Regardless of the poor saliency ratio the other performance characteristics of salient pole geometry are also not acceptable, because, if the inter-polar space region in the q-axis is spread to reduce $L_q$, it also results in narrowing the pole space in the d-axis thus also reducing $L_d$. In this case other kind of rotor configurations, for example barrier ones must be employed to improve the machine performances [44].

Figure 2.9: Historical evolution of different alternative rotor geometries Staton and Miller et al. [32] and Hrabovcova, Pyrhonen and Haataja et al. [44]
A one barrier configuration, Figure 2.9 (c) and Figure 2.9 (d), is also not sufficient to improve the machine performance [44]. The configuration in Figure 2.9 (d) is derived from the synchronous motor with interior PM, if the PMs are removed.

Therefore, the number of barriers must be increased. As early as 1923, Kostko [2] analyzed a rotor of the form of Figure 2.9 (e), see also Figure 2.10 (a), embodying several features of the main schools of later development, including the use of multiple flux barriers, segmental geometry and a q-axis channel. Kostko also points out the essential limitation of the salient pole design, namely, that if the interpolar cut-off barrier is widened to decrease the q-axis inductance, the pole arc is thereby narrowed, producing an unwanted reduction in $L_d$. He concludes, in effect, that the multiple barrier or segmented arrangement is the natural way to make a synchronous reluctance motor because it involves no sacrifice of pole arc in the d-axis [2] and [32]. Subsequent workers, generally aware of Kostko’s work, see Figure 2.10 (a), developed the geometry along two main lines: the segmental geometry, see Figure 2.9 (e) and Figure 2.10 (b) (Fratta and Vagati et al.), and the axially laminated geometry, see Figure 2.9 (f).
2.2.2 SynRM and IM’s performance comparison

Induction motors are the worldwide most used motor in industrial and civil applications, due to its low cost, robustness and the possibility to be supplied directly from the mains, without the need of a power electronic converter. However, when the application requires speed regulation, different types of motor can be profitably adopted and parameters as torque/volume, efficiency and control easiness assume greater importance [19].

For the TLA type SynRM, production cost is comparable to IM and somehow it can be even cheaper due to the cage elimination in the rotor and the removal of casting stage in the production line. If the same stator size is chosen as the IM then just by changing the punching tools for the rotor geometry the SynRM can be produced with the same production line [19]. Also TLA can easily be skewed like IM for torque ripple reduction.

A SynRM and its correspondent IM rotor are shown in Figure 2.11. The stator flux in the IM induces current in the rotor short-circuited cage, which is normally made by casted aluminum in the pre-punched rotor slots, due to asynchronous rotation of the rotor with respect to the stator rotating MMF. The induced current in the rotor cage creates another airgap MMF that rotates synchronously to the stator MMF and with a small slip to the mechanical rotational speed of the rotor. The resultant MMF induces an airgap flux density that is interacting with the stator current loading and an electromechanical torque is developed in the machine [33]. IM can produce torque at starting due to the slip function. It can also produce different shaft torque by self-adjusting the slip. Induced current in the rotor cage creates extra losses in the machine. Thus machine temperature rise increases, rotor and shaft are warmed up and machine efficiency is reduced, see Figure 2.11.

The torque production mechanism in SynRM is different from IM. SynRM utilizes the reluctance concept and rotating sinusoidal MMF, which can be produced by the traditional IM stator, for torque production [16] and [17]. This can be explained through Figure 2.1. A sinusoidally distributed stator current loading \( i \) creates an airgap flux. The current angle, \( \theta \), and load angle, \( \delta \), are not the same due to the rotor reaction, as the SynRM rotor has two magnetic axes. One axis has higher magnetic permeance (d-axis) than the other axis (q-axis), due to the introduction of suitable magnetic insulation layers (here air, see Figure 2.11) inside the rotor. This anisotropic rotor structure will interact with the rotational stator MMF and electro-mechanical torque will be developed according to Equation (2.19).

\[
T_{ag} \propto |\lambda| \cdot |i| \cdot \sin\beta. \tag{2.19}
\]

The torque will act to minimize the electro-magnetic potential energy of the whole system.

The SynRM was analyzed for the first time by J. K. Kostko in 1923 [2]. Traditional salient pole machines have low torque density due to the poor anisotropic structure. Kostko showed that suitably designed SynRM with interior flux barriers (insulation layers) [2], [3] and [16] has almost the same torque per ampere as IMs. A
disadvantage of SynRM is the lack of starting torque in direct on line (DOL) operation, but a SynRM under closed-loop control can easily be controlled and operated, due to the completely new possibilities that have been made possible through the development of power electronic based variable speed drives. Thus the drawback of this machine under direct on line operation and supply, especially stability and start up torque can be overcome, in VSD applications.

The absence of the rotor cage reduces the joule losses in comparison to the IM, see Figure 2.11. Consequently SynRM has a lower temperature rise, lower rotor and shaft temperatures and higher efficiency than the correspondent IM [3] and [16] and consequently higher reliability. SynRM has higher overload capacity compared to the IM and it can reach up to 3 times nominal load [3] and [19]. The high saliency and anisotropic rotor can be used to adapt the sensorless and zero speed control techniques [19].

The SynRM that is shown in Figure 2.11 (left) has been prototyped and tested at two different conditions. The same tests have been repeated with the corresponding IM. One test was done at 1500 rpm when both machines have the same temperature rise, and another test was done at 2500 rpm when both machines have the same current loading. Roughly, MTPA was used as the operating point for both machines [16]. By measuring the total loss ratio \( k_{loss} \) and shaft torque ratio \( k_{T} \) between the two machines, similar to the methods in [14] and [20], the efficiency improvement

\[
\text{EffRM} = \frac{P_{out}}{P_{in}} = 1 - \frac{\Delta P_{loss}}{P_{in}}
\]

Figure 2.11: SynRM [22] vs. IM rotor geometry for the same stator size (IM 15 kW) (left). Estimated efficiency difference between SynRM and IM based on measurements on 15 kW Eff1-motors at similar VSD operating conditions (right).
2.3. SYNRM BASIC CONTROL CONCEPTS

when the IM rotor is replaced by the SynRM rotor is extrapolated and estimated for a wide IM efficiency range according to Equation (2.20).

\[ \Delta \eta = \eta_{SynRM} - \eta_{IM} = \frac{1}{1 + \frac{k_T}{k_{loss}} \cdot \left( \frac{1}{\eta_{IM}} - 1 \right)} - \eta_{IM} \] (2.20)

The results are shown in Figure 2.11 (right). The corresponding standard IM’s power is shown too. Study shows 1.5 – 5%-unit machine efficiency improvement by using SynRM rotor instead of the IM, for the power range 1, 1 – 90 kW. Normally the efficiency of IM increases, if the nominal power increases. The graph in Figure 2.11 (right) shows that for low rated power, the SynRM can be more efficient than the IM [16].

If the stator structure can be changed then the optimum machine geometry for maximum stall torque at constant power loss dissipation shows that the SynRM with ribs always has higher torque density than IM with a copper cage [21] and [45]. Also, it is shown that the optimum airgap to outer diameter ratio for maximum stall torque is not the same in both machines. The optimal ratio for SynRM is smaller than the IM optimal value [21]. These analytical calculations have been also verified by measurement [19], [23] and [25].

SynRM has 5% to 10% lower power factor than IM. This is due to the combined effect of cross coupling and large q-axis inductance. The large q-axis reactance is an inherent drawback of the SynRM. It depends on the different field distribution in the rotor and can not be overcome. Moreover, the flux in the rotor ribs adds to this effect [19]. In practice, this drawback becomes important when a large constant power speed range is requested by the application [19] and [38].

In fact, the inverter over-sizing which is needed in this case to cope with a fixed constant power speed range directly depends on the rated \( \lambda_q/\lambda_d \) value. The larger this value, the larger is the inverter over-sizing. However, this drawback can be overcome by introducing some permanent magnets into the rotor, thus changing from a TLA SynRM to Permanent Magnet Assisted Synchronous Reluctance Motors (PMaSynRM) [13] and [38].

Inverter size is also related to the machine efficiency. Therefore the required inverter size can be estimated by the product of efficiency and power factor (\( \eta \cdot \cos \varphi \)).

2.3 SynRM basic control concepts

Field orientated control (FOC) based on the machine current angle control is straight forward and a natural way to control SynRM see Equations (2.9) and (2.12) [3], [8] and [11]. Constant torque trajectories according to Equation (2.12) are hyperbolas in the current dq-plane, see Figure 2.12. The constant voltage trajectories can be expressed according to the following:

\[ \left( \frac{e}{\omega \cdot L_q} \right)^2 = i_q^2 + \xi^2 \cdot i_d^2. \] (2.21)
CHAPTER 2. SYNRM BASIC PRINCIPLES

Equation (2.21) shows that constant voltage trajectories are ellipses as is shown in Figure 2.12. Point A is the maximum torque per current control (MTPA) point and B is the maximum torque per voltage (MTPV) or maximum rate of change of torque (MRCT) control [9] point. It is clear that the terminal stator current angle must be increased to compensate the airgap flux displacement due to saturation, cross-coupling, winding leakages and iron losses see Figure 2.1 [11].

Saturation, mainly in the machine d-axis, reduces $L_{dm}$ and consequently torque for a certain current. By increasing the current angle the d-axis current is reduced. Thus, the level of saturation and also airgap flux density are reduced also $L_{dm}$ and torque are increased and compensated [11].

The iron losses require an additional angle advance to ensure optimal torque per current operation, this is clearly shown in the vector diagram of Figure 2.1, compare the angle of stator current $i_s$ and $i$ in that figure. Because of the iron losses, $i_c$, the effective current vector $i$ is pushed back towards the d-axis by an angle. In order to have optimal torque per ampere operation, the stator current $i_s$ needs to be adjusted to an angle that is even larger than the angle needed when saturation is considered alone [11].

To have optimal efficiency operation (ME), an even larger increase in the current angle is required to further reduce the flux and hence the core loss. The optimum occurs when the additional copper loss associated with the increased q-axis current required to produce the torque offsets the reduction in core loss [11]. Studies

Figure 2.12: SynRM current dq-plane and full operation trajectory (below base speed A and field-weakening AB∞) [46]. Saturation is disregarded.
have shown that the MTPA will not always give the optimal performance of the
SynRM machine. In fact in some circumstances the SynRM machine will have
better performance if the current angle increases beyond the corresponding MTPA
point even if the current increases above the MTPA value. The reason is related to
the following:

Firstly, the SynRM power factor increases if the current angle increases more
than MTPA and at the same time torque and efficiency will remain constant. Sec-
ondly, the optimal performance of the machine is reached if a specific balance
between the iron losses and copper losses is reached and not by minimizing the
current for a certain torque which consequently only minimizes the copper losses.
Thirdly, speed is not considered to determine if the MTPA is the best operating
point, except (limitedly) when the machine runs at nominal speed and nominal
load. Fourthly, the more general term to judge for the best operating point is: run-
ning the machine at such a condition that the following balance between total losses
and copper (Joule) losses becomes valid:

\[ P_{\text{copper}}^{\text{losses}} \approx \left( \frac{1}{2} \times P_{\text{total}}^{\text{losses}} \right). \quad (ME) \tag{2.22} \]

In conclusion, for SynRM different strategies can be applied by current angle
control: Maximum Torque per Ampere (MTPA), Maximum Power Factor (MPF)
or Maximum Torque per kVA (MTPkVA), Maximum Rate of Change of Torque
(MRCT) or Maximum Torque per Volt (MTPV), Constant d-axis Current (CDAC),
Maximum Efficiency (ME) etc. As discussed, ME control becomes important if
machine iron losses become comparable to the copper losses. Otherwise, MTPA
and ME are equivalent [8], [11] and [47].

2.3.1 Field weakening

This issue has been discussed briefly in section 2.1.4 on page 30, here it will be
discussed in more details. Field-weakening of SynRM is briefly studied in [1] too,
and more deeply in [3], [9], [10], [12], [13], [14], [15], [29] and [34]. Here, losses are
not considered and saturation is included by inductance tables. For each speed the
current and voltage are set to be below or equal to the nominal values and at the
same time the output power is maximized. As is described in [1], [3], [9], [10], [12],
[13], [14], [15], [29] and [34], in field-weakening the machine operating point starts
from point A in Figures 2.6 and 2.12 and move towards point B.

Field-weakening here means: What is the maximum achievable speed for SynRM
in comparison to the base speed, if the machine’s base current and voltage are fully
utilized? In this sense, the true field-weakening point for SynRM is B. In case of
ideal conditions (lossless and unsaturated machine) the behavior of SynRM below
and above base speed in field-weakening and in per-unit is demonstrated in Figure
2.13 (a). A field-weakening of 1 : 4 (4 \approx 1/2 \cdot (\xi + 1/\xi)), see Figures 2.6, where here
\( \xi \approx 8 \), is possible for SynRM assuming lossless and unsaturated conditions.
Figure 2.13: (a) SynRM Performance vs. speed (pu), in the ideal case, (b) Current, Flux and Voltage (pu) loci, when saturation is considered, (c) SynRM Performance vs. speed (pu), when saturation is considered.
2.3. SYNRM BASIC CONTROL CONCEPTS

The machine terminal current, voltage and airgap flux density loci are shown in Figure 2.13 (b), when saturation is included. Machine speed is changed from 500 to 20000 rpm. Normally, point A is MTPA and point B is MTPV. As is clear from Figure 2.13 (b), the load angle at point B must settle to around 45°. In practice, when saturation is involved, as in the simulation here the load angle becomes slightly larger than 45° and settles at a value around 48°. As is shown in Figure 2.13 (c), a field-weakening of 1 : 2.5 is possible for SynRM assuming lossless conditions.

2.3.2 Simple parameter estimator

Equation (2.11) directly shows a fundamental dependency of the machine saliency ratio to IPF. This fact can be used to estimate online the machine $\xi$ and inductances at the operating point. In [18] the inductances are estimated by using the machine voltage vector Equation (2.1) in the dq-frame, instead of the terminal values.

Saliency Ratio Estimator

Solving Equation (2.6) for $\xi$ as function of $\varphi_i$ leads to an equation through which $\xi$ can be estimated by using the machine power factor according to Equation (2.23).

$$\xi(i_d, i_q) = -\tan\theta \cdot \tan(\theta + \varphi_i).$$
$$= -\tan\theta \cdot \tan\left(\theta + \cos^{-1}(IPF)\right). \quad (2.23)$$

Direct and Quadrature Axis Magnetizing Inductances

Solving Equation (2.6) for $\delta$ as function of $\varphi_i$ leads to an equation through which $\delta$ can be estimated by using the machine power factor according to Equation (2.24) see Figure 2.1. This estimator uses the machine terminal values instead of the dq-frame voltage and current, which is the case in [18]. The machine $L_d$ can be estimated using Equations (2.4), (2.23) and (2.24). The performance of the machine’s saturation level estimator is simulated in Simulink-Matlab. The results are shown in Figure 2.14, which shows that the actual machine inductances in steady-state can be estimated in less than one second after start up.

$$L_q(i_d, i_q) = \frac{\lambda_d}{i_q} = \frac{\lambda}{i} \cdot \frac{\sin\delta}{\sin\theta} = \frac{E}{I\omega} \cdot \frac{\sin\delta}{\sin\theta} =$$
$$= \frac{E}{I\omega} \cdot \frac{-\cos(\theta + \varphi_i)}{\sin\theta} =$$
$$= \frac{E}{I\omega} \cdot \frac{-\cos(\theta + \cos^{-1}(IPF))}{\sin\theta}. \quad (2.24)$$
The model of the simulated machine does not consider iron losses, but the stator resistance effect is included. For inductance calculations the terminal power factor is used instead, which causes small discrepancy between the actual inductances and the estimated ones. However, if the machine terminal parameters are known (voltage, current and power factor), then calculating IPF by some standard mathematical manipulation and using vector diagram in Figure 2.1 is straightforward. This will give a better estimation. Of course, at least $R_s$ must be known, the effect of iron losses can be neglected due to the smaller iron loss current $i_c$ in comparison to the stator current at normal operating conditions.

**2.3.3 Overload capacity**

Result from an overload study for the rotor in Figure 2.7 (e) is shown in Figure 2.15. Increasing the stator current shows that the torque versus current follows an almost linear relationship up to a very high overload condition of about 9 p.u. assuming the current angle in this situation to be around 62°, see Figure 2.15 (left). The nominal current is chosen at MTPA operation, therefore, the nominal power factor is around 0.75. However for this heavily overloaded condition (9 p.u.) the power factor is reduced by 50%, see Figure 2.15 (right). If the power factor at
2.4 Evaluation of different control schemes

Different control strategies and possible operating conditions of the Synchronous Reluctance Machine (SynRM) are briefly discussed in this chapter. In particular, the new operating diagram of the SynRM in current angle, torque and power factor space, is used to demonstrate the SynRM major performance characteristic, see Figure 2.6.

Conceptually, possible SynRM machine operating points and control are studied in the literature, e.g. see [1], [3], [8] - [13], [15], [34], [38], [46], [47], [57], [93], [94], [123] - [126], [130], [131] and [132]. The Maximum Torque Per Ampere (MTPA) control is the most well-known control method in SynRM, e.g. see [1], [3], [8] - [12], [34], [36], [38], [46], [57], [93], [94], [96], [121] - [123], [127], [129] and [131]. The overload is also considered then a 3 p.u. overload is achievable, by using a suitable control strategy based on methods in [31], without affecting the power factor, see Figure 2.15 (right) for 3 p.u. stator current [16]. The SynRM shows higher overload capacity compared to induction machines (normally 2 p.u.).

A direct conclusion from the torque vs. current graph, see Figure 2.15 (left), is that the machine design is related to the machine current capability, both thermally, relating to copper losses, and electrically, relating to current density in the stator slots. Increasing the SynRM stator current carrying capability increases the torque density of the machine proportionally. In other words if the machine geometry is going to be designed for torque maximization then special attention has to be paid to increase the current carrying capacity of the machine instead of magnetically optimizing the rotor. Simply increasing the stator slot area or the amount of the copper has more impact on the rated torque, compared to increasing anisotropy or iron material.

Figure 2.15: (left) SynRM [22] airgap MTPA (pu) as function of stator current (pu), and (right) internal power factor as function of current angle and stator current (pu).
MTPA for certain current supply occurs when the machine d- and q-axis currents are controlled in such an area that the machine delivers the maximum possible torque, e.g. see point A in Figure 2.6 for machine torque $T_0$. The next most important control of the SynRM is Maximum Power Factor (MPF) or Maximum Torque Per kilo Volt-Ampere (MTPkVA), e.g. see [8] - [12], [34], [46], [57], [93], [94], [123] and [131]. The MTPkVA for certain apparent power (kVA) occurs when the machine d- and q-axis currents are controlled in such an area that the machine power factor is maximized, e.g. see point G in Figure 2.6 for machine torque $T_0$. The Maximum Efficiency (ME) control of SynRM is the best way to save energy for a certain delivered shaft power, e.g. see [8] - [12], [46], [47], [57], [118] - [120] and [128]. In such an operating point the machine efficiency is maximized. A rough general rule for ME is Equation (2.22), when the copper losses are almost half of the total losses if the copper losses in the machine are not the dominant part of the machine losses. The control schema for implementing these strategies can be found in [3], [8] - [12], [28], [30], [31], [46], [57] and [118] - [131]. Implementation of the different control strategies in SynRM are evaluated by measurements in literatures as well, e.g. see [1], [10] - [12], [34], [46], [47] and [122] - [129]. An introduction to an evaluation of the different control strategies in SynRM is given in this section. For this purpose the optimized machine design SynRM−1 − Opt$M$, see Figure 9.10 and Table 9.2 and chapter 9 for its detailed design and characteristic, 15kWM machine ($M400-50A$), is used. The performance of this machine is calculated in a characterization test. In this test the machine current angle for a certain load torque at a certain speed is varied and the machine performances are measured and also calculated at thermal steady-state conditions. By this means, different control strategies can be achieved while the machine load condition is fixed, for example see points A, D or G and B or C in Figure 2.6.

2.4.1 Description of the calculation method for SynRM−1 − Opt$M$ characterization

This machine at nominal conditions can deliver around 63 kW, 133 Nm at 4500 rpm with winding temperature class F− and around 120°C. FEM calculation is used to determine the inductances values as a function of magnetizing current and current angle. A similar method is used to calculate the iron losses, made up of hysteresis, eddy and excess losses. Calculations are also performed as a function of load as well. The iron losses are calculated for the different parts of the machine rotor, stator back and stator teeth, separately. By this means, different type of iron losses for each part of the machine can be scaled for speed and iron sheet material as well as the machine load current and current angle.

The effect of the voltage harmonic in the extra iron losses are also considered in the calculation by the method presented in [83]. The measured friction losses is used in the calculation. The machine stator resistance is calculated based on an estimation of the winding temperature rise
2.4. EVALUATION OF DIFFERENT CONTROL SCHEMES

Figure 2.16: Calculated, "C", machine performance for varying current angle, \( \theta \), on SynRM – 1 – OptM, see Figure 9.10 and Table 9.2, 15kWM machine, for different shaft torque, "T", and speed, "S". "C T100 S4500" means calculated machine performance at 100% of nominal torque, 133 Nm, and speed of 4500 rpm.

determined from Equation (10.2) where the empirically, using measurements, found values for \( k_{cs}, x, y \) and \( z \) are used. The copper losses are calculated from the calculated stator resistance at each operating point.

Once the machine inductances, iron losses, friction losses and stator resistance are known, the machine terminal and shaft values can be determined using the machine model and the methods discussed in this chapter. The calculated inductances and total iron losses are also calibrated with just one measurement at nominal point, 63 kW, 133 Nm, 4500 rpm and class F – at MTPA. As a result of this calibration the calculated and measurement values of the machine performance are exactly the same at nominal point. Once the machine shaft speed and torque are fixed, the current angle is changed and the current that gives the load demand for each current angle is calculated through iterations.
Figure 2.17: Calculated, "C", machine performance for varying fundamental phase voltage on SynRM−1−OptM, see Figure 9.10 and Table 9.2, 15kW M machine, for different shaft torque, "T", and speed, "S". "C T100 S4500" means calculated machine performance at 100% of nominal torque, 133 Nm, and speed of 4500 rpm.

2.4.2 Characterization of the SynRM−1−OptM, analysis of calculation results

The calculated effect of current angle, $\theta$, on machine performance indexes are shown in Figure 2.16. In the legend e.g. the "C T100 S4500" label means calculated machine performance at 100% nominal torque, that is 133 Nm, and speed of 4500 rpm.

The optimal current angle for MTPA depends on the machine torque but is almost independent of the machine speed, see Figure 2.16 (b). This can be the main reason that the MTPA control is the most interesting type of control in literature and is also more easy to implement in a control scheme. The MTPA current angle for low torque, 25%, is around 50° which is very close to the ideal MTPA current.
2.4. EVALUATION OF DIFFERENT CONTROL SCHEMES

![Graphs showing motor performance](image)

**Figure 2.18**: Calculated, "C", machine performance for varying fundamental flux on SynRM−1−OptM, see Figure 9.10 and Table 9.2, 15kWM machine, for different shaft torque, "T", and speed, "S". "C T100 S4500" means calculated machine performance at 100 % of nominal torque, 133 Nm, and speed of 4500 rpm.

The machine losses and apparent power are minimized if the current angle is around 70° and it is almost independent of the torque and speed, see Figure 2.16 (c) and (d), respectively. Finally, the machine voltage reduces almost linearly when the current angle is increased, see Figure 2.16 (a), simply because the load angle increases and the machine flux decreases when the current angle is increased.

The minimum losses in the machine occur when the reduction of flux in Figure 2.16 (a) and increase of machine current for current angles beyond MTPA in Figure 2.16 (b), reach an optimal trade-off. In other words, it can be assumed that the flux and iron losses are directly inter-connected and just as the machine current and copper losses are inter-connected. Therefore, a trade-off point between voltage, or flux, and current, represents a trade-off point between iron losses and copper losses.
at each operating point that has maximum efficiency. This means that the optimal current angle that gives maximum machine efficiency is at least higher than the optimal MTPA current angle, see Figure 2.16 (c). This optimal point seems to be very close to the operating point of minimum machine apparent power as well, compare Figure 2.16 (c) and (d), respectively. It is also worthy to note that for each optimal operation, MTPA, ME and MTPkVA, exactly, there is just one and only one suitable current angle.

The calculated effect of phase fundamental voltage on machine performance indexes are shown in Figure 2.17. This is another representation of the same variables as in Figure 2.16. It is important to note that here for each optimal operation of the machine, MTPA, ME and MTPkVA, exactly, there is just one and only one suitable voltage.
2.4. EVALUATION OF DIFFERENT CONTROL SCHEMES

Figure 2.20: Calculated, "C", machine performance for varying fundamental power factor, PF, on SynRM−1 − OptM, see Figure 9.10 and Table 9.2, 15kWM machine, for different shaft torque, "T", and speed, "S". "C T100 S4500" means calculated machine performance at 100 % of nominal torque, 133 Nm, and speed of 4500 rpm.

The calculated effect of phase fundamental flux on machine performance indexes are shown in Figure 2.18. This is another representation of the same variables as in Figures 2.16 and 2.17. Effect of flux on machine current and power factor are the most important issues, see Figure 2.18 (b) and (a), respectively. The optimal flux for MTPA and MPF depends on machine torque and is almost independent of the machine speed. These optimal fluxes increase with torque as well. However, the optimal flux for MTPA is larger than the optimal flux for MPF. The optimal flux for MPF, motor ME, and motor MTPkVA are almost the same. This flux level is only a function of the machine torque and almost independent of the machine speed as well, see Figure 2.18 (a), (c) and (d), respectively.

The effect of flux, that is almost independent of the machine speed, on optimal operation of the SynRM for different control strategies, qualitatively, is similar to the effect of the machine current angle, and its usage for machine control can be as easy as current angle control. It provides a strong tool to adjust each machine and/or inverter and/or system performance parameter such as current, PF and efficiency, optimally. This is done by simply finding a suitable flux level for the machine that is a function of load and independent of speed.

The calculated effect of current on machine performance indexes are shown in Figure 2.19. This is another representation of the same variables as in Figures 2.16, 2.17 and 2.18. In this figure the machine MTPA is compared with other optimal operation of the machine. An almost trade-off between the MTPA and MPF controls can be made, because the machine MTPA can be achieved for a wide range of the machine power factor as a function of torque and almost independent of the speed, see Figure 2.19 (a) and (d). However, the MPF control requires more current than MTPA control. This is also true for motor ME and MTPA controls as well, see
Figure 2.21: Calculated, "C", loss distribution in SynRM—1—OptM, see Figure 9.10 and Table 9.2, for different shaft torque, "T", and speed, "S". "C T100 S4500" means calculated machine performance at 100 % of nominal torque, 133 Nm, and speed of 4500 rpm.
2.4. EVALUATION OF DIFFERENT CONTROL SCHEMES

The calculated effect of power factor on machine performance indexes are shown in Figure 2.20. This is another representation of the same variables as in Figures 2.16, 2.17, 2.18 and 2.19. The trade-off between MPF and motor ME controls can be achieved even more precisely, see Figure 2.20 (a) or (b).

The distribution of the machine calculated losses at different load torque and speed is shown in Figure 2.21. The machine losses are minimized for low speeds, 1500 rpm, when the iron losses are not the dominant part of the losses, if a specific balance between copper losses and machine total losses takes place. In this situations the copper losses are almost half of the machine total losses, see Figure 2.21 (b). However, when the machine speed increases and consequently, iron losses increases, the minimum total losses takes place when the copper losses become almost 20 – 35 % of the total loss, see Figure 2.21 (b).

In this condition the ME is equivalent to almost operating the machine to minimize the iron losses as much as possible, but the reduction of iron losses is limited because of the increase in copper losses. Therefore, there is a trade-off operation for ME again. This will take place if the iron losses become almost 1/2 - 2/3 of the machine total losses, see Figure 2.21 (d) for speeds 3000 and 4500 rpm. In such conditions, the copper losses of the machine are almost 25 - 50 % of the machine iron losses for high speeds where the iron losses are dominant, see Figure 2.21 (f). At low speed, 1500 rpm the optimal iron losses and copper losses are equal for ME operation of the machine, see Figure 2.21 (f).

The trade-off between MTPA, minimum copper losses, and MPF controls almost corresponds to minimum iron loss control that leads to a conclusion that ME control is a general approach rule not only for machine control development but also for designing the machine as it is used for machine design in chapter 6 for 3kWM machine design, see Figure 6.8 (left) and for 15kWM machine design, see Figure 9.9.

The issue of the loss distribution in the SynRM machine in both the control and the design stages and its effect on machine performances needs more study and it is out of the scope of this thesis.
Chapter 3

PMASynRM

The synchronous reluctance machine (SynRM) is analyzed by simulations and measurements from different aspects, see chapters 1, 2, 6, 7, 8, 9 and 10. The analyses show that this type of machine has some advantages such as higher efficiency, overload capacity, torque density, simplicity in production and lower rotor temperature, in comparison to the induction machine (IM).

If the maximum torque per ampere (MTPA) strategy is implemented for increasing the SynRM efficiency, an important problem that arises as a drawback is the poor machine power factor. In this case the machine power factor is lower than the corresponding IM, this could lead to an over-sizing of the inverter, see chapter 2 and [16], [102].

A solution to this problem has been shortly discussed in chapter 2 and [16], which is the introduction of permanent magnets (PM) in the rotor structure, thus moving from a SynRM to a permanent magnet assisted SynRM (PMASynRM) concept. A PMASynRM machine is an interior permanent magnet (IPM) machine [15], [31], [38], [88], [94], [95], [96], [101], [114] and [139], however, in this machine the nature of the produced torque is more based on the reluctance properties of the machine than on the PM properties.

An IPM has been designed, analyzed and its performance measured and reported in chapter 5, see Figure 5.1 and Table 5.1. The rotor structure of this machine is shown in Figure 5.1 (right). Measurements show that all machine performances (T, PF, $P_{out}$ and $\eta$) are improved in comparison to the corresponding SynRM and IM, for details on measurement results refer to chapter 5.

In this study the PMASynRM is studied by means of the finite element method (FEM). Different possible rotor structures, basic machine concept and model and potential improvements in the machine performance in comparison to the corresponding SynRM are explained and presented. The main aim here, is not to have a classified design procedure approach and optimization, which needs more study and time, but to address accurately, qualitatively and quantitatively the main characteristics of such a machine by FEM. The study is focused on the improved machine
design SynRM that has been designed, see chapters 6, 7 and 8, and prototyped, see chapters 8 and 9, but the results can be generalized.

Detailed PMaSynRM design tools and procedure, using [15], [31], [38], [60], [61], [85] - [90], [93], [94], [96], [97] - [100], [106], [114] and [139], can be developed under two categories, basic principles and detailed design principles. All analyses are based on calculations by FEM. The M600-50A material characteristic is used for the electro-magnetic analysis [72].

3.1 Brief basics on PMaSynRM

The rotor structures of some (prototyped) PMaSynRM, which are reported in different papers, are shown in Figure 3.1. In this figure, geometry (e) is one of the first IPM machines [88]. Geometries (a) [75] and [86], (c) [85] and (f) [60] and [89] are suitable for hybrid electric vehicle (HEV) or similar applications.

It will be shown that if the standard stator of an IM is used for the SynRM, then the 4-pole machine has the best performance, see chapter 10, however if other constrains, mainly on geometrical parameters such as shaft, outer and airgap diameters, active length, are considered in the optimization then this view can be changed as we can observe in the different geometries of Figure 3.1.

Geometries (h) [91], (b) [31], (i) [92] and (a) [75] and [86] are the most advanced SynRM designs. Consider the shape of the barriers (insulation layers) and observe, how close they are to the shape of the natural flux lines inside the solid rotor, see chapter 9 for the optimized machine design approach based on this concept.

In all geometries an attempt has been made to minimize the cost [102] by minimizing the required PM flux. This can be achieved by using cheap magnet materials like ferrite, see data sheet in e.g. [107], or some kind of flexible plastic or powder technology based materials and filling almost all available barrier spaces in the rotor by PM material, see Figure 3.1 geometry (f) [60] and [89].

Another way is to implement high quality PM materials like NdFeB, see data sheet in e.g. [107], or similar materials, but minimizing the magnet volume, see all geometries in Figure 3.1 except (d), (e) and (f).

3.2 Nature of PMaSynRM

The required PM material is directly a function of the desired performance of the IPM. This desired performance includes, rated power factor [38], [61], [94] and [95], constant power speed range (CPSR or F) in field-weakening [15], [38], [86], [93] - [95], [97], [99] and [103], maximum back-EMF (electromotive force) and DC-link voltage limitation [85], [89], [97] and [98], uncontrolled generator operating (UCG) mode in field-weakening, which can take place at high speed in case of inverter failure [86], [97] and [139], demagnetization of the PM [15], [87] and [106], efficiency [87]. If a high anisotropy rotor structure (high saliency ratio $\xi$) is designed, then by utilizing
3.2. NATURE OF PMASYNRM

the reluctance torque the required PM can effectively be reduced in a PMaSynRM [31], [38], [61], [75], [86], [87], [99], [114] and [139], see Figure 3.2 (b-1) and (b-2).

Due to the geometrical limitations in the PMaSynRM rotor, the PM material can produce flux only in the q-axis direction, see Figure 3.1 and $\lambda_{PM}$ in Figure 3.2, thus the PM material cannot contribute to the d-axis flux, which is contrary to the IPM. In these machines the stator current is responsible for the d-axis flux production. However, the magnet flux $\lambda_{PM}$ can be chosen to completely compensate the unwanted q-axis flux effect, which is the source of low IPF in the SynRM.

Such a condition is called natural compensation (NC) of the SynRM [15], [38], [86], [93]-[95], [97], [99] and [103]. In this situation IPM or PMaSynRM satisfies
Equation (3.1), where \( i_0 \) is the nominal current of the machine.

\[
\lambda_{PM} = -L_q \cdot i_0
\]  

This condition is presented by a curve in the design space in Figure 3.2 (b-1) and (b-2) that is named "optimal field-weakening design line". This curve shows that the better the rotor anisotropic characteristic (big \( \xi \)) the lower is the required PM
material to satisfy Equation (3.1). IPF in this case will not only stop decreasing at high current angles around \(90^\circ\), see IPF behavior of machine (d) in Figure 3.3 (right) or IPF for \(I_m = 32\) A in Figure 3.3 (left), but will also in fact increase.

Consider specifically, the improved machine design SynRM, see chapter 8. The vector diagram of such machine in MTPA motoring conditions is schematically shown in Figure 3.2 (c). The corresponding point in the design spaces are shown in Figure 3.2 (b-1) and (b-2) by the letter c inside a black circle. The torque and IPF values, at the operating point, are shown in Figure 3.3 (right) at a point marked by (c).

Adding some PM material to this machine, so that Equation (3.1) is satisfied and at MTPA, changes the machine vector diagram to that which is shown in Figure 3.2 (d). The corresponding points in design spaces are shown in Figure 3.2 (b-1) and (b-2) by the letter d inside a black circle, and the torque and IPF values, at the operating point, are shown in Figure 3.3 (right) at a point marked by (d). The resultant airgap linkage flux \(\lambda_m\), see chapter 2, has two components, one from the reluctance variation and the other from the permanent magnets. The reluctance contribution to \(\lambda_m\) is shown by \(\lambda_{r-m}\) and the magnet contribution is shown by \(\lambda_{PM}\) in Figure 3.2 (d). The value of current is the same as in Figure 3.2 (c). The power factor improvement is evidence by comparing these two vector diagrams, compare angle \(\beta\) in the two conditions, and consider that \(\text{IPF} = \sin\beta\).

Due to cross-coupling between the PM and the reluctance fluxes the reluctance performance is improved [31], [96] and [104] both in amplitude and in argument, compare \(\lambda_{r-m}\) vectors in the two diagrams.

PM material has two magnetic effects, not only does it provide the saturating flux that is required for the iron ribs in the rotor, this flux has negative effect on the machine performance, but it also increases the reluctance component of the airgap flux. The latter effect is achieved by magnetically eliminating the iron ribs effect and compensating the q-axis flux linkage, which reduces the negative cross coupling effect of the q-axis stator flux on the d-axis flux and also increases the machine effective saliency ratio. This issue will be addressed briefly but with greater depth later, more details can be found in [31], [96] and [114]. The reasoning above is valid as long as the PM material does not magnetically affect the d-axis flux path in the machine.

Increasing the amount of PM material even more than the values suggested by Equation (3.1), changes the machine vector diagram at MTPA to that shown in Figure 3.2 (e). The corresponding points in the design spaces are shown in Figure 3.2 (b-1) and (b-2) marked by letter e inside a black circle, and the torque and IPF values, at the operating point, are shown in Figure 3.3 (right) at a point marked by (e). As is shown in Figure 3.2 (e), the reluctance flux is weakened due to negative effect of the cross-coupling between the PM and the reluctance fluxes. In this case the segments (conducting layers in the rotor) and stator iron materials are magnetically excited even further in comparison to the SynRM without the magnets, consequently the effective reluctance flux is reduced. Moreover, the magnet flux contribution to the airgap flux pushes the airgap flux angle to higher negative
values and consequently the MTPA current angle is also reduced. Due to the PM material both the airgap flux density and the angle \( \beta \) are increased, therefore, machine torque and IPF are further improved, see Figure 3.3. In this condition the machine has more PM nature than reluctance.

The machine performances in Figure 3.3 are calculated based on the above discussion and considering some simplified assumptions to account for the cross-coupling between the PM and reluctance fluxes. The FEM calculated inductances of the SynRM are used for the calculations. To consider the interaction between the PM and reluctance fluxes it is assumed that the q-axis inductance of the PMaSynRM will be constant and equal to the fully saturated value at constant stator current, according to Equation (3.2).

\[
L_q^{PMaSynRM}(i_m, \theta) \approx L_q^{SynRM}(i_m, 90^\circ)
\] (3.2)
The d-axis inductances of the PMaSynRM and the SynRM are kept equal. Of course, these assumptions are reasonable if the PM does not magnetically affect the d-axis flux conducting iron path significantly. Curves in Figure 3.3 are calculated based on this model.

A better magnetic model can be built up if the SynRM rotor geometry is a little modified for the inductances calculation by eliminating all iron tangential and radial ribs. These ribs are saturated by the PM flux in a PMaSynRM, therefore, from the stator side point of view their reluctance is equivalent to that of air. The new SynRM inductances can thereafter be calculated as before. Using again assumption Equation (3.2) and keeping the PMaSynRM d-axis inductances equal to this modified SynRM d-axis inductances at each value of current.

The cross-coupling between PM and d-axis fluxes is not modeled accurately at high PM flux values, however for PM flux values up to the level determined from Equation (3.1) and a little higher, the results seem reasonable. Accuracy of the explained model will be compared with FEM later. If the inductances and PM flux are somewhat determined then the machine torque and IPF can be calculated according to Equations (3.3) and (3.4) [105], respectively. \( \theta = \beta + \delta \) is the current angle from the d-axis.

\[
T_{ag} = \frac{3p}{2} \cdot ((L_d - L_q) \cdot i_{dm}i_{qm} - \lambda_{PM}i_{dm}) \tag{3.3}
\]

\[
IPF = \frac{\cos\theta \cdot \left( (\xi - 1) \cdot \sin\theta - \frac{\lambda_{PM}i_{qm}}{L_qi_{qm}} \right)}{\sqrt{\xi^2 \cdot \cos^2\theta + \left( \sin\theta + \frac{\lambda_{PM}i_{qm}}{L_qi_{qm}} \right)^2}} \tag{3.4}
\]

Using the method described above, the PMaSynRM airgap torque and IPF are calculated at constant PM flux (\( \lambda_{PM} = psiPM0 = -321 \ mVs \)) for different current angles and current as a parameter, see Figure 3.3 (left). The required PM flux for natural compensation, based on Equation (3.1), of this (studied) SynRM is around 300 mVs. Machine nominal current (100 % load) is \( i_0 = 35.68 \ A \) at 1500 rpm and \( L_q = 6.4 \ mH \) (FEM). The PMaSynRM MTPA has the same behavior as the SynRM. For increasing current, the optimal current angle shifts to the higher angles, due to the effect of saturation and cross-coupling. This is also true for the IPF, see Figure 3.3 (left).

The PMaSynRM airgap torque and IPF are also calculated at constant current (\( I_m = 35.68 \ A \)) for different current angles and PM flux (psiPM0) as a parameter, see Figure 3.3 (right). Increasing the PM flux improves both the machine torque and IPF, it also decreases the MTPA optimal angle.

As was mentioned earlier, in a specific machine, natural compensation can be obtained by selecting the nominal current and setting the PM flux value according to Equation (3.1), see IPF curve in Figure 3.3 (right) for \( \lambda_{PM} = psiPM0 = -353 \ mVs \) here \( I_{m}^{nom} = 35.68 \ A \), or selecting the PM flux value and setting the
nominal current according to Equation (3.1), see IPF curve in Figure 3.3 (left) for $I_m = 32 \, \text{A}$ here $\lambda_{PM} = psiPM0 = -321 \, \text{mVs}$.

### 3.3 Analysis methods overview

The PM effect on the PMaSynRM performance is qualitatively discussed in earlier sections. A quantity of PM flux was used in these sections for the analysis, but in the following the actual simplified method to create this flux in a PMaSynRM is presented. The accuracy of the quantitative analysis using FEM is compared with a Matlab code and the results are discussed in this section as well.

PM materials can be introduced inside the barriers of a SynRM, see rotor in Figure 3.4 (a). Different methods are graphically shown in Figure 3.4 (b), (c) and (d) by black colored regions, without any fine tuning and practical considerations. In geometry (b) the length of the PM is around 20 mm, in geometry (c) the length of the PM is the maximum possible in each barrier and in geometry (d) all available barrier spaces are filled with PM. In all geometries, the PM region width in each barrier is equal to the barrier width.

The open circuit PM flux $\lambda_{PM0}$ is related to the fundamental open circuit airgap flux density $\hat{B}^1_{PM0}$ by Equation (3.5). FEM can be used to calculate $\hat{B}^1_{PM0}$.

$$\lambda_{PM0} = \frac{k_{w1} Q_s n_s}{6 \rho C_s} D L \hat{B}^1_{PM0}$$  \hspace{1cm} (3.5)

Where, $k_{w1}$ is the stator fundamental winding factor, $Q_s$ is the total stator slots

![Figure 3.4](image)
number, \( n_s \) is the number of conductor per stator slot, \( D \) is the airgap diameter, \( L \) is the active length of the machine, \( C_s \) is the winding connection factor, \( p \) is the pole pairs number, [58].

Two PM materials are used to fill in the geometries in Figure 3.4 (b), (c) and (d), first with cheap ferrite and then with high quality NdFeB PM materials see [107]. BM9 is selected for the ferrite PM. The minimum remanence flux density \( B_r \) at 20\( ^\circ \)C for BM9 is 385 mT and the temperature coefficient of \( B_r \) is \(-0.2\ %/\ ^\circ \)C, maximum \( \mu_r \) is 1.3. BM53 is selected for the NdFeB PM, this is not a motor grade PM but the strongest NdFeB in production. The minimum remanence flux density \( B_r \) at 20\( ^\circ \)C for BM53 is 1.44 T and the temperature coefficient of \( B_r \) is \(-0.12\ %/\ ^\circ \)C, \( \mu_r \) is 1.1, see [107] for details. The operating point temperature of the PM is kept constant and equal to the rotor shaft measured temperature which is around 70\( ^\circ \)C taking into account some safety margin. Thus \( B_r \) for the ferrite PM at the operating point will be 346.5 mT and for NdFeB PM it will be 1.3536 T. These values are set in the FEM model for the PM material.

The open circuit PM flux \( \lambda_{PM,0} \) for the geometries shown in Figure 3.4 has been calculated, using Equation (3.5), with both ferrite and NdFeB PM materials. The resultant \( \lambda_{PM,0} \) vs. PM volume is demonstrated in Figure 3.4 (e). The geometries and PM materials combination, creates six PMaSynRMs, and covers a wide-range variation of \( \lambda_{PM,0} \) from 0 up to 1237 mVs. As is evident in Figure 3.4, NdFeB PM material can create the same \( \lambda_{PM,0} \) as the ferrite PM with almost four times lower PM volume, compare \( \lambda_{PM,0} \) of geometry (d) with ferrite and geometry (b) with NdFeB in Figure 3.4 (e). The \( \lambda_{PM,0} \) value in these machines is around the natural compensation value for the SynRM suggested by Equation (3.1), see Figure 3.2, as well.

The six different PMaSynRM and correspondent SynRM torque vs. current angle is calculated by FEM and Matlab code, respectively, the results are shown in Figure 3.5. The \( \lambda_{PM,0} \) effect on torque will be discussed later. As it was explained, the magnetic model of the PMaSynRM can be derived from the corresponding SynRM if somehow the effect of the PM on the SynRM inductances, especially q-axis inductance, are considered and accounted by implementing some simple assumptions and methods. The resultant machine torque calculated from a simplified magnetic model using Matlab is compared with the FEM calculations. The results show acceptable compatibility for ferrite magnets but not for NdFeB magnets, see Figure 3.5 (b). The result confirms the earlier discussion: as long as the magnet does not locally or homogeneously affect the d-axis flux path in iron so that the magnetic operating point of the material is changed, the method is accurate.

This condition shows that the natural compensation PM flux value is really natural, because it just compensates the q-axis flux of the SynRM.

\[
T_{ag} = \frac{3p}{2} \cdot \left\{ [L_d(i_m, \theta, \lambda_{PM,0}) - L_q(i_m, \theta, \lambda_{PM,0})] \cdot i_{qm} + \lambda_{PM} (i_m, \theta, \lambda_{PM,0}) \cdot i_{dm} \right\} 
\]

(3.6)

Increasing the PM flux beyond the natural compensation values activates the
Figure 3.5: Torque vs. current angle for the SynRM and six different PMaSynRMs of Figure 3.4, comparing the results from FEM and the simplified magnetic model. (a) large scale in torque and current angle plan, (b) for the specified region in (a).
cross-coupling effect between the PM and mainly the d-axis flux, see [104] for some calculation results. In this condition the resultant torque of the PMaSynRM is not equal to the scalar sum of the unsaturated PM and reluctance torques according to Equation (3.3), see torque curves in Figure 3.5 (a) for $\lambda_{PM0} > 400 \text{ mVs}$. More realistic values can be reached if somehow the magnetic model accounts for this coupling effect, because torque values seem to obey Equation (3.6). A comprehensive analysis and method to tackle the problem is presented in [31], [96] and [114].

3.4 FEM study

The subject machines under study, the SynRM and six PMaSynRMs, shown in Figure 3.4, have been modeled with FEM software. The current is set to the SynRM nominal (MTPA) current $I_m = 35.68 \text{ Arms}$ at 100 \% load, 50 Hz and 1500 rpm. The PM temperature is kept constant and equal to the rotor temperature 70$^\circ$C. The dominant part of the losses in the machine is copper losses and at constant current it is expected that all machines will experience almost the same temperature and temperature rise. Of course, due to the different PM flux in the PMaSynRMs the flux density will change and consequently the iron losses will be slightly affected. The effect of the PM flux on losses and efficiency will be studied later.

The FEM simulation results for airgap torque, internal power factor (when $R_s = 0$ and end winding leakage $L_{sl-end} = 0$) and the torque ripple vs. current angle are shown in Figure 3.6. Internal power factor (IPFx) is evaluated by calculating the fundamental terminal voltage of the machine, using an external circuit. The end winding leakage and the stator resistance are not considered in these calculations.

Comparison between calculated IPF (when $R_s = 0$), shown in Figure 3.3 (right) and FEM simulated results for IPFx, shown in Figure 3.6, demonstrates a good agreement, at least for those PMaSynRMs that are compensated up to and even little higher than the natural compensation values, see Equation (3.1).

Machine performances at Maximum torque per ampere (MTPA) and maximum IPFx per ampere (MIPFxPA) operating points, based on graphs in Figure 3.6, have been determined and are shown in Figure 3.7 (left) and (right), respectively. Qualitatively the results are compatible with the discussions in the previous chapters.

There is a restrictive fact regarding the relation of $\lambda_{PM0}$ and IPFx at MTPA, see Figure 3.7 (left-bottom), area marked by an ellipse. As long as $\lambda_{PM0}$, refer to Figure 3.7, is less than a certain value (here around 550 mVs) the IPFx at MTPA is sharply and linearly increasing with $\lambda_{PM0}$, but the IPFx improvement effect is weakened when $\lambda_{PM0}$ is increased beyond this value. Increasing $\lambda_{PM0}$ from 0 to 550 mVs improves IPFx by 23 \% – units, but adding the same amount of the magnet again improves IPFx only by 4 \% – units. How can this be magnetically explained? And what is the nature of this phenomena? We will explain this new phenomenon by refering to balance compensation (BC) and will discuss around this subject later.
CHAPTER 3. PERMANENT MAGNET ASSISTED SYMNR (PMASYNRM)

The equi-potential contours, at MTPA and nominal current for all machines, are shown in Figure 3.8. Compare, graphs (a), (d or e) and (f) in this figure with (c), (d) and (e) vector diagrams in Figure 3.2, respectively, especially observe the airgap flux angle $\delta$, see also Figure 3.9. Figure 3.8 (d) shows that in the natural compensated PMaSynRM, it is mainly the d-axis flux that is circulating in each pole face, in fact, magnetically it does not mean any thing else.

In Figure 3.8, (a) - (c) show under compensation (UC), (d) and (e) show natural compensation (NC), (f) shows balance compensation (BC) and (f) and (g) show over compensation (OC), situations for the PMaSynRMs. Each machine current and airgap flux density vectors (actual values) in the dq-plan are also shown in Figure 3.9, compare with Figure 3.2 (c), (d) and (e). In this figure different regions

Figure 3.6: Torque, IPFx ($R_s = 0$, $L_{sl-end} = 0$) and torque ripple as function of current angle for different PMaSynRMs and SynRM (FEM). Rotor and PM temperature is 70°C = cte. at $I_m = 35.68$ Arms.
3.4. FEM STUDY

Figure 3.7: (left) MTPA and (right) MIPFxPA (maximum IPFx per ampere) \((R_s = 0, L_{sl-end} = 0)\) operating points characteristics as function of \(\lambda_{PM0}\) for PMaSynRMs and SynRM (FEM). Rotor and PM temperature is 70\(^\circ\)C cte. at \(I_m = 35.68\) Arms, see also Figures 3.4 and 3.6.
Figure 3.8: Equi-potential contours for SynRM and PMaSynRM machines that are the subject of this study, at MTPA and $I_m = 35.68$ Arms, (FEM).

UC: Under Compensation  
NC: Natural Compensation  
BC: Balance Compensation  
OC: Over Compensation
3.4. FEM STUDY

Stator Current $I_m$ [A/100, Peak] and Air gap Flux density Vector [T] @ $I_m=35.68$ [A rms] & MTPA

Figure 3.9: Airgap flux density and current vectors for SynRM and PMaSynRMs that are the subject of the study, at MTPA and $I_m = 35.68$ Arms, (FEM).
Figure 3.10: Open circuit (left) airgap flux density and (right) voltage for PMaSynRMs of Figure 3.4, (FEM).
3.4. FEM STUDY

Figure 3.11: PM Demagnetization situations, flux density picture, at $I_m = 2.2 \times 35.68$ Arms, $\theta = 90^\circ$. The worst case when PM is Ferrite (BM9, $H_c.B$ (min) = $-270$ kA/m at $20^\circ C$) and it is placed along the q-axis (see bottom-left figure), in this condition $\lambda_{PM0}$ is at minimum. PM Property curve (bottom-right) (FEM), see [107] for BM9 material characteristic.
and their corresponding compensation method are demonstrated graphically by some ellipses. The reluctance flux improvement achieved by increasing $\lambda_{PM0}$ and moving from a SynRM to a NC-PMaSynRM is evident from Figure 3.9, compare the d-axis components of the airgap flux density vectors for cases (a), (b), (c), (d) and (e) in this diagram. The open circuit airgap flux density and terminal voltage and their fundamental harmonic curves are shown in Figure 3.10 (left) and (right), respectively. Magnet distribution affects torque ripple due to the non-sinusoidal open circuit airgap flux density. Specially if high quality PM materials (with high $B_r$) are used then an attempt has to be made to achieve low ripple, for torque ripple see also Figure 3.6. The torque ripple due to the magnet flux can be controlled by using methods described in [60], [86], [100] and [106].

The most critical situation for the PM demagnetization takes place when the current angle is around 90°. The resultant flux contour and flux density arrows for the worst case at $I_m = 2, 2 \times 35, 68$ Arms are shown in Figure 3.11. In case of the ferrite PM and minimum PM material the minimum flux density in the PM in barrier 3 is 15 mT therefore, $-Hc.B$ (from the working line) is around 200, which is lower than the maximum allowed 270 kA/m. In the worst case the flux density inside the PM material is just above the critical value with an acceptable and safe margin, barrier width has a major role here [15] and [106].

### 3.5 Different approaches to the balance compensation of SynRM at MTPA

Three known compensation techniques for SynRM have been discussed and explained in the previous sections. These are Under, see Figure 3.8 (a) - (c), Natural, see Figure 3.8 (d) - (e), and Over, see Figure 3.8 (f) - (g), compensated (UC, NC and OC) PMaSynRM, see also Figure 3.2 (c), (d) and (e), respectively and Figure 3.9.

Equation (3.1) is the key criterion to characterize these techniques, according to Equation (3.7):

\[
UC: \quad |\lambda_{PM}| < L_q \cdot i_0 \quad (1) \\
NC: \quad |\lambda_{PM}| \approx L_q \cdot i_0 \quad (2) \\
OC: \quad |\lambda_{PM}| > L_q \cdot i_0 \quad (3) \quad (3.7)
\]

If the PMaSynRM power factor behavior is also considered, then a new compensation technique can be introduced [139]. Consider an ideal machine, without saturation and cross-coupling and also losses. Typical IPF for this machine versus $\lambda_{PM0}$ (real value + sign) at constant current and MTPA operation is shown in Figure 3.12 (right) and linearized in Figure 3.12 (left), using Equation (3.4) and equation for MTPA current angle in ideal conditions described in [61] and [105].
Observe that non-ideal condition was considered earlier, see Figure 3.7 (bottom-left). The ideal and non ideal conditions are compared in Figure 3.13.

Increasing $\lambda_{PM0}$ beyond NC (here around 300 mVs) improves IPF linearly up to a certain value. This value for PF seems to be between 0.9 - 0.95 and for $\lambda_{PM0}$ to be around 600 mVs. Adding more PM does not improve PF as before. We call this particular PM flux, the balance compensation (BC) limit. The flux vector trajectory of the PMaSynRM in this condition is shown in Figure 3.9. Balance compensation (BC) is a level of over compensation (OC) state in a PMaSynRM, as is shown in this figure. Balance compensation defines the practical limitation boundary for power factor improvement in PMaSynRMs.

Figure 3.8 (f) and Figure 3.2 (e) demonstrate a balance compensated PMaSynRM’s flux contour and vector diagram, respectively. Machines in Figure 3.1 (a) and (c) seems to be in the BC state, as well. Similar to the NC criterion, see Equation (3.7-(2)), there are different possibilities to define a criterion for BC. The PMaSynRM’s power factor can be one choice, e.g. increasing $\lambda_{PM0}$ to satisfy Equation (3.8).

$$BC: \quad |\lambda_{PM}| \uparrow \Rightarrow 0.9 \leq IPF^{PMaSynRM} \leq 0.95 \quad (3.8)$$

At the moment, there is not enough information to verify the other BC criterion definitions, but some ideas, regarding the BC limit definition, are described in the following. More analysis and simulations are required to find the best definition.

The main idea behind the PMaSynRM here is to improve the machine performance in a cost effective way by combining the PM and reluctance concepts and utilizing maximally the reluctance torque. This means that there must be an optimal balance between the reluctance nature and the PM nature of the PMaSynRM regarding the target performance of the machine.

Figure 3.12: Typical open circuit PM flux, $\lambda_{PM0}$, (±sign) and IPF relation at constant stator current and MTPA. Saturation and cross-coupling are not considered here.
The PM machines, especially surface mounted (SPM) types or IPM with high $\lambda_{PM0}$, see machine (g) in Figure 3.8 or Figure 3.13 (right) when $\lambda_{PM0} = 1250 \text{ mV}s$, have high power factors in contrast to the SynRM, see machine (a) in Figure 3.8 or Figure 3.13 (right) when $\lambda_{PM0} = 0 \text{ mV}s$. Combination of this fact and the upper and lower limits in Equation (3.8) gives some idea on possible definitions of BC limits. At BC, the PM property dominates or is at least at the same level as the reluctance property. This can be formulated according to Equation (3.9).

$$BC : \quad (|\lambda_{PM}| \uparrow \Rightarrow \text{Nature}^{\text{Reluctance}} \approx \text{Nature}^{\text{PM}})_{\text{for \, PMaSynRM}}$$

(3.9)

Considering Equation (3.9) for the lower limit of Equation (3.8) can be translated into two BC limit definitions according to Equations (3.10) and (3.11), $\delta_{0}^{\text{SynRM}}$ and $\delta_{0}^{\text{PMaSynRM}}$ in Equation (3.10) are the $\lambda_{m}$ vector angle, see Figure 3.2, or load angle of the SynRM and the PMaSynRM, respectively.

$$BC : \quad |\lambda_{PM}| \uparrow \Rightarrow \delta_{0}^{\text{SynRM}} \approx |\lambda_{PM}^{\text{PMaSynRM}}|$$

(3.10)

$$BC : \quad |\lambda_{PM}| \uparrow \Rightarrow 2 \times \lambda_{r-qm}^{\text{SynRM}} \approx |\lambda_{PM}^{\text{PMaSynRM}}|$$

(3.11)

Equation (3.11) is another representation of Equation (3.10), where $\lambda_{r-qm}^{\text{SynRM}}$ is the q-axis flux of the SynRM and $\lambda_{PM}^{\text{PMaSynRM}}$ is the PMaSynRM’s PM flux.

The airgap flux vector $\lambda_{m}$ position with reference to the d-axis, before and after introducing the PM in the SynRM, is used in Equations (3.10) and (3.11).
3.5. DIFFERENT APPROACHES TO THE BALANCE COMPENSATION OF SYNRM AT MTPA

Qualitatively, at BC the flux angle must be equal or higher than in the SynRM or at least $\lambda_m$ of the PMaSynRM must be a mirror image of $\lambda_m$ of the SynRM with respect to the d-axis, compare machines (a) and (f) flux vector positions in Figure 3.9. Here it is assumed that the reluctance nature can be modeled by $\lambda_{r-qm}^{SynRM}$ and PM nature by $\lambda_{PM}^{PMaSynRM}$ values. Equation (3.11) can be written by just considering the PM and Reluctance part of $\lambda_m$ in the PMaSynRM according to Equation (3.12), as well.

\[
BC : \left( |\lambda_{PM}| \uparrow \right) \Rightarrow 2 \times \lambda_{r-qm} \approx |\lambda_{PM}|^{for \ PMaSynRM} (3.12)
\]

Similarly, considering Equation (3.9) for the upper limit of Equation (3.8) can be translated into three BC limit definitions according to Equations (3.13), (3.14), [31], and Equation (3.15). The parameters in Equation (3.13) are defined in Figure 3.2, and $\theta = \beta + \delta$ in Equation (3.15) is the machine stator current angle.

\[
BC : \left( |\lambda_{PM}| \uparrow \right) \Rightarrow |\lambda_{r-m}| \approx |\lambda_{PM}|^{for \ PMaSynRM} (3.13)
\]

\[
BC : \left( |\lambda_{PM}| \uparrow \right) \Rightarrow T_{r-m} \approx T_{PM}^{for \ PMaSynRM} (3.14)
\]

\[
BC : \left( |\lambda_{PM}| \uparrow \right) \Rightarrow \theta \text{ at } MTPA \approx 45^\circ^{for \ PMaSynRM} (3.15)
\]

The base of the NC limit, see Equation (3.7-(2)), can be used for BC as well, see Equation (3.6), compare machines (a), (d) and (f) flux vector position in Figure 3.9.

\[
BC : \lambda_{PM} \approx 2 (-L_q \cdot i_0) (3.16)
\]

Analysis of limits given in Equations (3.12) - (3.16) needs some kind of technique to separate the reluctance and PM flux parts of the airgap flux vector $\lambda_m$ in PMaSynRMs, this issue is out of the scope of this thesis, however, some useful ideas can be found in [31], [96], [104] and [114]. According to Equation (3.8) the upper limit of the IPF for BC is considered to be around 0.95, the corresponding $\lambda_{PM0}$ is around 700 mVs here, see Figure 3.13 (right) and the related current angle at (MTPA) is 45°, see Figure 3.7 (middle-left). This is supported by Equation (3.15) as well.

All conditions for balance compensation’s lower limits, Equations (3.10), (3.11) and (3.12) and upper limits, Equations (3.13), (3.14), (3.15) and (3.16) in PMaSynRM have to be studied more closely to characterize the criterion of the new ideas accurately and investigate the PMaSynRM performance at BC situation. Comparison between BC and other compensation states of PMaSynRM could be practically interesting, as well.
3.6 PMaSynRM machines performance and characteristic

A similar procedure to that which will be used later in chapter 10 is used to simulate each machine performance using calibrated terminal variables values. The stator current is set equal to the SynRM nominal current (35.68 Arms) at 100 % load and 1500 rpm for all machines (in chapter 10 the torque is kept constant for all machines). The reluctance machine current in this condition is 30 % higher than the correspondent IM. A simple thermal model based on the Broström’s formula, which is described in chapter 10 see Equation (10.1), is used for the stator temperature rise estimation. The IM with the same stator and almost the same temperature rise and at 1470 rpm (50 Hz) has 11 kW output shaft power (71,4 Nm), 92 % efficiency (IEC 91,7 % and according to the measurement in VSD supply condition less than 91 %) and 0,83 terminal power factor.

In the simple thermal model, variation of the stator temperature rise coefficient $k_{cs}$ versus speed is taken into account by using $k_{cs}$ values for different pole numbers. Thus, the speed variation effect of the cooling fan on temperature rise is modeled, as well.

The PM temperature for all machines is kept constant, speed independent, and equal to the measured rotor temperature of the SynRM at the thermal nominal condition. Due to the domination of the copper losses over the iron losses, this assumption seems reasonable at 1500 rpm. Of course, the rotor temperature will be changed (decreased) with speed but for relative comparison this assumption is acceptable, because the magnet temperature is assumed to be equal to the maximum value and the temperature of all the machines will be almost the same at the same speed. This will be shown later. Iron losses are slightly different for different machines. The simulation results are summarized in Figure 3.14 for different medium range speeds, and Figures 3.15 and 3.16 for 1500 rpm.

Increasing $\lambda_{PM0}$ overall improves the PMaSynRM performance in comparison to the SynRM and IM in the entire speed range, 500 – 4000 rpm, see Figure 3.14.

The simulated temperature shows that all machines experience almost the same temperature rise as the SynRM with a difference margin of less than 5 K except for the machines that are highly over compensated, see Figure 3.14 (bottom-right). The small increase in the temperature rise of the machine, with high $\lambda_{PM0}$, is due to the slightly higher iron losses.

The effect of $\lambda_{PM0}$ on efficiency is limited to less than 2 % when moving from SynRM to the maximum possible over compensated PMaSynRM, see machine (g) in Figure 3.8. However the machine’s PF, T and (PF times efficiency) factors are significantly improved by around 25 % – units, 80 % and 26 % – units, respectively, almost over the entire speed range.

Simulation shows that a high under or slight natural compensated PMaSynRM (with $\lambda_{PM0} \approx 250$ mVs in this case here) and almost at the same temperature rise will have the same (PF x efficiency) as the IM, but with 55 % higher shaft power (17 instead of 11 kW) and 2.5 % – unit better efficiency, see Figures 3.15 and 3.16.

PMaSynRM simulated performance at 1500 rpm shows that natural compensa-
Figure 3.14: Effect of $\lambda_{PM0}$ on the PMaSynRM performance in medium speed range MTPA operation, stator resistance and friction losses are calibrated with measurements, stator temperature rise coefficient $k_{cs}$ is a function of speed using different pole number values, PM temperature $= cte = 70^\circ C$ = rotor temperature at 1500 rpm, $I_{m} = 35.68 \text{ Arms} = cte$. See the geometries of the machines in Figure 3.8.
Figure 3.15: Effect of $\lambda_{PM0}$ on the PMaSynRM performance at 1500 rpm, $I_m = 35.68$ Arms = cte. and MTPA operation, PM temperature = cte = 70°C, see also Figure 3.14. The geometry of the machines are shown in Figure 3.8.
3.7 Conclusion

Permanent Magnet assisted Synchronous Reluctance Machine (PMaSynRM) has been qualitatively and quantitatively analyzed in this chapter. This is a primary study to address the main characteristics of such a machine and to determine its advantages and disadvantages.

The study shows that the major drawback of the Synchronous Reluctance machine (SynRM), which is the poor power factor in comparison to the Induction Machine (IM), can be overcome by introducing a small amount of the Permanent Magnet (PM) material in the machine. The new machine that has still dominant reluctance behavior and therefore this type of motor is called PMaSynRM.

The main idea behind the PMaSynRM here is to improve the machine per-
CHAPTER 3. PERMANENT MAGNET ASSISTED SYNRM (PMASYNRM)

<table>
<thead>
<tr>
<th>Motor Mass of PM (kg)</th>
<th>SynRM</th>
<th>Ferrite</th>
<th>Ferrite</th>
<th>Ferrite</th>
<th>Ferrite</th>
<th>Ferrite</th>
<th>Ferrite</th>
<th>Ferrite</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0</td>
<td>1.5</td>
<td>2.6</td>
<td>5.5</td>
<td>2.3</td>
<td>3.9</td>
<td>8.4</td>
<td></td>
</tr>
<tr>
<td>Relative Power Output at Same Current (%)</td>
<td>100</td>
<td>107</td>
<td>111</td>
<td>124</td>
<td>119</td>
<td>131</td>
<td>178</td>
<td></td>
</tr>
<tr>
<td>Power Factor (*)</td>
<td>0.72</td>
<td>0.76</td>
<td>0.79</td>
<td>0.86</td>
<td>0.86</td>
<td>0.92</td>
<td>0.97</td>
<td></td>
</tr>
<tr>
<td>Efficiency (%)</td>
<td>92.8</td>
<td>93.2</td>
<td>93.4</td>
<td>94</td>
<td>93.6</td>
<td>93.9</td>
<td>94.9</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.1: PM material and relative performance comparison for the PMaSynRM machines in Figure 3.8 at 1500 rpm, $I_m = 35.68 \text{ Arms} = \text{cte. and MTPA operation.}$

formance in an effective way by combining the PM and reluctance concepts and utilizing maximally the reluctance torque. This means that there must be an optimal balance between the reluctance nature and the PM nature of the PMaSynRM regarding the machine target performance.

Such a machine has a complicated and mixed reluctance and PM magnetic behavior. Due to the reluctance domination, a separation technique is required to separate the reluctance and PM flux behavior. Otherwise simulation by FEM has to be implemented, which is time consuming. The main reason for the complex nature of the analysis is the cross-coupling between the d-axis, q-axis and PM ($\lambda_{PM0}$) fluxes in PMaSynRM.

PMaSynRM can be classified based on the amount of the q-axis flux compensation in the corresponding SynRM. Four different classes of compensation for SynRM have been explained and characterized, which are under, natural, balance and over compensation. Balance compensation (BC) is some kind of over compensation state in a PMaSynRM. Under, natural and somewhat balance compensations improve the reluctance behavior of the PMaSynRM in comparison to the SynRM due to the positive effect of the cross-coupling between the reluctance and the PM fluxes at low compensation levels.

The natural compensation expands the field-weakening range of a PMaSynRM to infinity, as well. The balance compensation characterizes the practical limitations for power factor improvement in a PMaSynRM. Over compensated PMaSynRM, due to high permanent magnet flux in comparison to the reluctance flux, behaves more like a surface mounted PM machine (SPM) with much higher PF compared to a reluctance machine.

Practical considerations such as PM material quality and available free spaces in the rotor geometry of the PMaSynRM limits the maximum $\lambda_{PM0}$ which can be added to the machine structure to less than the nominal airgap flux of the corresponding SynRM.

The effect of $\lambda_{PM0}$ on efficiency is limited to less than $+2\%$ which is achieved by moving from SynRM to the maximum possible over compensated PMaSynRM, but the machine’s PF, T and (PF x efficiency) factors are significantly improved by around 25 $\% – \text{units}$, 80 $\%$ and 26 $\% – \text{units}$, respectively, see Figure 3.14 and Table 3.1, almost over the entire speed range and below base speed.
3.7. CONCLUSION

Simulation shows that a high under or slight natural compensated PMaSynRM and at almost the same temperature rise will have the same \((\text{PF} \times \text{efficiency})\) as the IM, but with 55\% higher shaft power (17 instead of 11 kW here) and 2.5\% – units better efficiency. PMaSynRM simulated performance at 1500 \text{rpm}, see Table 3.1, shows that the natural compensation improves the SynRM machine’s \text{PF}, \text{T}, \text{efficiency} and \((\text{PF} \times \text{efficiency})\) factors by around 14\% – units, 22\%, 1\% – unit and 13\% – units, respectively and similarly, the balance compensation (BC) improves these factors by around 20\% – units (to 0.919), 30\%, 1.1\% – units and 19\% – units, respectively. The iron losses for the BC-PMaSynRM and the NC-PMaSynRM are just 30\% and 11\% higher than the SynRM, respectively. In this study due to almost constant temperature rise and stator current in all machines the copper losses are constant.

Introducing PM materials in the SynRM machine improves the machine performance, however this raises some problems in the machine operation as well as production and operation. For example, the PM materials are sensitive to the temperature, therefore, increased temperature in PM during normal operation due to extra losses in the PM can reduce the machine torque capability. Demagnetization is another risk in IPM machines due to high temperature as well as transient conditions in short circuit conditions and UCG operation of these machines. Using PM material clearly, increases the initial cost as well as production cost (handling the PM material in production line) and maintenance cost during operation. The availability of PM material is also another risk which can further increase the IPM machine production cost. In IPM machines field-weakening as well as normal operation requires more complicated control method adaptation, for both hardware and software developments, as well due to protection issues in faulty conditions in comparison to IM and SynRM machines.
Chapter 4

SynRM: an investigation around suitable barrier’s shape

Finding a suitable rotor geometry for the Synchronous Reluctance Machine (SynRM) has been a subject for major investigation since 1923 till now [2], [3], [16] and [31]. The machine performance and its lumped circuit model, see Figure 2.1, was presented earlier in chapter 2, page 21. This chapter will investigate the interior barrier (magnetic insulation layer) rotor structure of SynRM, as suggested by J. K. Kostko [2], using the Finite Element Method (FEM) based sensitivity analysis. The main goal is to investigate his idea and at the same time search for the most important geometrical parameters of the rotor that affect the machine torque capability? Finding a simple and general rotor barrier shape is another target. The simplest rotor that has just one barrier will be used in the investigation.

4.1 A general barrier’s shape proposal

The proposed rotor structure has to be flexible enough and at the same time simple. Flexible in order to cover almost all possible geometries and simple to be able to distinguish between parameters (here called microscopic) that are important and those that are not effective. Simplicity is very important otherwise the main effective geometrical parameters can not be detected and the only way that remains there after is to follow an optimization algorithm. Most of these solutions present a time consuming procedure combining some kind of FEM calculation with mathematical optimization algorithms. Such examples can be found in [49] - [52]. Taking these notes into consideration, the rotor geometry for salient pole and one interior barrier structure is shown in Figure 4.1. Each microscopic parameter of the geometry is defined in this figure.

The constant rotor slot pitch idea was used in [40] and [43] for ripple control, see $\alpha_m$ in Figure 4.1. There, constant rotor slot pitch is adapted to the unsymmetrical rotor structure of the SynRM by the definition of an imaginary rotor slot opening in
total iron in q-axis = \( l_y = S_1 \)

total air in q-axis = \( l_a = W_1 \)

\[
\begin{align*}
S_1 & S \quad l_q \\
\alpha & \alpha_m \quad \frac{\alpha_m}{2} \\
W_1 & \quad \alpha_2 \quad \frac{\alpha_2}{2} \\
\end{align*}
\]

\( l_a + l_y \)

---

total iron in q-axis = \( l_y = S_1 + S_2 \)

total air in q-axis = \( l_a = W_1 \)

\[
\begin{align*}
S_1 & S_2 \quad Y_q \quad W_1 \\
\alpha & \alpha_m \quad \frac{\alpha_m}{2} \\
\alpha_2 & \quad \frac{\alpha_2}{2} \\
\end{align*}
\]

\( l_a + l_y \)

---

Figure 4.1: Proposed rotor geometries and related microscopic and macroscopic parameters definition for (top) salient pole (SP) and (bottom) SynRM with one interior barrier, for more detail refer to [16] where a simple approach for \( l_d \) definition is given. Do not confuse \( W_1 \) that is the barrier width in the q-axis and \( W_1d \) that is the barrier width in the d-axis over \( l_d \).
4.2 SynRM with one barrier

Using a rational and general rotor barrier shape will reduce the number of geometric parameters that are involved. However, the parameters in Figure 4.1, which are referred to as microscopic, are dealing with the barrier and segment dimensions. Using these microscopic parameters it is very difficult to characterize the rotor anisotropic structure behavior [16].

Independent studies have tried to deal with macroscopic parameters, which can be used to characterize a SynRM rotor anisotropic structure behavior [3], [16], [32], [53] and [54]. The simplest rotor geometry with one barrier will be used to investigate the effect of rotor dimensions on the machine performance, mainly torque maximization. Simultaneously, an attempt will be made to determine which macroscopic parameters that correspond to the microscopic parameters in Figure 4.1 [16].

4.2.1 Insulation ratio in the q-axis

A macroscopic geometry parameter of the SynRM is for the first time introduced and is theoretically and analytically (and at the same time by some FEM simulation as well [3], [32] and [53]) connected to the machine d-and q-axis inductances in [3], [32], [53] and [54]. This geometric parameter is introduced in two different ways by different authors. Vagati et al. [3], [54] emphasizes on the total amount of insulation along the q-axis and inside the rotor \( l_a \), see Figure 4.1. On the other hand, Lipo et al. [3], [53] and Miller et al. [3], [32] introduce the insulation ratio, which is defined as the ratio of thickness of total insulation \( l_a \) over total iron conducting material \( l_y \) inside the rotor \( k_{wq} = l_a/l_y \), see Figure 4.1.

Both of these macroscopic parameters \( k_{wq} \) and \( l_a \) attempt to represent the feature of the machine anisotropic structure quality as both the torque capability and power factor can be characterized by these parameters through analysis of their effect on \( (L_a - L_q) \) and \( (\xi = L_d/L_q) \) [3] and [16]. One barrier rotor geometries for SP (salient pole) and interior barrier machines are shown in Figure 4.2. Three simple rules are considered for introducing the insulation for sensitivity analysis by FEM [16]:

a) The amount of insulation is given by the variable \( k_{wq} \) and/or \( l_a \).

b) The width of the segments is preferably constant for equal flux density along each segment.

There is another simple rule for sizing of the barrier in the d-axis that makes it possible to achieve the initial condition of barrier position in the rotor:
c) Barrier opening equivalent angle, see $\alpha_2$ in Figure 4.1, and position of the center of barrier opening in the airgap should follow the rotor slot pitch, therefore, $\alpha_2 = \alpha_m/2$ in Figure 4.1.

This means that for one barrier and a 4-pole machine, considering an extra imaginary point, shown by a star in Figure 4.1, the second segment along the q-axis, the rotor slot pitch is, see Figure 4.1:

$$\alpha_m = (\text{Angle between } d \text{ and } q \text{-axis})/2 = (\pi/4)/2 = 22.5^\circ. \quad (4.1)$$

Therefore, the equivalent angle of barrier opening in the airgap and angle between axis of barrier and the d-axis are almost equal to $\alpha_2 = \alpha_m/2 = 11.25^\circ$, see Figure 4.1. For the first approximation it is assumed that the amount of iron ($= S_1 + S_2$ here, see Figure 4.1 (bottom)) in the rotor and the stator back ($= 25, 5 \text{ mm}$ here) are equal (so $S_1 + S_2 = 25.5 \text{ mm}$ here). Thus barrier width in the q-axis ($W_1$) will be (where $l_a + l_y = 55, 45 \text{ mm}$ here):

$$W_1 = (l_a + l_y) - 25.5 = 55, 45 - 25.5 = 29.95 \text{ mm}. \quad (4.2)$$

To calculate each segment size, the assumption that the segment width is proportional to the per-unit d-axis magneto motive force [3] and [16] over each segment is used. This issue will be discussed later in chapter 6 on page 105. This gives segment one a width of about $S_1 = 12.3 \text{ mm}$ and segment two a width of about $S_2 = 13.2 \text{ mm}$. Consequently the radial position of the barrier in the q-axis becomes $Y_q = 53.8 \text{ mm}$. Using rules (b) and (c) on page 85 makes it possible to position and size the barrier in the d-axis. The d-axis barrier width ($W_{1d}$) for airgap opening which should be around $\alpha_2 = 11.25^\circ$ will be 16 mm. Remember that due to rule (b), the barrier edges in the q-axis is kept orthogonal to this axis.
while in the d-axis they are in parallel to the d-axis, the reason will be shown later. Starting from this initial condition and changing the barrier size in the q-axis and proportionally in the d-axis (by keeping $Y_q = \text{cte.}$), the insulation ratio effect on machine torque and ripple is studied. The result is shown in Figure 4.2 (right), which shows that the maximum torque of around $30,5 \text{Nm}$ is obtained at an optimal barrier width of $22,5 \text{mm}$ which is smaller than the initial value of $29,95 \text{mm}$. If in the same way, the study is repeated for the SP machine then the maximum torque is around $26 \text{Nm}$ and the optimal barrier width is $28 \text{mm}$, see Figure 4.2 (left), which is close to the initial guess.

The SP and one interior barrier machines study show that just by introducing the insulation inside the rotor, as in Figure 4.1 (bottom), increases the torque by 17%. This improvement was presented and shown by J. K. Kostko in 1923 [2]. The required $W_1$ for the interior rotor geometry is $22,5 \text{mm}$ and lower than SP, $28 \text{mm}$. This can be explained by the positive effect of the second segment in capturing more d-axis flux from the stator and thereby increasing $L_d$, because maximization of torque strategy needs maximum possible iron in the rotor structure [3], [16] and [51].

### 4.2.2 Radial position and the q-axis insulation ratio

If, instead of insulation ratio in the q-axis, the barrier position in the q-axis ($Y_q$) is considered as the first parameter for analysis then there will be two bounding situations. First, introducing a q-axis barrier with a small width, for example equal to the stator slot width. Second, the use of one barrier with maximum possible width i.e. here $29,95 \text{mm}$. Both of these have been analyzed and the results are shown in Figure 4.3. The same analysis for optimum insulation ratio ($W_1 = 22,5 \text{mm}$), is shown in Figure 4.3 too.

In both cases (small and large barrier width) the maximum achievable torque does not exceed $27 \text{Nm}$. But, for the optimum insulation ratio as is shown in Figure 4.3 (b), torque can reach a value that is higher than $31 \text{Nm}$. In this case the optimum value of $Y_q$ is $54 \text{mm}$, which is quite close to the initial value ($Y_q = 53,8 \text{mm}$). The problem complexity will arise if the number of barriers is increased. The results in Figure 4.3 show firstly, that the main priority for optimization is finding the optimum insulation ratio. Secondly, torque sensitivity in $Y_q$ is small if the barrier width is optimally selected (see Figure 4.3 (b)). $L_q$ is inversely proportional to $l_a$ and changing $Y_q$ does not change $l_a$. Thirdly, comparison between graphs in Figure 4.3 (a) and (c), clearly shows the existence of an optimum barrier width $W_1$ and an optimum for its related position in the q-axis ($Y_q$), inside these boundaries.

Parameter $Y_q$ for each barrier around optimum torque point is a strong tool to control the torque ripple without interference with the average torque. What is important about $Y_q$ is that adjusting $Y_q$ makes it possible to adjust the end points of the barrier at the air gap, which directly determines the torque ripple [16], [40], [42], [43] and [48], and at the same time it does not change the average torque. In fact torque around the maximum torque point is a function of the insulation ratio,
mainly in the q-axis [16], see Figure 4.3 (b).

4.2.3 Insulation ratio in the d-axis

The q-axis insulation ratio sensitivity analysis was based on simple assumptions on the d-axis insulation ratio according to rules (b) and (c) in page 85. The initial value for the barrier width in the d-axis ($W_{1d}$) was 16 mm. The corresponding $W_{1d}$ to the optimal $W_{1} = 22.5$ mm according to Figure 4.2 (right) and Figure 4.3 (b) is 12 mm. Now by keeping $W_{1} = 22.5$ mm = cte. and $Y_{q} = 54$ mm = cte. the barrier width in the d-axis is changed. The machine torque change is shown in Figure 4.4 (left) by varying $W_{1d}$.

Similar to $k_{wq}$, the insulation ratio in the d-axis ($k_{wd} = W_{1d}/l_{y}$) affects the machine torque. The optimal $W_{1d}$ is 10.5 mm and close to 12 mm, but it is smaller than $W_{1} = 22.5$ mm. This can be explained based on rule (b) on page 85, Figure 4.1 (bottom) and Figure 4.4 (right).

The insulation ratios in the q- and d-axis are defined in Figure 4.4 (right). If we assume that the iron width ($l_{y}$) in the d- and q-axis are the same, based on Figure 4.4 (right) a simple rough relation between $k_{wq}$ and $k_{wd}$ can be derived according to Equation (4.3).
4.2. SYNRM WITH ONE BARRIER

Figure 4.4: (left) Effect of barrier width in the d-axis on torque for constant barrier width and position in the q-axis, (right) the q- and d-axis insulation ratios relation.

\[
\frac{l_d}{l_a + l_y} = \frac{W1_d + l_y}{W1 + l_y} = \frac{\frac{W1_d}{l_y} + 1}{\frac{W1}{l_y} + 1} = \frac{k_{wd} + 1}{k_{wq} + 1} = \frac{l_d}{l_a + l_y} \leq \frac{(D/2 - g) \cdot \sin\left(\frac{\pi}{2p}\right)}{(D/2 - g - \frac{D_{shatt}}{2})} = a \quad (4.3)
\]

Therefore, \( k_{wq} \) and \( k_{wd} \) are interconnected by a simple relation according to Equation (4.4).

\[
k_{wd} \leq a \cdot k_{wq} + (a - 1) \quad (4.4)
\]

For the machine, which is the subject of the study here, \( a \approx 1 \). Therefore, it can be simply stated that in a 4-pole machine if the second rule is followed then: \( k_{wd} \leq k_{wq} \).

The \( k_{wq} \) and \( k_{wd} \) sensitivity analysis shows that at least there are two macroscopic parameters that have to be optimized for torque maximization, see Figure 4.2 (right) and Figure 4.4 (left). The optimization of these parameters can be done independently and sizing of barrier in the d-axis according to rule (c) on page 86 can be ignored.

4.2.4 Barrier leg angle

The sensitivity analysis in last sections was based on an assumption that the angle \( \alpha \) in Figure 4.1, which is assumed to be 135° is the best choice. Effect of this angle on machine torque when \( W1 \) and \( Y_q \) are constant and at their optimal values is shown in Figure 4.5 (left). As can be seen in this figure, the optimum value for \( \alpha \)
is around 135°, which means parallel segment edges with the d-axis, if rule (b) on page 85 is to be followed. The analysis by analytical method in [42] and [48] shows the same behavior for \( \alpha \).

### 4.2.5 Optimum q-axis barrier positioning

In this section the effect of changing the radial position of the barrier in the q-axis \( (Y_q) \) on machine torque is studied, assuming that the insulation ratios and end points of barrier in the airgap are kept unchanged. This will show how critical is the constant segment width and the relative division of iron between the two segments inside the rotor? The result of such analysis is shown in Figure 4.5 (right). The best value is \( Y_q = 54 \text{ mm} \). The analysis clearly shows that the most important specifications of a barrier in the rotor are insulation ratios and end-point positions of the barrier in the airgap.

Regarding the amount of the iron in the rotor it seems that the division of iron between two segments in the q-axis is not a sensitive parameter and rule (b) on page 85 seems here to be of lesser significance. This observation shows that there are rotors with different structure that have torque close to optimum. However rule (b) can still be used as a strong guide to find the best insulation ratio very close to the absolute optimum value at the first steps of the design. Results from more detailed analysis can then be used for final tuning of the optimum rotor structure.

### 4.3 One barrier geometry analysis conclusion

By analyzing the effect of the main microscopic parameters which are involved in barrier geometry for two most simple situations, see Figure 4.1, the following general rules can be derived:
4.3. ONE BARRIER GEOMETRY ANALYSIS CONCLUSION

1. Introducing maximum anisotropy in the rotor to find optimum insulation ratio, or total air in the q-axis \( (l_a) \) is a major task.

2. The width of the segments all along the length of each one must be kept constant to achieve almost constant flux density in the segment and to increase the utilization factor of rotor iron.

3. A simplified but general shape of segments and barriers will be similar to those shown in Figure 4.1.

4. The edges of barrier and segments in the d-axis must be parallel to the d-axis and perpendicular to the q-axis, \( \alpha = 135^\circ \) in Figure 4.1 is the best choice.

5. The insulation ratio in the d-axis also can, independent of the insulation ratio in the q-axis, be considered as another parameter for positioning the barriers and end points of barriers in an optimal way in the d-axis. This technique is much more effective than the equivalent opening angle of the barrier in the air gap according to rule (c) in page 86 to position the end points of rotor barriers in the airgap.

6. The barrier slot opening position has to follow the rotor slot pitch according to Figure 4.1.

7. The insulation ratio in the d-axis of machines with more than two pole pair is smaller than the insulation ratio in the q-axis. As is shown \( k_{wd} \leq k_{wq} \) or \( W_{kd} \leq W_{1k} \) for \( p > 1 \).

8. Parameter \( Y_q \) for each barrier around optimum torque point is a strong tool to control the torque ripple without interference with the torque. Therefore, some kind of independent torque maximization and torque ripple minimization is possible for SynRM.

9. The simple procedure explained here can give a rotor geometry very close to the absolute optimum, but the best rotor structure for SynRM considering practical limitations is not unique.

10. SynRM rotor with interior flux barrier arrangement has better performance than the traditional salient pole machine. A SynRM with just one interior barrier has 17% more MTPA compared to the traditional salient pole machine.
Chapter 5

SynRM: Initial machine design

A rough design method has been used to optimize a high performance SynRM rotor [22]. The method is based on general rules that are governing the anisotropic structure of the SynRM rotor behavior. These rules in general terms are discussed in [3], [16], [32], [53] and [54]. This machine will be studied to demonstrate the effect of airgap length and ribs dimensions on its performance. Finally, a heat-run test has been performed on a prototyped SynRM and its corresponding IM and Interior Permanent Magnet (IPM) Machine to investigate the potential of the SynRM, under variable speed drive (VSD) supply conditions and to compare it to its counterparts, the IM and the IPM machines. This chapter gives the state-of-art based on measurement on the initial prototype SynRM [17] and benchmarks its performance.

5.1 Rough rotor design concept of the SynRM

This method can be expressed as follows:

Firstly, decide on the number of barriers per pole. The practical choice can be determined based on analysis in [3], [16], [43], [53] and [54]. Secondly, define an insulation ratio especially in the q-axis [3], [16], [32], [53] and [54]. Finally, by using some simple finite element methods [3], [16], [32] and [53] try to find the best insulation ratio in the q-axis.

This kind of design procedure is used in [3], [32] and [56] and all axially laminated anisotropy (ALA) [3] type designs. In this work, it will be used to optimize a SynRM rotor. The final geometry is shown in Figure 2.7 (e) and Figure 5.1 (Middle). The FEM simulation of this machine is used in this chapter to show the effect of the airgap length [35].

A prototyped version of this design is used for verifications and measurements and for performance comparison to its counterpart IM and IPM machines. The
overload capacity of the SynRM study in section 2.3.3 on page 44 was based on this machine [16].

The IPM machine is shown in Figure 5.1 (right). This machine is kind of a balance compensated synchronous reluctance machine, where the magnet flux is almost two times the q-axis flux and it opposes the q-axis flux due to the reluctance structure. The balance compensated SynRM is discussed in chapter 3 on page 55. The IPM machine in Figure 5.1 (right) is saturated with $\xi \approx 4$ and in the MTPA region the reluctance torque and magnet torque are almost equal. This IPM machine will be used for benchmarking measurements on IM, SynRM and IPM machines in this chapter.

5.2 Airgap length

The airgap length ($g$) has a considerable effect on the d-axis inductance $L_d$, but negligible effect on the q-axis inductance $L_q$. The airgap length variation effect on the inductances of the SynRM machine, see Figure 5.1 (Middle), is presented in Figure 5.2 (a), a similar study can be found in [35]. The study shows that the airgap length $g$ must be kept as low as possible in order to increase the torque and the only limitation is due to mechanical considerations.

If the torque ripple is considered this $g$ reduction will increase the torque ripple (also the iron losses), because of the increase in the Carter’s factor as this is general for all slotted stators. In this case circulating q-axis flux at the end of the segments is also increased [3] and [16].

The fact that $L_q$ is not affected by the change in the airgap length can be explained by the reduced effect of d-axis cross-magnetization on the q-axis inductance [35], and the different nature of $L_d$ and $L_q$. Generally, the d-axis inductance is inversely proportional to $g$, total air that the d-axis flux is crossing, and $L_q$ is inversely proportional to $(l_a + g)$, total air that the q-axis flux is crossing. As $(l_a \gg g)$, $L_d$ is much more sensitive to the airgap changes than $L_q$. This subject is widely explained in [16], [24] and [32] and somewhat also in [3].
5.3 Rib dimension

Mechanically strong and self-sustained rotor structure is sought because the rotor structure in Figure 5.1 (Middle) is not suitable for high speed applications. One way is to introduce some tangential and radial ribs in barriers and connect the segments to each other [3], [16], [35] and [54]. These ribs will be saturated by the q-axis flux, during normal operation. Radial rib width effect on torque reduction is shown in Figure 5.2 (b). By increasing the rib width the required flux to saturate the rib is increased and therefore, the torque is reduced. The relation is linear. Some analytical estimation of torque reduction presented in [16], [35] and [54] show the same behavior. The rounding radius of the rib corners effect is also investigated by FEM simulation, see Figure 5.2 (c), which shows that the rounding radius has a negligible effect on torque [16].

Figure 5.2: (a) Airgap length $g$ effect on machine [22] inductances, inductances difference and saliency ratio. (b) Effect of radial rib width and (c) radial rib radius (at width 2mm), on torque.
5.4 Heat-run test on SynRM, IM and IPM

The SynRM and other machine types, especially IM, performance comparison has been a major research issue since 1923 and especially during 1990s till now [2], [3], [14], [15], [19] - [21], [22] and [26]. The experimental comparison between these machines performance has been reported in different literatures. Some of these reports can be found in [19] and [22] - [26].

The performance of a specific SynRM [17], [22] (see its rotor geometry in Figure 5.1 (Middle) and Figure 5.4) has been evaluated through heat-run tests and compared with the corresponding IM and IPM machines, refer to Figure 5.1 (left) and (right), respectively. The test-bench is shown in Figure 5.3. Two test conditions have been considered, constant shaft torque and constant winding temperature-rise for all machines at 500, 1500 and 2500 rpm speed. The test condition is almost the same as is explained in [19].

The test set-up includes a power analyzer connected at the terminal of the test machine for measurement of electrical parameters and a torque transducer mounted on the shaft, coupling the load and the test machine, for the evaluation of mechanical parameters. Measuring the output voltage and current of the inverter
could be done by the Fast Fourier Transform (FFT) function provided by the power analyzer. Some thermal probes are used in critical parts of the test set-up to both measure and protect the machines. The probes determine the temperature-rise of the test machine and room ambient temperature. The average winding temperature is measured by a DC current test immediately after the heat-test.

All machines, the IM, IPM and SynRM, are tested with the same set-up, in order to reduce measurement errors. A flexible laboratory inverter is used to supply all motors. A speed sensor takes care of the shaft speed. The inverter operates with a constant switching frequency of 10 kHz. The DC-link voltage is considered in the machine’s winding design to keep the test condition well below voltage limitation or field-weakening. By means of the flexible inverter both the current amplitude and angle can be controlled. During the test for a certain shaft torque, which is dictated by the load machine, and at a certain speed the test machine is controlled to minimize the current. After the thermal steady-state is reached the major mechanical and electrical parameters are read from the instruments. The measurement results are summarized in Table 5.1.

The most important conclusions regarding the measurements and IM, IPM and SynRM capabilities are summarized in the following sub-sections. Comparison between IM and SynRM with respect to the PF and efficiency is preferred to be done at the same output power test instead of the same temperature rise test as the magnetic loading of both machines will be very close to each other if the stator currents of both machines are almost the same. The absence of the rotor cage and consequently rotor joule losses is the main reason for better efficiency and torque capability (at the same temperature) of the SynRM in comparison to the IM [3] and [22].

5.4.1 Torque capability

The torque capability of SynRM closely follows the IM, refer to the last row \( (T/I) \) in Table 5.1. This is almost independent of load and speed [2], [3], [5], [7], [12], [13], [14] [16], [19], [20], [21], [23], [24], [25], [26], [32], [36], [39], [45], [46], [53] and [57]. The IPM shows around 15% higher \( (T/I) \) in comparison to the IM and SynRM, due to the permanent magnet torque [14], [21].

5.4.2 Efficiency

The efficiency improvement for SynRM in comparison to IM was estimated based on the machine power in Figure 2.11. For power range \( 7−35 \) (\( kW \)) the efficiency improvement is 3.2–2.5%-units, respectively [16] and [22]. The measured efficiency for IM and SynRM in Table 5.1 shows almost the same improvements in efficiency by 3.5%-units, refer to Table 5.1 - columns (1,2), (4,5), (7,8), (11,12), (15,16).

As was discussed in section 2.2.2 on page 37, the main reason for efficiency improvement is the absence of the rotor cage in case of the SynRM and IPM machines in comparison to the IM. In case of the IPM it is a little more due
<table>
<thead>
<tr>
<th>Machine Type</th>
<th>IM</th>
<th>SynRM</th>
<th>IPM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operation Type</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
</tr>
<tr>
<td>Perform. Evalu. Type</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>156</td>
<td>154</td>
<td>152</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>16.4</td>
<td>17.1</td>
<td>17.4</td>
</tr>
<tr>
<td>No. of cond. / slot</td>
<td>9</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>83</td>
<td>44</td>
<td>46</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>51</td>
<td>99</td>
<td>99</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>1990</td>
<td>1610</td>
<td>1623</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>1184</td>
<td>1491</td>
<td>1427</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>23</td>
<td>23</td>
<td>23</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>783</td>
<td>96</td>
<td>127</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>145.3</td>
<td>155.2</td>
<td>190.5</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>7.9</td>
<td>8.4</td>
<td>10.4</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>9.8</td>
<td>10.0</td>
<td>12.0</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>12.6</td>
<td>13.1</td>
<td>14</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>79.8</td>
<td>83.8</td>
<td>88.5</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0.78</td>
<td>0.76</td>
<td>0.87</td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0.622</td>
<td>0.64</td>
<td>0.75</td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1.61</td>
<td>1.87</td>
<td>1.35</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1, ns=1)</td>
<td>0.32</td>
<td>0.30</td>
<td>0.37</td>
</tr>
<tr>
<td>dT-rise ~ cte.</td>
<td>456789</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td>T ~ cte.</td>
<td>1523</td>
<td>1513</td>
<td>1510</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>1514</td>
<td>1516</td>
<td>1521</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>2.3</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>No. of cond. / slot</td>
<td>9</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>125</td>
<td>122</td>
<td>125</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>81</td>
<td>94</td>
<td>91</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>2471</td>
<td>1806</td>
<td>1971</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>1014</td>
<td>1340</td>
<td>1260</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>146</td>
<td>172.7</td>
<td>129.2</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>152</td>
<td>172.7</td>
<td>129.2</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>129.8</td>
<td>146</td>
<td>129.2</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>20.6</td>
<td>23.9</td>
<td>25.1</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>23.0</td>
<td>24.9</td>
<td>25.1</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>30.5</td>
<td>34.5</td>
<td>35</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>89.3</td>
<td>92.7</td>
<td>93.3</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0.74</td>
<td>0.71</td>
<td>0.87</td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0.661</td>
<td>0.658</td>
<td>0.79</td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1.51</td>
<td>1.52</td>
<td>1.26</td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>102</td>
<td>102</td>
<td>102</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1, ns=1)</td>
<td>0.31</td>
<td>0.30</td>
<td>0.35</td>
</tr>
<tr>
<td>dT-rise ~ cte.</td>
<td>111213</td>
<td>14</td>
<td></td>
</tr>
<tr>
<td>T ~ cte.</td>
<td>2503</td>
<td>2554</td>
<td>2505</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>2512</td>
<td>2538</td>
<td></td>
</tr>
<tr>
<td>Slip [%]</td>
<td>1.098</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>No. of cond. / slot</td>
<td>9</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>177</td>
<td>189</td>
<td>165</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>63</td>
<td>67</td>
<td>71</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>1480</td>
<td>2389</td>
<td>1539</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>527</td>
<td>648</td>
<td>710</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>123</td>
<td>123</td>
<td>125</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>133</td>
<td>133</td>
<td>133</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>114.9</td>
<td>117.1</td>
<td>119.5</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>30.1</td>
<td>35.2</td>
<td>41.8</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>36.1</td>
<td>41.8</td>
<td>50.1</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>45.9</td>
<td>51.5</td>
<td>51</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>91.5</td>
<td>94.6</td>
<td>95.3</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0.717</td>
<td>0.734</td>
<td>0.867</td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0.656</td>
<td>0.69</td>
<td>0.888</td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1.33</td>
<td>1.45</td>
<td>1.22</td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>1</td>
<td>1</td>
<td></td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1, ns=1)</td>
<td>0.28</td>
<td>0.28</td>
<td>0.28</td>
</tr>
</tbody>
</table>

Table 5.1: Heat-run test measurements on SynRM [22], IM and IPM machines, 15kW machine, see Tables 1, 2 and 3 on page 229 as well.
5.4. HEAT-RUN TEST ON SYNRM, IM AND IPM

Figure 5.4: (left) IM’s rotor and stator end-windings, (right) Rotor and iron sheets of SynRM, initial design machine.

to higher torque capability of this machine because of the magnets. Table 5.1 - columns (2,3), (5,6), (8,9), (12,13) show that the IPM has around 1%-units better efficiency than the SynRM due to higher torque capability for a certain current. This issue will be discussed later in chapter 3 on page 55.

5.4.3 Temperature rise

The magnetic and thermal limits of the SynRM have been discussed briefly in section 2.3.3 on page 44. An optimal electrical machine design and performance issue is a compromise between two completely different limitations and facts, magnetic limits and machine thermal behavior and limits, for good examples see [21] and [58]. In case of the SynRM investigated here, the study shows that the machine can be magnetically loaded up to 2 to 3 times the nominal (thermal) current. Therefore, the magnetic limitation is, at least by a factor 2, bigger than the thermal limitation. This gives us a guideline to how the power capability of the machine can be increased.

As shown in Table 5.1, the torque capability of SynRM is around 7 – 15% larger than the correspondent IM for the same temperature rise depending on the speed and load mainly due to the absence of rotor copper losses, refer to Table 5.1 - columns (1,2), (7,8), (11,12). Two main factors push the machine thermal limitation and characteristic: reducing machine losses and increasing the capability of the machine structure to transfer these losses to the surroundings. The SynRM solution is a good example of the case where the machine losses are reduced.

A cooler SynRM for a certain torque is evident from measurements presented in Table 5.1 - columns (4,5), (15,16). A 5–10 K temperature reduction for the SynRM can be expected in this example. In this sense the SynRM experiences on overall
lower temperature particularly in the winding, rotor and bearings. Temperatures in these regions specifically affect reliability aspects and lifetime of the machine.

In case of the IPM machine, the torque capability is around 15 – 25% and 30 – 40% higher than the SynRM and the IM, see Table 5.1 - columns (2,3), (8,9), (12,13) and - columns (1,3), (7,9), (11,13), respectively. The winding temperature rise of this machine is also around 15 K and 20 – 50 K lower than the SynRM and the IM, see Table 5.1 - columns (5,6) and - columns (4,6), (7,10), (11,14), respectively.

The improved temperature in IPM and SynRM in comparison to IM suggests a rule of thumb that eliminating the cage reduces the machine temperature rise by roughly 10 K and adding the permanent magnet reduces it further by 10 – 40 K. On the other hand, eliminating the cage increases the torque capability by roughly 10% and adding the magnet increases it by a further 20%.

5.4.4 Lifetime

The lifetime of an electrical machine is determined by the lifetime of its components. The most critical failures in electrical machines are bearing, around 70 – 80%, and winding’s insulation system, around 10 – 20%, failures, respectively. The bearing failure causes are due to: lubrication, around 40%, mounting, around 30%, fatigue, around 10% and others, around 20% [59]. Both bearing (lubrication) and insulation’s lifetime are strongly affected by their operating temperature. As a rule of thumb, the bearing lifetime will be halved by increasing its operating temperature by 10 – 15 K. Similarly, the winding lifetime will be reduced by half, as a rule of thumb, if the winding temperature increases by 8 – 10 K. Therefore, temperature plays a critical role in determining the lifetime of electrical machines.

In SynRM the absence of the rotor cage affects the bearing and winding temperatures. Both stator and rotor will be cooler and consequently both bearing and windings will have a lower temperature than the IM. This is true for IPM in comparison to IM, as well. In conclusion, using a SynRM or IPM instead of an IM rotor can reduce the machine temperature rise by 5 – 10 K and 20 – 50 K, respectively. This can lead to an increase of the SynRM and IPM machine’s lifetime by a factor 2 - 3 or 3 - 5 times in comparison to the lifetime of the IM.

5.4.5 Power

The improvement in torque capability and temperature rise can directly affect the power of the machine and efficiency, see Table 5.1 and [21]. In case of the SynRM increasing the torque creates an opportunity to reduce the machine size for the same power (high output, HO), in some cases even up to one size, or improve the efficiency (high efficiency, HE ) for the same torque, in some cases up to 3,5%-units (middle power sizes). Implementing the magnets as in the IPM machine in comparison to the SynRM can reduce the size even further (HO), at least by one , or increase the efficiency by up to 1%-unit (HE).
5.4.6 Power factor

The poor power factor of the SynRM in comparison to the IM, see Table 5.1, is due to the nature of this machine and the existence of the q-axis flux and cross-coupling between the d-axis and the q-axis flux [3] and [19]. In case of the IPM machine, due to the magnet, machine power factor is improved, because the magnet flux reduces the negative effect of the q-axis flux and cross-coupling. The power factor improvement is directly dependent on the amount of magnet that is used in the machine. The IPM machine here is a balance compensated SynRM. In such a condition the machine power factor can reach 0.85 – 0.9 at MTPA. This issue is discussed in detail in chapter 3 on page 55.

The poor power factor implies a higher kVA rating of the SynRM and potentially a bigger inverter. However, the network power factor of the whole drive is not affected by the machine power factor but by the drive load and it is independent of the machine type. This is due to positive effect of the DC-link capacitor and the drive rectifier performance [142]. Of course, the power factor is not the only parameter that affects the inverter size. A better factor could be \((\eta \cdot PF)\) for inverter sizing or equivalently \(1/(\eta \cdot PF)\). The operation condition and control strategy has a great impact on inverter rating, as well (see Figure 2.6, Figure 2.7 and [1], [3], [9] and [16]). The factor \(1/(\eta \cdot PF)\), which is per-unit inverter current, is shown in Table 5.1 for each test. The results show that the SynRM closely follows the IM, but the per-unit current for SynRM is slightly smaller than the IM. The IPM machine has the best per-unit current as can be expected.

5.4.7 Speed and load effect

The effect of speed and load can be considered as the effect of varying power on the machine performance. For example the lower the power the better is the SynRM’s efficiency in comparison to the IM, see Figure 2.11. The test results are compatible with the results reported in [19], especially in case of Table 5.1 - columns (4,5) and (7,8), but the variation of the performance parameters is different.

This issue is interlinked with the power range of the machine. The nominal power is \(\leq 4\ kW\) in [19], whereas in this thesis the power range is from 8 kW to 35 kW. A brief estimation of the efficiency improvement when SynRM is used instead of the IM versus the nominal power of the machines under comparison was reported in section 2.2.2 on page 37 and [16] and [22].
Part II

SynRM Design with Multi-Barrier Structure
Chapter 6

Torque and Power Factor Optimization

The synchronous reluctance machine (SynRM) performance has been studied in the earlier chapters and [1], [2], [3], [31], [57]. The main behavior and characteristics of an anisotropic structure, suitable for high performance SynRM rotor geometry design, is distinguished and discussed in the following. This issue is based on the combination of the already existing concepts [1], [2], [3], [5], [31], [32], [35], [45], [51], [53], [54], [57], [60] and [61] and utilizes a previous advanced conceptual theory for anisotropic structure modeling [54] that analytically explains the SynRM rotor anisotropic structure behavior [2], [3], [57], [32], [35], [54], [45], [53].

This is a crucial issue, because the SynRM rotor has a complex structure and a lot of geometrical parameters are involved in the machine dimensioning and optimization, see analysis of the one barrier machine in chapter 4 on page 83. The previous attempts to tackle the complex nature of the problem can be found in Vagati et al., Lipo et al. and Boldea et al. ’s works [3], [57], Kamper et al. ’s work [35], [51], Vagati/Fratta et al. ’s works [45], [54], Lovelace et al. ’s work [60] and Talebi/Toliyat et al. ’s works [61].

In Vagati/Fratta et al. ’s approach, an analytical method based on a lumped equivalent magnetic circuit of the machine was used without considering the saturation effect. The method came up with an optimal distribution of insulation material inside the rotor [45], [54].

This method is developed further by implementing a detailed lumped equivalent circuit, including saturation in the simulation by using saturable relative permeability, and varying the rotor geometrical parameters freely, by Lovelace et al. [60] and Talebi/Toliyat et al. [61]. This method is not accurate enough, because Finite Element Method (FEM) is used partially to correct the optimization. Due to variation of all geometrical parameters in this method, a large number of designs have to be analyzed and evaluated, see chapter 4 on page 83 too.

Boldea et al. does not generally suggest an optimization procedure. He fo-
CHAPTER 6. TORQUE AND POWER FACTOR OPTIMIZATION

\[ \text{total iron in } q\text{-axis } = l_y = S_1 + S_2 \]
\[ \text{total air in } q\text{-axis } = l_a = W_1 + W_2 \]

\[ k_{Wq} \triangleq \frac{l_a}{l_y} = \frac{W_1 + W_2}{S_1 + S_2} \]
\[ k_{Wd} \triangleq \frac{l_{ad}}{l_y} = \frac{W_1 + W_2}{l_d - (W_1 + W_2)} \]

Figure 6.1: Proposed rotor geometry (with-cut-off rotor structure, see Figure 6.5) and related microscopic and macroscopic parameters definition for SynRM with two interior barriers.

cusses much more on airgap flux density analysis [3], [5] and SynRM performance. He mainly follows Kostko et al.’s method [2]. Similar method to his work is used by Lovelace et al. in modeling of the saturable relative permeability and by Talebi/Toliyat et al. in the use of an effective airgap in the presence of saturation.

Kamper et al. on the other hand, does not follow the direct analytical approach for SynRM design. He uses a FEM parameter sensitivity analysis on a SynRM to investigate the effect of geometrical parameters on machine inductances [35], [51]. However, his optimization procedure is based on variation of the geometrical parameters very similar to Lovelace et al. and analyzing each machine with FEM [35], [51].

Matsuo/Lipo et al. [3], [53] and Staton/Miller et al. [3], [32] introduce a macroscopic parameter, insulation ratio in the rotor, for optimizing mainly the Axially Laminated Anisotropy (ALA) rotor structure. Their approach does not deal with the Transversally Laminated Anisotropy (TLA) rotor in general [3]. A similar analysis has been given somehow by Vagati et al. [3], [54], [57], when he discusses the total amount of air in the rotor.

In this chapter, the carefully selected general rotor shape, Figure 6.1, and some optimum distribution rules from analytical anisotropy theory [54] are used to develop a novel FEM-aided fast rotor design optimization procedure for SynRM. The
significance of this method is that it combines the analytical and FEM method for fast SynRM rotor optimization. The theoretical analysis gives an optimal insulation distribution rule in the rotor [54]. An optimal iron distribution rule is added here. These two rules are combined with a simple, general, macroscopic parameter, the insulation ratio [3], [32], [53] with an expanded definition covering both the q- and d-axis, and a microscopic parameterized rotor arrangement, see Figure 6.1. The final geometry is analyzed with FEM.

Consequently, the optimization does not directly deal with the rotor microscopic parameters. Conversely, the macroscopic parameters are used for optimization in the approach followed here. The resultant rotor structure for each set of insulation ratios in the q- and d-axis is simulated by FEM to consider all non-linearities in order to find the best machine. Afterwards, the pure geometrical parameter variation, similar to Kamper et al. 's work [51], is used to verify the accuracy and validity of the procedure by simulating around 50 SynRM designs by FEM. The present study shows that by implementing this method the total number of geometries that must be modeled (FEM) is around 10 and independent of the stator and rotor shapes.

This optimization method will be used to optimize rotor geometries with different barrier numbers in order to investigate the optimum number of barriers that gives the best rotor structure with minimum complexity. Some key issues, such as influence of electrical parameters (current loading and current angle), in the optimization procedure, have not been addressed yet by suitable FEM sensitivity analysis. This issue is discussed through some electro-magnetic rules in electrical machines, especially in SynRM [16], and in [70]. Another goal in this chapter is to investigate the influence of the electrical parameters on the final shape of the optimized SynRM’s rotor geometry, using 3kW M machine as the base machine.

6.1 Objective facts: the nature of the problem

The SynRM rotor complexity naturally increases the optimization steps and time. In particular, it is essential to consider the saturation effect [35]. A representation of what is happening, when the mathematical method based optimization is directly dealing with the rotor geometry in the presence of saturation, is reported in [35], [51], [60], [61]. It is clear that by using FEM, it is possible to overcome the non-linearity nature of the problem, especially saturation, but mathematical optimization can be avoided. For this purpose, an analytical behavior explanation is necessary.

Fortunately, for the first time, in 1923 a theoretical analysis of a possible anisotropic rotor structure and its behavior was presented by Kostko et al. [2] and his work was further developed by Vagati/Fratta et al. [3], [5], [45], [54], [57]. The optimization method evolution is based on the key facts that are described mainly in [3], [32], [53], [54], [57], especially optimization in general term that has been investigated in [3], [57] has made it possible to come up with the final design method. This method is based on a proposed rotor geometry, shown in Figure 6.1,
and optimization using sensitivity analysis (FEM) of some macroscopic parameters.

6.1.1 Suitable rotor arrangement and parameterization in microscopic term

The first step is a suitable rotor geometry selection for SynRM. As Kostko et al. has shown [2], the conventional salient pole reluctance machine is not the optimum choice for the SynRM rotor arrangement, see chapter 4 on page 83 and Figure 4.1. Instead, a SynRM with interior flux barriers has to be developed [2], [3], [32], [51], [53], [54], [57], [61].

In order not to deal with the rotor geometry parameters directly, the next crucial issue is to find a simple, flexible and general rotor barrier shape. The findings in chapter 4 show that the multiple flux barrier rotor can be microscopically parameterized as is demonstrated in Figure 6.1. The key fact in the barrier and segment shape is that the rotor geometry has to satisfy the following rule as closely as possible: the rotor structure has to fully block the q-axis flux while minimally affecting the d-axis flux.

6.1.2 Parameterization in macroscopic term

Using a rational and general rotor barrier shape will reduce the number of geometric parameters. However, they are still microscopic as they are related to the barrier’s and segment’s dimensions. It is difficult to characterize the rotor anisotropic structure behavior with these microscopic parameters.

Independent studies have tried to deal with macroscopic parameters [3], [32], [53], [54], [57]. A macroscopic geometry parameter of the SynRM is theoretically and analytically connected to the machine inductances in [3], [32], [53], [54], [57]. Vagati et al. [3], [54], [57] emphasize on the total amount of insulation along the q-axis and inside the rotor ($l_a$, see Figure 6.1). On the other hand, Matsuo/Lipo et al. [3], [53] and Staton/Miller et al. [3], [32] introduce the insulation ratio ($k_w = l_a/l_y$) that is the ratio of the total amount of air over total amount of iron material inside the rotor, see Figure 6.1. Both these macroscopic parameters ($l_a$ and $k_w$) are basically two sides of the same coin. The machine anisotropic structure can be characterized by these two parameters through analysis of their effect on $(L_d - L_q)$ and $\xi$.

6.1.3 Optimization strategy and methodology

The next most important issues are: How should this insulation be introduced in the rotor? What is the best distribution of the insulation? Vagati et al.’s derivations [3], [54], [57] show that each barrier width $W_{1_k}$ must follow a distribution rule in order to optimally utilize the insulation, $l_a$. Consequently the q-axis flux is minimized as much as possible. This rule is expressed according to Equation (6.1).
Figure 6.2: (top) Per-unit MMF distribution of the q-axis MMF over the segments, and (bottom) per-unit MMF distribution of the d-axis MMF over the segments for the geometry in Figure 6.1, (with-cut-off rotor structure).
In Equation (6.1), subscripts $k$ and $h$ are the barriers numbers, $\Delta f_k$ is the difference in the average per-unit MMF, $\sin(p\alpha)$, over barrier $k$ where $p$ is pole pair number. If a q-axis magneto-motive force $f_q$ is applied to the machine, $\Delta f_k = f_{q{k+1}} - f_{q_k}$, for details refer to [3], [5], [54] and [57] and see Figure 6.2 (top). The barrier length $S_{b1}$ for barrier one is shown in Figure 6.1.

$$\frac{W_{1k}}{W_{1h}} = \frac{\Delta f_k}{\Delta f_h} \sqrt{\frac{S_{bk}}{S_{bh}}}$$  \hspace{1cm} (6.1)

For positioning the barriers end points in the airgap two assumptions are considered for constant rotor slot pitch, which is defined as the distance between two barriers openings, and the distance between the last barrier opening and an imaginary extra point on the last barrier ($W_{1k}$), point (B) in Figure 6.1 and Figure 6.2. To have an extra degree of freedom for the end point positions of each barriers ($k$), point (B) angle from the q-axis ($\beta$) can be considered as another design parameter. By this parameter the end points can be adjusted for a certain number of barriers, for example to control the torque ripple, this issue will be discussed later in chapter 7 on page 125.

Above issues suggest that the end point angles should be as shown in Figure 6.2. Now, it is straightforward to express rotor mechanical slot pitch angle ($\alpha_m$) as a function of barriers number ($k$), pole pair ($p$), and variable angle ($\beta$) according to Equation (6.2), for with-cut-off rotor structure.

$$\alpha_m = \frac{\pi}{2p} - \frac{\beta}{k + \frac{1}{2}}$$  \hspace{1cm} (6.2)

Implementing Equation (6.1) can be combined with an assumption on the barrier’s permeance $p_k = S_{bk}/W_{1k}$ [3], [5], [54], [57]. Assuming constant and equal permeance ($p_k = p_h$), which implies a rotor with homogeneous anisotropic structure gives using Equation (6.1) the following:

$$Assumption : \frac{p_k}{p_h} = cte. = 1 \quad \Rightarrow \quad \frac{W_{1k}}{W_{1h}} = \left(\frac{\Delta f_k}{\Delta f_h}\right)^2$$  \hspace{1cm} (6.3)

Similarly there must be a concept to utilize the total iron ($l_y$, see Figure 6.1). Generally increasing air reduces $L_q$ effectively, and increasing iron increases $L_d$ significantly. This means that the segment optimal distribution rule has to be strongly interconnected to the d-axis flux maximization. A straightforward assumption for segment size $S_k$ is that it should be proportional to the average d-axis MMF, which that segment is facing in the airgap. This is formulated according to Equation (6.4).

$$\frac{S_k}{S_h} = \frac{fd_k}{fd_h}$$  \hspace{1cm} (6.4)
In Equation (6.4), subscripts \( k \) and \( h \) are the segments number, \( f d_k \) is the average per unit MMF, \( \cos(p\alpha) \), over segment \( k \), if a d-axis MMF \( fd \) is applied to the machine. Using Equation (6.4), it is almost ensured that the flux densities in all segments are the same and iron utilization in the rotor will be increased, see Figure 6.1 and Figure 6.2 (bottom).

6.2 Optimization of multiple flux barriers geometry

Using the simple theory, which is summarized in Equations (6.3) and (6.4), the geometrical microscopic parameters in Figure 6.1 can be replaced by general and significant macroscopic parameters such as insulation ratios and barrier number. Assume that the stator geometry is fixed, i.e. \( l_a + l_y = \text{cte.} \), then for each set of pole pair number, barrier number, insulation ratio in the q-axis and in the d-axis, the rotor microscopic dimensions can be derived using Equations (6.3) and (6.4). For simplicity saturation, stator slotting, iron potential drop, imperfect stator winding and MMF distribution effects are disregarded without any loss in the theory’s generality [2], [3], [57]. Instead, the resultant geometry can be modeled by FEM.

6.2.1 Insulation ratio in q-axis

Consider that \( k_{wq} \), which is defined in Figure 6.1, is known for a specific stator \( (l_a + l_y \text{ is known}) \), then \( l_a \) and \( l_y \) are known. Therefore, by using Equations (6.3) and (6.4) each barrier and each segment width in the q-axis can be calculated respectively. On the other hand in a 4-pole machine if it is assumed that the total amount of iron in the q-axis and d-axis are the same, it is shown that always \( k_{wd} \leq k_{wq} \) or \( W_kd \leq W1k \) for \( p > 1 \), see item 7 on page 91 from chapter 4 and [22], especially when \( l_d \approx l_a + l_y \), see Figure 6.1 for their definition, which is the case here, where \( l_d \) is the width of a specific path that is crossed by more than 90% of the d-axis flux. \( k_{wd} \) is defined over this path as is shown in Figure 6.1.

Now by simple assumption Equation (6.5) and a suitable value for \( k_{wd} \), each barrier width in the d-axis can be calculated.

\[
\left( \frac{W_kd}{W_hd} \right)_{d-axis} = \left( \frac{W1k}{W1h} \right)_{q-axis}
\]  

(6.5)

Here for each \( k_{wq} \) it is assumed that \( k_{wd} = 1/2 \times k_{wq} \). The effect of the q-axis insulation ratio on the torque is modeled using FEM and the result is shown in Figure 6.3 (left). The optimum insulation ratio in the q-axis for maximum torque is around 0.6 – 0.7 which is equivalent to a value of \( l_a \) around 21 mm. The largest torque is around 32 Nm. Analyses of the results in Figure 6.3 (left) shows the behavior of the machine torque as a function of \( k_w \) is the same as reported in [53], as well:

- Insulation ratio from 0.1 to 0.4: In this region mainly air is introduced. This causes a high reduction in \( L_q \) compared to a complete solid rotor, while \( L_d \) does
not reduce as fast as $L_q$. Actually by increasing $k_{wq}$ in this region an anisotropic structure will be formed inside the rotor. The rate of torque increase is high because $L_d$ is actually greater than the optimum point value and $L_q$ is reducing very fast toward the optimum value for maximum torque.

- Insulation ratio from 0,4 to 1,2: Torque is almost constant in this region. $L_q$ has almost reached its minimum at $k_{wq} \approx 1,2$ and continues to reduce slowly. Also $L_d$ reduces due to the removal of more iron from the rotor.

- Insulation ratio greater than 1,2: In this region increasing the air and reducing the iron do not have a significant effect on $L_q$ but it decreases $L_d$, thus torque is reduced.

It is clear from the results that torque is highly dependent on $k_{wq}$.

### 6.2.2 Insulation ratio in d-axis

If $k_{wq}$ is set to its optimum ($k_{wq} = 0,6$), then the effect of $k_{wd}$ on torque will be as shown in Figure 6.3 (right). Torque behavior is seen to be practically independent of $k_{wq}$. Notice that $k_{wd} \leq k_{wq}$ at the optimal point.

### 6.2.3 Proposed method evaluation and validity

The analysis in the last sections ended with a geometry with $k_{wq} \approx 0,6$ and $k_{wd} \approx 0,2$. This gives $W_{11} \approx 15 \ mm$ and $W_{12} \approx 5,75 \ mm$. To evaluate and confirm the optimum geometry and its related torque around 32 $Nm$, the barriers width in the q-axis and proportional to it in the d-axis are independently varied around the optimal point and over a wide range. As in [35] $Y_{q1}$ is kept constant and equal to its optimal value for different geometries. The resultant torque is shown in Figure 6.4 (left) and (top-right). The blue area shows maximum torque for different combinations of two barriers width.

With reference to Figure 6.1 and the definition of $k_w$, related $k_{wq}$ and $k_{wd}$ are around 0,7 – 0,8 and 0,2 – 0,3 respectively and optimal torque is around 32 $Nm$. 

---

Figure 6.3: (left) Torque calculated using FEM for different $k_{wq}$ when $k_{wd} = 1/2 \times k_{wq}$, (right) Torque calculated using FEM for different $k_{wd}$ when $k_{wq} = 0,6$. 

---
These values are compatible, with reasonable tolerance, to the values which were calculated from combined analytical and FEM fast analysis in the last sections. Torque behavior in Figure 6.4 is comparable with $k_{wq}$ effect on torque. Consequently, Figure 6.3 (left) shows a general effect of increasing air in the rotor.

Figure 6.4 clearly shows that the optimum rotor geometry for maximum torque is not unique, but the optimum torque is the same for both pure geometrical and analytical aided FEM analysis. Thus, the analytical aided FEM method can be used as a fast procedure to find the most important parameter values which are insulation ratios without dealing directly with the rotor microscopic dimensions.

### 6.3 Number of barriers sensitivity analysis

Positioning of barriers in the rotor has two main patterns as Figure 6.5 shows. In type (a) the part of the rotor along the q-axis nearest to the airgap, is iron, but in type (b) this layer is air. The main procedures for analyzing both of them are quite similar but with some differences. The with-cut-off rotor structure has been treated in this chapter as shown in Figure 6.1. The without-cut-off rotor structure will be studied later.

An extra angle $\beta$ is defined to introduce another degree of freedom for barriers end points in the airgap, see Figure 6.1 [16], [40]. This angle is kept constant in this part of the analysis, because the main goal here is to investigate the effect of rotor barrier number on torque, and not torque ripple. It will be shown in chapter 7 on page 125 that some kind of independent torque and torque ripple optimization is possible by introducing this angle.

Therefore, for normal rotor structure and with-cut-off rotor structure this angle

---

**Figure 6.4:** Effect of barriers width on torque (FEM) for different combination of barrier and cut-off width for rotor geometry shown in Figure 6.1, $Y_{q1} = \text{c.t.e.}$
Figure 6.5: Two main rotor barriers positioning; (a) 2 barriers without-cut-off rotor structure, see Figure 7.1, (b) 2 barriers with-cut-off rotor structure, see Figure 6.1 too, (1 barrier + 1 cut-off rotor structure).

is kept constant. Thus, the rotor slot pitch, by considering two imaginary point in the q-axis for without-cut-off rotor structure and one point for the with-cut-off rotor structure, see Figure 7.1 when $2\beta = \alpha_m$, and Figure 6.1 when $\beta = 0$ respectively, becomes constant all around the rotor circumference.

### 6.3.1 Number of barriers effect

The optimum point for each number of barriers is calculated, based on finding the optimum insulation ratio in the q-axis when $k_{wd} = 1/2 \times k_{wq}$. Then, at optimum q-axis insulation ratio the best d-axis insulation ratio is determined. The position and size of barriers, with each selection of insulation ratios is calculated based on the theoretical calculation in this chapter.

The resultant torque and torque ripple per pole as a function of barrier number for both arrangement models, see Figure 6.5, are shown in Figure 6.6. Also a comparison between the two models from the torque point of view is demonstrated.

Figure 6.6: Number of barriers effect on optimum torque and its related torque ripple per pole for rotor structure model: (left) without-cut-off rotor structure, (right) with-cut-off rotor structure, when constant rotor slot pitch, $2\beta = \alpha_m$ is used.
6.3. NUMBER OF BARRIERS SENSITIVITY ANALYSIS

in Figure 6.7 (left).

Actually the cut-off barrier becomes very small if the number of barriers is increased. Furthermore generally one extra barrier with the cut-off rotor structure model is equivalent to the without-cut-off rotor structure model. This is correct if rotor slot pitch is kept constant.

Increasing the number of barriers directly and effectively reduces the circulating component of q-axis flux but it does not affect the going-through component [3], [21], [45], [54], [57]. Also increasing the numbers of barriers does not affect the d-axis inductance very much. Therefore, torque will not be affected if the number of barriers is increased more than a certain value [21], [54], which from Figure 6.7 is around 3 – 5 barriers.

The barrier widths are calculated according to constant permeance for barriers and optimum distribution rule Equation (6.3) for minimizing the going-through component of the q-axis flux [3], [16].

6.3.2 Number of layers effect

Another way to compare the different structure is to calculate the machine inductions at an operating point. Results from such an analysis are presented in Figure 6.7 (right). By increasing the number of layers from 2 (salient pole machine) to 5 (2 barrier machine without-cut-off rotor structure) the q-axis inductance reaches its minimum. As it is discussed in [3], [21], [45], [54], [57], the q-axis flux has two components: The circulating component and the going-through component. For each number of barriers, using the optimum distribution rule Equation (6.3) for barrier width, guarantees a minimum of q-axis going-through flux component. Furthermore the circulating component is inversely proportional to the square of layer number and by increasing the layer number the circulating flux reduces rapidly.

Figure 6.7: Effect of number of barriers on optimum total torque for both rotor structure models. (left) Comparison are made at the best current angle. (right) Inductances in q- and d-axis, inductance difference and saliency ratio for different number of rotor layers. Number of layers is the sum of number of segments and number of barriers [32].
Increasing the number of layers also increases the rotor d-axis flux capturing capability and therefore the d-axis inductance. Machine torque is proportional to the inductances difference ($L_d - L_q$). Increasing the number of rotor layers more than 9, actually does not change this difference. Also machine power factor is strongly dependent to inductances ratio, saliency ratio ($L_d/L_q$). This also does not change for number of layer more than 9. The effect of the number of layers on machine inductances is completely compatible with the direct torque analysis and it shows that increasing the number of barrier more than 5 or number of layer more than 10 will not have any significant gain in the machine performances.

6.4 Objective facts: effect of electrical parameters on optimization

Generally, parameters in SynRM analysis and design can be divided into two types of parameters: electrical, and mechanical (or geometrical) parameters [16]. Earlier, it was assumed that the electrical parameters (current and current angle or simply electrical loading) do not affect the final shape of the rotor geometry if the optimization procedure in this chapter is followed. In this section this issue will be studied. A similar study has been done but at a fixed current angle $70^\circ$ and varying current in [70].

6.4.1 Example of optimization at one electrical operating point and effect of $k_{wq}$ on (Torque, IPF) and ($L_d - L_q$, $L_d/L_q$)

In this chapter, the insulation ratio sensitivity analysis for torque optimization, using FEM, is performed on a suitable rotor for 3kWM machine at nominal current: $I_s = 8.32A = I_n$ and $\theta = 45^\circ$. The rotor slot pitch angle controller $\beta$ is kept constant and equal to the optimal value 7°, for more information regarding this angle refer to chapter 7 on page 125. The results are summarized in Figure 6.8 (left). The IPFx is calculated by evaluating the machine current and voltage vectors in FEM.

The different effect of the insulation ratio on torque and power factor was mentioned in the previous sections briefly and it has been discussed in details from design aspects point of view in [16]. The optimal geometry with respect to the torque has $k_{wq} = 0.6$ or MTPA, on the other hand, $k_{wq} \approx 1$ gives a geometry with maximum power factor or MTPkVA, see Figure 6.8 (left). As was mentioned earlier, the main reason for this is the existence of a big change in the inductances of the machine, which can have a different effect on the torque in comparison to the power factor. The torque capability is directly a function of ($L_d - L_q$), but the power factor is sensitive to ($\xi = L_d/L_q$). The insulation ratio has a different effect on these two parameters. One value of insulation ratio maximizes ($L_d - L_q$) and another value of insulation ratio maximizes ($\xi = L_d/L_q$).
6.4. OBJECTIVE FACTS: EFFECT OF ELECTRICAL PARAMETERS ON OPTIMIZATION

A simple method was developed in chapter 2, subsection 2.3.2 on page 43 to evaluate the machine online inductances and saliency ratio by means of the machine terminal values and power factor. The same method is used to estimate the machine inductances at each point of Figure 6.8 (left). Results from such an inductance estimation are summarized in Figure 6.8 (right). As this figure shows, the q-axis inductance reduces quickly by increasing $k_{wq}$ up to 0.6 while the d-axis inductance and specially $(L_d - L_q)$ are not so sensitive to $k_{wq}$ for values up to 0.8. The torque and $(L_d - L_q)$ on one hand and the power factor and $(\xi = L_d/L_q)$ on the other hand, compare Figure 6.8 (left) and (right), respectively, demonstrate the strong claimed ties between these parameters. This inductance behavior was investigated in [32] and [53] for the first time and it is followed up and developed in detail in [16] in the optimization procedure.

This different effect of $k_{wq}$ can be used to obtain a better machine with respect to both the torque and power factor capability. This is done if instead of MTPA or MTPkVA, a rational trade-off between these two geometries is selected. The trade-off geometry has to keep the torque capability with respect to MTPA and at the same time, as much as possible, it has to be close to MTPkVA geometry. This will guarantee a geometry with maximum $(\eta \cdot PF)$ instead of maximum $(\eta)$ or maximum $(PF)$. Such an optimum geometry is obtained for $3kW$ machine, if, here, $k_{wq} = 0.8$ is chosen, see Figure 6.8.

6.4.2 Electrical parameters sensitivity analysis

The effect of electrical parameters on the optimization is briefly discussed in [16], see chapter 2 on page 21 for the SynRM’s model and for the definition of the electrical parameters (current and current angle $\theta$). There, it is claimed that: If a certain current with certain current angle is applied to two machines with different rotor geometry (i.e. here different $k_{wq}$) and the torque for one of them is higher than the other one then for all currents and current angles it will produce higher torque than the other machine [16].

![Figure 6.8](image_url)
CHAPTER 6. TORQUE AND POWER FACTOR OPTIMIZATION

Figure 6.9: (top) Machine torque for different current angle \( \theta \) and \( k_{wq} \) as parameter, (bottom) machine torque for different \( k_{wq} \) and current angle \( \theta \) as parameter, when \( I_s = 8.32 \text{ A} = 100\% \cdot I_n \), (3kWM machine).
Figure 6.10: (top) Machine power factor for different current angle $\theta$ and $k_{wq}$ as parameter, (bottom) machine power factor for different $k_{wq}$ and current angle $\theta$ as parameter, when $I_s = 8.32\,A = 100\% \cdot I_n$, (3kW M machine).
Figure 6.11: (top) Machine torque for different current angle \( \theta \) and \( k_{wq} \) as parameter, (bottom) machine torque for different \( k_{wq} \) and current angle \( \theta \) as parameter, when \( I_s = 6.24A = 75\% \cdot I_n \), (3kWM machine).
6.4. OBJECTIVE FACTS: EFFECT OF ELECTRICAL PARAMETERS ON OPTIMIZATION

Figure 6.12: (top) Machine power factor for different current angle \( \theta \) and \( k_{\text{wq}} \) as parameter, (bottom) machine power factor for different \( k_{\text{wq}} \) and current angle \( \theta \) as parameter, when \( I_s = 6.24 \text{A} = 75\% \cdot I_n \), (3kW \( \text{M} \)) machine.
To examine this claim an electrical parameters sensitivity analysis has been performed for a specific 3kWM machine stator, the same as in the last section. For this purpose, two current values (100% and 75% of the thermal nominal value \(I_{sn}^{SynRM} = 8,32 A = 100\% \cdot I_n = 1,3 \times I_{sn}^{IM}\)) are chosen, eight different machines within 0,04 < \(k_{wq}\) < 1,9 are selected. Their torque and internal power factor, for \(0^\circ < \theta < 90^\circ\) (at 14 different angles) are evaluated by FEM time-stepping (100 steps per period). Totally, \((14 \times 14) \times 8 \times 2 \times 8 = 224\) time-stepping calculations are performed.

The simulation results are summarized in Figure 6.9 - Figure 6.12. As can be seen in Figure 6.9 (top) and Figure 6.11 (top) the machine with \(k_{wq} = 0,6\) has the best torque for both currents and for all current angles up to 73°. This is exactly equal to the value obtained from the analysis in the last sub section, see Figure 6.8 (left). If the current angle goes above 73° then it seems that the optimal \(k_{wq}\) is shifted to lower values around 0,25, but the torque capability with respect to the optimal value \((k_{wq} = 0,6)\) is not so much bigger, refer to Figure 6.9 (top) and Figure 6.11 (top). Similarly, the machine with \(k_{wq} \approx 1\) (the same as in Figure 6.8 (left)) has the best power factor for both currents and for all current angles up to 73°, see Figure 6.10 (top) and Figure 6.12 (top). For both cases, comparison between graphs in Figure 6.9 (bottom) - Figure 6.12 (bottom) and Figure 6.8 (left), shows high compatibility between the results, when \(\theta \leq 73^\circ\), see similar results in [70].
Three important issues become more significant at high current angles, $\theta > 73^\circ$. Firstly, the torque ripple (in %) starts to increase because the absolute torque harmonic (peak-peak) value remains constant and almost independent of the average torque as can be deducted from Figure 6.13. Secondly, at constant stator current, as the current angle is increased the d-axis flux weakens strongly due to the negative effect of the q-axis flux on the d-axis flux. Thirdly, the driving MMF in the d-axis becomes comparable with the MMF drop over the iron material in the d-axis path.

A larger relative harmonic contribution in the airgap flux density affects the fundamental airgap flux, both in amplitude and phase, and this is directly reflected in the strange power factor behavior of the machine, see IPFx in Figure 6.10 and Figure 6.12 for $\theta > 73^\circ$. This could create instability problems at high current angles, especially in field-weakening. The cross-coupling and iron drop at high current angles can be compensated by increasing the iron material in the rotor. This means that a lower $k_{wq}$ has to be chosen, if $\theta > 73^\circ$. This issue requires more study which is out of the scope of this thesis.
Chapter 7

Torque Ripple Optimization

Torque ripple and other secondary effects such as rotor iron losses, vibration and noise, are definitely important issues in Synchronous Reluctance Machine (SynRM) design [3], [4], [16], [37], [40] - [44], [46], [48], [52], [54], [55], [57] and [63] - [69], similar to the Induction Machine (IM), [33] and [58]. An analytical study of torque ripple and its main root-cause is addressed in [3], [16], [37], [40] - [43], [48], [54], [55], [57], [64] - [67] and [69]. A FEM investigation on torque ripple is presented in [4], [16], [40] - [43], [52], [65] and [67]. Some methods proposed by different authors to reduce the side effect of the SynRM torque ripple can be found in [3], [4], [16], [37], [40] - [43], [48], [52], [55], [57], [63] - [65], [68] and [69]. These studies show the complexity of the problem, specially if the iron losses are considered, [16], [46], [66] and [67]. For example [67] describes a possible trade-off between torque ripple and iron losses which has to be made while designing SynRM. An important conclusion is that there is a strong coupling between the stator and rotor structures. Interaction between the stator, specially the stator slots, and the rotor slots and its magnetic reaction to the stator MMF, plays an important role in the torque quality and ripple developed by the machine [3], [37], [40] - [43], [54], [55], [57], [63], [65], [67], [68] - [69].

Torque ripple minimization/optimization of SynRM is discussed in this chapter. A method for ripple reduction in SynRM suitable for and compatible to the torque maximization procedure, that is discussed in chapter 6 on page 105, is introduced in this chapter [16] and [137]. The decoupling between stator and rotor structure during the torque ripple minimization is the main goal. This can be achieved e.g. by the development of a general method that minimizes the ripple independent of the stator structure, specially the slots number of the stator, and the number of barriers in the rotor or rotor slots number. Furthermore, it will be shown that torque maximization and torque ripple minimization are independently possible in the SynRM design.
7.1 Optimization strategy and methodology

The torque ripple and torque quality is an important macroscopic characteristic of the SynRM. Like any other electrical machine, SynRM rotor slot pitch, see \( \alpha_m \) in Figure 7.1, has a big influence on the ripple. The second most important factors are the constant rotor slot pitch [3], [16], [40], [43], [57] and a rotor with a homogeneous anisotropic structure, see Equation (6.3) on page 110, [3] and [57]. The constant rotor slot pitch is a general accepted rule for all conventional electrical machines including general purpose (GP) machines, but this is not the only way for ripple minimization [4], [16], [37], [41], [42], [48], [52], [55], [63], [68] and [69].

In SynRM the idea of using \( \alpha_m = \text{cte.} \) was for the first time used by Vagati \textit{et al.} for ripple control [54] and [3], [37], [40], [43] and [67]. Even the Kostko’s design, see Figure 2.10 (left), somehow considers the constant rotor slot pitch as well [2]. Vagati \textit{et al.}'s idea was based on the selection of a suitable number of barriers, assuming constant rotor slot pitch is used. This takes place when \( 2\beta = \alpha_m \) in Figure 7.1 and the number of the stator slots is known [43]. His claim is that the torque ripple is minimized if there is a relation between the rotor and the stator slot numbers and the rotor slot pitch is kept constant.

\[
\text{total iron in } q\text{-axis} = l_y = S_1 + S_2 + S_3
\]
\[
\text{total air in } q\text{-axis} = l_a = W_1 + W_2
\]
\[
k_{wq} = \frac{\Delta l_a}{l_y} = \frac{W_1 + W_2}{S_1 + S_2 + S_3}
\]
\[
k_{wd} = \frac{\Delta l_d}{l_y} = \frac{W_1_d + W_2_d}{l_d - (W_1_d + W_2_d)}
\]

Figure 7.1: \textit{Proposed rotor geometry (without-cut-off rotor structure, see Figure 6.5) and related microscopic and macroscopic parameters definition for SynRM with two interior barriers.}
An intelligent idea that Vagati et al. used for adapting the constant rotor slot pitch to the SynRM unsymmetrical rotor structure (especially in the q-axis region of the rotor geometry with without-cut-off rotor structure) is the definition of an imaginary rotor slot opening in each pole of the machine. This imaginary slot opening is placed in the last segment, here $S_3$ area, and one end of the opening is represented by point $B$ in Figure 7.1. According to Vagati et al. ’s idea [43], if the completely constant rotor slot pitch concept is followed, when $2\beta = \alpha_m$, then for each stator structure there will be a specific number of barriers that gives a rotor structure with minimum torque ripple [40]. A possibility that comes to mind is to keep the rotor slot pitch constant mainly in the d-axis for barriers number 1 to $k$ while $2\beta$ is allowed to vary for a certain number of barriers ($k$) [16].

Introducing this method does not change the equal rotor slot pitch rule for the major part of the rotor, except those imaginary slots that are near to the q-axis [16]. However, the interlock between the ripple and the number of the barriers according to the Vagati et al. ’s idea is eliminated and for any number of barriers and any stator structure the ripple can be minimized by performing a limited number of FEM sensitivity analysis on angle $\beta$. Using this method, the rotor slot pitch is kept constant in most part of the rotor which contributes to torque ripple reduction. On the other hand, by adjusting $\alpha_m$ with angle $\beta$ the optimal slot pitch for any number of barriers can be found for torque ripple minimization.

Another parameter that can be used to reduce the ripple further is for example adjusting the rotor slot pitch by choosing a suitable radial position of the barrier in the q-axis $Y_q$. For example, see the effect of this parameter on torque ripple in Figure 4.3 (b) when the optimal insulation ratio is determined. For torque ripple minimization other techniques are also possible, for example selecting independently different slot opening position for different barriers instead of equal rotor slot pitch in the d-axis, this kind of optimization is considered in [41], [42], [48], [55], [68] and [69] or adjusting the airgap dimension for controlling the airgap permeance [55], [64], [68] and [69] or using different pole pitch sizes in different poles [63].

### 7.2 Torque ripple minimization of multiple flux barrier geometry

The effect of angle $\beta$ on a specific SynRM machine is investigated to demonstrate the torque ripple variation due to this design parameter. As was shown earlier, a 4 barrier SynRM with without-cut-off rotor structure exhibits a high torque ripple when a constant rotor slot pitch, $2\beta = \alpha_m$, is used, see torque ripple of this machine in Figure 6.6 (left) when the number of barrier is 4. Tangential ribs (1mm) are introduced in the geometry of this machine and rotor barrier ends are rounded here.

As a first step, assume that $\beta$ is known and as a starting point the constant rotor slot pitch is suggested. Then, based on the number of barriers $k$, which is 4 here, and assumed $\beta$, the position of end points of barriers in the airgap, points $D_i$, will be as shown in Figure 7.2. For this conditions the rotor slot pitch (for
CHAPTER 7. TORQUE RIPPLE OPTIMIZATION

Figure 7.2: (top) Per-unit MMF distribution over segments in the q-axis MMF, (bottom) per-unit MMF distribution of the d-axis MMF over the segments, for the geometry in Figure 7.1, (without-cut-off rotor structure).
7.2. TORQUE RIPPLE MINIMIZATION OF MULTIPLE FLUX BARRIER GEOMETRY

Figure 7.3: (top) Simulated (FEM) torque/torque ripple (peak-to-peak (pp)) curves for a SynRM with 4-pole, 4 barriers, without-cut-off rotor structure, see Figure 7.4, as a function of $\beta$, and (bottom) corresponding torque ripple (pp) in percentage of the average torque, when $k_{wd}$ is 0.3 and $k_{wq}$ is 0.7, see Figure 7.4, and end points adjusted by $Y_q$.

without-cut-off rotor structure), $\alpha_m$, can be determined by Equation (7.1).

$$\alpha_m = \frac{\pi}{2p} - \beta$$  \hspace{1cm} (7.1)

The $\Delta f_k = f_{q_{k+1}} - f_{q_k}$ over each barrier due to the q-axis magneto motive force $f_q$, and the $f_{d_k}$ over each segment due to d-axis MMF $f_d$, can be calculated based on Figure 7.2, if the end of barriers are known. Consider that $\Delta f_k$ for each barrier and $f_{d_k}$ for each segment are calculated, then it is straightforward to calculate each barrier and segment size in the q-axis using Equations (6.3) and (6.4), respectively. The insulation ratio in the d-axis, $k_{wd}$, and the q-axis, $k_{wq}$, are
kept constant and equal to the optimum values for maximum torque. Optimal \( k_{wd} \) is 0, 3 and \( k_{wq} \) is 0, 7 for the specific machine with 4 barriers and without-cut-off rotor structure. Each barrier dimension in the d-axis also can be calculated by Equation (6.5), if barrier dimension in the q-axis and insulation ratio in the d-axis are known. Finally, after positioning the barriers and segments in the rotor, if there is a small discrepancy between the calculated and the actual end point position and the airgap, the parameter \( Y_{qk} \) for each barrier is changed until the end points of the barrier in the airgap are adjusted to the position of the calculated constant rotor slot pitch angle.

The effect of \( \beta \) on torque and torque ripple for the 4 barrier machine is shown in Figure 7.3. Actual machine structure variation due to changing \( \beta \) is also demonstrated in Figure 7.4. In Figure 7.3, the corresponding angle \( \beta \) for constant rotor
7.3. OTHER PARAMETERS AFFECTING THE OPTIMIZATION

slot pitch is 4.5° mechanical. By increasing β torque ripple (pick-to-pick) is reduced from 40%, for β around 4.5°, to 13%, for β around 8°. Angle β has a negligible effect on the average torque, see Figure 7.3 (top), because the insulation ratios are the same here for all values of β, \( k_{wq} = 0.3 \) and \( k_{wq} = 0.7 \).

The torque ripple minimization in Figure 7.3 and Figure 7.4 shows that, torque maximization and torque ripple minimization, for a certain number of barriers and stator structure, can be achieved independently. This is achieved by optimizing the angle β with equal rotor slot pitch in the d-axis. The rotor slot pitch in the d-axis is the most effective parameter for the reduction of torque ripple without significantly affecting the average torque, but this is not true for the rotor slot pitch in the q-axis, see Figure 7.4.

7.3 Other parameters affecting the optimization

7.3.1 Barriers permeance and governing rule sensitivity analysis with respect to ripple

Possibility of a rotor with homogeneous anisotropic structure is mentioned in the last section. Implementing Equation (6.1) for insulation ratio can be combined with some assumptions on the barrier’s permeance to further reduce the torque ripple, [3], [16], [54] and [57]. There are two boundary situations regarding the permeance, one constant, the rotor with homogeneous anisotropic structure, according to Equations (7.2) or (6.3), and the other inversely-proportional to the magnetic potential difference over each barrier according to Equation (7.3) [3].

Assumption : \( \frac{p_k}{p_h} = cte. = 1 \) \( \Rightarrow \) \( W_{1k} \frac{W_{1k}}{W_{1h}} = \left( \frac{\Delta f_k}{\Delta f_h} \right)^2 \) (7.2)

Assumption : \( \frac{p_k}{p_h} = \frac{\Delta f_h}{\Delta f_k} \) \( \Rightarrow \) \( W_{1k} \frac{W_{1k}}{W_{1h}} = \left( \frac{\Delta f_k}{\Delta f_h} \right) \) (7.3)

Figure 7.5: (right) Torque ripple minimization by rotor slot pitch parameter, \( \beta \), using the constant permeance Equation (7.2), and (left) permeance inversely-proportional to the magnetic potential difference over each barrier Equation (7.3).
Using Equation (7.2) effectively helps to reduce the ripple.

A FEM sensitivity analysis has been performed, using both Equations (7.2) and (7.3) for a certain insulation distribution here $k_{wq} = 0.8$ between four barriers. The effect of varying $\beta$ on machine torque and torque ripple is shown in Figure 7.5 (right) and (left), respectively. Clearly, the machine with homogeneous anisotropic structure according to Equation (7.2) gives lower ripple here ($3kW M$ machine).

### 7.3.2 Independent torque and torque ripple optimization sensitivity analysis

An advantage of the optimization method here is that the torque maximization and the torque ripple minimization are independent with respect to $k_{wq}$ and $\beta$. This is, on one hand, due to a suitable division and selecting of the the optimization parameters and, on the other hand, the different nature of the torque and torque ripple characteristics of SynRM. This issue is studied by minimizing the torque ripple of a specific machine, $3kW M$ machine, using FEM, when two different insulation ratios are used. Results are shown in Figure 7.6. The optimal values for ripple controller parameter, angle $\beta$, are evidently the same for both cases and equal to $7^\circ$.

### 7.3.3 Torque ripple and iron losses in SynRM

Parts of the SynRM with critical temperatures are discussed in chapter 12, specially inside the rotor. However, due to the absence of a cage in the rotor of the SynRM, iron losses in this region are the most important source of heat inside the rotor.

In this section the machine iron losses will be investigated to give a rough insight into one of the root-cause of the temperature gradients that the machine exhibits in the rotor and in other parts.

The close relation between iron losses and torque ripple was briefly discussed earlier. This issue is studied in [16], [46], [66], [67] and ([42] and [79]). Specially in [67] and ([42] and [79]) it is shown that minimum iron losses and minimum torque ripple in machine can not be achieved at the same time and somehow a trade-off
between low iron losses and low torque ripple has to be considered. A study on the effect of the design parameters on the machine iron losses can provide a guideline to how the machine iron losses specifically in the rotor can be reduced.

To study this issue the torque ripple optimization parameter, angle $\beta$, can be a suitable starting point. Every parameter that affects the ripple has to affect the iron losses and vice-versa. For this purpose in a specific SynRM, with 1.5 kW power size (1.5kWM machine), the effect of rotor slot pitch controller, angle $\beta$, on both ripple and iron losses are studied. This machine has just four barriers and the number of barriers is kept constant.

The effect of $\beta$ on machine torque ripple and iron losses is shown in Figure 7.7. Clearly, $\beta$ has an opposite effect on torque ripple and iron losses for the rotor with a given barrier number. For values of $\beta$ that give minimum torque ripple the iron losses are maximum, see Figure 7.7 right side of the figure, and for values of $\beta$ that give minimum iron losses the torque ripple is maximum, see Figure 7.7 left side of the figure. The iron losses increase by around 30 – 40 % for values of $\beta$ that minimize the torque ripple in comparison to $\beta$ values that give minimum iron losses, see Figure 7.7. This study obviously shows that the machine design can provide an opportunity for the reduction of iron losses.
Chapter 8

SynRM: Improved machine design, fine tuning, validation and measurements

The Synchronous Reluctance Machine (SynRM) optimization procedure for torque maximization is discussed in chapter 6 on page 105. A fast torque ripple minimization method that is compatible with the torque maximization procedure, is also described in chapter 7 on page 125. Based on these design tools, a design that is a compromise between the final machine’s performance and simplicity of the rotor structure, is studied as the improved machine design (SynRM ImprM) in this chapter. Afterward, the promising designs are studied and the best machine is fine tuned. Fine tuning includes reducing the number of barriers to a practical minimum number, rounding the sharp corners, mechanical dimensioning and adding the radial ribs. Extra goals for fine tuning are to reduce the iron losses and also to minimize the q-axis inductance ($L_q$) as much as possible without significantly disturbing the d-axis inductance ($L_d$). All machine performance parameters are also calculated in the finally tuned design. All torque and iron losses values that are mentioned in this chapter are not calibrated and for more realistic values the calibration factors most be considered. The Finite Element Method (FEM) calculated values are however suitable for relative comparison.

Finally, the fine tuned most promising design is prototyped and its performance is compared with its corresponding Induction Machine (IM), by measuring their performance through heat-run tests under variable speed supply operation. For this purpose, a specific drive that is equipped with a suitable software for SynRM machine control is used.


**Figure 8.1:** Effect of the number of barriers on optimum torque for both rotor structure models, with- and without-cut-off rotor structure, see Figure 6.5. Also compared before and after applying torque ripple minimization technique, at the best current angle (TR = Tangential Rib).

### 8.1 Best rotor

#### 8.1.1 Torque

The rotor geometry optimization for maximum torque by finding the best values for design parameters, insulation ratio in q-axis, $k_{wq}$ and in d-axis, $k_{wd}$, see geometry of with-cut-off rotor structure in Figure 6.1 and of without-cut-off rotor structure in Figure 7.1, is discussed in chapter 6 on page 105. The study is done for different number of barriers and a specific 4-pole induction machine stator, see the results in Figure 6.6 and Figure 6.7. Torque of the final optimized geometries are shown in Figure 8.1.

A large number of barriers from the mechanical and manufacturing point of view is not recommended. However, increasing the barrier number more than 5,
8.1. BEST ROTOR

according to the discussion and sensitivity analysis in the last chapters, does not have any practical effect on the machine torque, see Figure 8.1. Therefore, four different designs are selected for further analysis. The first design is the 3 barrier rotor without-cut-off rotor structure, the second is the 4 barrier rotor without-cut-off rotor structure, the third is the 4 barrier rotor with-cut-off rotor structure (or 3 barriers and 1 cut-off rotor structure) and finally, the 5 barrier rotor with-cut-off rotor structure (or 4 barriers and 1 cut-off rotor structure). The torque of these machines are shown in Figure 8.1.

Torque ripple for these designs, specially the 4 barrier rotor without-cut-off rotor structure, see Figure 6.6 (left), and the 5 barrier rotor with-cut-off rotor structure (or 4 barriers and 1 cut-off rotor structure), see Figure 6.6 (right) are not acceptable, as it is around 100% at the best current angle. Reducing the torque ripple, which is strongly dependent on the end point position of barriers in the airgap (rotor slot openings), by the present constant rotor slot pitch strategy, is not possible. The ripple reduction of these designs is studied in the next sub-section.

8.1.2 Torque Ripple

Torque ripple minimization by optimizing the design parameter, angle $\beta$, which determines the rotor slot pitch, see the definition of this parameter for with-cut-off rotor structure in Figure 6.1 and for without-cut-off rotor structure in Figure 7.1, is done for the best designs with maximum torque by using the method that is discussed in chapter 7 on page 125. In these designs, the barrier’s ends are rounded, $1 \text{ mm}$ tangential rib is introduced for all barriers and the machine torque ripple is minimized without adjusting the end points with $Y_q$. The final optimized rotor structures are shown in Figure 8.2 and their performance comparison is shown in Table 8.1.

As is expected the machine torque is not affected, however, the machine’s torque ripple is significantly reduced. All promising designs have higher torque, power factor, saliency ratio and difference in inductances, some of them have also lower torque ripple in comparison to the initial machine design ($\text{SynRM IniM}$), see chapter 5 on page 93.

The iron losses are slightly higher than in the initial machine design, because the corners and sharp angles in these designs are not rounded and with fine tuning this difference will be negligible. However, the improvements are not significant. The final design is a compromise between the machine’s performance and simplicity of the rotor structure. Therefore, two designs are selected as the most promising: rotor with 3 barriers and rotor with 3 barriers and 1 cut-off rotor structure. In case of the 3 barriers and 1 cut-off rotor structure machine, the size of the cut-off barrier is very small and the rotor dimensions are practically very similar to the 3 barriers machine.
Figure 8.2: Promising designs after torque and torque ripple optimization.
### 8.2 FINE TUNING

The main idea for fine tuning is to first delete the cut-off barrier in the final design from the last section (3 barriers + 1 cut off) and then to round the corners so as to obtain suitable shapes for manufacturing tools, and to add the radial ribs for barrier numbers 1 and 2, without losing too much on the machine performance in comparison to the initial machine design, see chapter 5 on page 93. Adding radial ribs is essential due to the high centrifugal forces at maximum speed which for this machine is around 4500 rpm. This issue is addressed by FEM based mechanical calculations of the stress in different parts of the rotor due to the centrifugal forces.

#### 8.2.1 Effect of eliminating the cut-off barrier

The best way to eliminate the cut-off barrier in the 3 barriers + 1 cut off design is to consider the 3 barriers rotor, see Figure 8.2. The 3 barriers design show a little

<table>
<thead>
<tr>
<th>Design Type</th>
<th>Initial Machine Design</th>
<th>3 Barriers</th>
<th>3 Barriers + 1 Cutoff</th>
<th>4 Barriers</th>
<th>4 Barriers + 1 Cutoff</th>
</tr>
</thead>
<tbody>
<tr>
<td>No. Of Barriers</td>
<td>5</td>
<td>3</td>
<td>4</td>
<td>4</td>
<td>5</td>
</tr>
<tr>
<td>Current Angle, $\theta$ [°]</td>
<td>60</td>
<td>60</td>
<td>60</td>
<td>60</td>
<td>60</td>
</tr>
<tr>
<td>Current, I [A rms]</td>
<td>50.0</td>
<td>50.6</td>
<td>50.0</td>
<td>50.0</td>
<td>50.0</td>
</tr>
<tr>
<td>Load Angle, $\delta$ [°]</td>
<td>12.6</td>
<td>12.2</td>
<td>12.0</td>
<td>11.8</td>
<td>11.0</td>
</tr>
<tr>
<td>Airgap Flux Density Peak, $B_\delta$ [T]</td>
<td>1.090</td>
<td>1.094</td>
<td>1.097</td>
<td>1.085</td>
<td>1.083</td>
</tr>
<tr>
<td>$i_d$ [A]</td>
<td>35.4</td>
<td>35.4</td>
<td>35.4</td>
<td>35.4</td>
<td>35.4</td>
</tr>
<tr>
<td>$i_q$ [A]</td>
<td>61.2</td>
<td>61.2</td>
<td>61.2</td>
<td>61.2</td>
<td>61.2</td>
</tr>
<tr>
<td>$L_{dm}$ [mH]</td>
<td>26.38</td>
<td>26.52</td>
<td>26.61</td>
<td>26.33</td>
<td>26.37</td>
</tr>
<tr>
<td>$L_{qm}$ [mH]</td>
<td>3.40</td>
<td>3.31</td>
<td>3.28</td>
<td>3.17</td>
<td>2.95</td>
</tr>
<tr>
<td>$\xi = (L_{dm} / L_{qm})$</td>
<td>7.8</td>
<td>8.0</td>
<td>8.1</td>
<td>8.3</td>
<td>8.9</td>
</tr>
<tr>
<td>$(L_{dm} - L_{qm})$ [mH]</td>
<td>23.0</td>
<td>23.2</td>
<td>23.3</td>
<td>23.2</td>
<td>23.4</td>
</tr>
<tr>
<td>IPF [*]</td>
<td>0.736</td>
<td>0.741</td>
<td>0.743</td>
<td>0.746</td>
<td>0.755</td>
</tr>
<tr>
<td>Tag [Nm]</td>
<td>149.6</td>
<td>151.2</td>
<td>152.5</td>
<td>152.8</td>
<td>153.4</td>
</tr>
<tr>
<td>Torque Ripple [%]</td>
<td>17</td>
<td>20</td>
<td>18</td>
<td>14</td>
<td>21</td>
</tr>
<tr>
<td>Eddy Iron Losses [W] @ 3000 rpm</td>
<td>703</td>
<td>709</td>
<td>708</td>
<td>719</td>
<td>772</td>
</tr>
<tr>
<td>Hysteresis Iron Losses [W] @ 3000 rpm</td>
<td>294</td>
<td>304</td>
<td>301</td>
<td>287</td>
<td>273</td>
</tr>
<tr>
<td>Excess Iron Losses [W] @ 3000 rpm</td>
<td>351</td>
<td>345</td>
<td>346</td>
<td>390</td>
<td>367</td>
</tr>
<tr>
<td>Total Iron Losses [W] @ 3000 rpm</td>
<td>1348</td>
<td>1358</td>
<td>1354</td>
<td>1396</td>
<td>1413</td>
</tr>
</tbody>
</table>

Table 8.1: Calculated main machine performance for the original promising designs.
loss of torque, an increase in the torque ripple and an increase in the iron losses, in comparison to the 3 barriers and 1 cut-off rotor structure design, see Table 8.1.

Both designs have better performance than the initial machine design except for torque ripple. Therefore, the 3 barrier design is selected for more analysis instead of the 3 barriers + 1 cut off design. By following this procedure, the number of barriers is reduced from 5 in the initial machine design machine to 3 in the 3 barrier design and almost all performances are kept practically unchanged in comparison to the reference machine, see Table 8.1.

8.2.2 Rounding effect

The optimization procedure that has been used for designing the rotor geometry with 3 barriers was based on the simplified but general rotor geometry that is shown in Figure 7.1 in chapter 7 on page 125. The main idea for this design was based on maintaining the natural path of the flux in the solid rotor, see Figure 8.3. The closer the general barrier shape is to the natural flux path, the closer to optimum will be the design [2], [3], [16], [23], [40], [71] and [138]. This concept can also be used to round the sharp corners of the design that is shown in Figure 8.2.

From inspiration of the natural flux path in Figure 8.3, the optimization procedure that is suggested in chapter 6 and chapter 7 can be also modified, such modifications are presented in chapter 9. Briefly, it can be stated that the modifications follow a mathematic equation that describes the optimum shape of each barrier.
### Table 8.2: Promising design performance comparison, summary of tuning steps.

<table>
<thead>
<tr>
<th>Design Type</th>
<th>Initial Machine Design</th>
<th>3 Barriers + 1 Cut-off</th>
<th>3 Barriers rounded</th>
<th>3 Barriers Fine Tuned + Ribs</th>
</tr>
</thead>
<tbody>
<tr>
<td>No. of Barriers</td>
<td>5</td>
<td>4</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>Current Angle, $\theta$ [<em>°</em>]</td>
<td>60</td>
<td>60</td>
<td>60</td>
<td>60</td>
</tr>
<tr>
<td>Current, $I$ [A rms]</td>
<td>50.0</td>
<td>50.0</td>
<td>50.6</td>
<td>50.6</td>
</tr>
<tr>
<td>Load Angle, $\delta$ [<em>°</em>]</td>
<td>12.6</td>
<td>12.0</td>
<td>12.2</td>
<td>11.4</td>
</tr>
<tr>
<td>Airgap Flux Density pick, $B_\delta$ [T]</td>
<td>1.090</td>
<td>1.097</td>
<td>1.094</td>
<td>1.086</td>
</tr>
<tr>
<td>id [A]</td>
<td>35.4</td>
<td>35.4</td>
<td>35.8</td>
<td>35.8</td>
</tr>
<tr>
<td>iq [A]</td>
<td>61.2</td>
<td>61.2</td>
<td>62.0</td>
<td>62.0</td>
</tr>
<tr>
<td>$L_{dm}$ [mH]</td>
<td>26.38</td>
<td>26.61</td>
<td>26.20</td>
<td>26.07</td>
</tr>
<tr>
<td>$L_{qm}$ [mH]</td>
<td>3.40</td>
<td>3.28</td>
<td>3.27</td>
<td>3.04</td>
</tr>
<tr>
<td>$\xi = (L_{dm} / L_{qm})$</td>
<td>7.8</td>
<td>8.1</td>
<td>8.0</td>
<td>8.6</td>
</tr>
<tr>
<td>$(L_{dm} - L_{qm})$ [mH]</td>
<td>23.0</td>
<td>23.3</td>
<td>22.9</td>
<td>23.0</td>
</tr>
<tr>
<td>IPF [*]</td>
<td>0.736</td>
<td>0.743</td>
<td>0.741</td>
<td>0.750</td>
</tr>
<tr>
<td>Tag [Nm]</td>
<td>149.6</td>
<td>152.5</td>
<td>151.2</td>
<td>152.3</td>
</tr>
<tr>
<td>Torque Ripple [%]</td>
<td>17</td>
<td>18</td>
<td>20</td>
<td>21</td>
</tr>
<tr>
<td>Eddy Iron Losses [W] @ 3000 rpm</td>
<td>703</td>
<td>708</td>
<td>709</td>
<td>703</td>
</tr>
<tr>
<td>Hysteresis Iron Losses [W] @ 3000 rpm</td>
<td>294</td>
<td>301</td>
<td>304</td>
<td>302</td>
</tr>
<tr>
<td>Exces Iron Losses [W] @ 3000 rpm</td>
<td>351</td>
<td>346</td>
<td>345</td>
<td>341</td>
</tr>
<tr>
<td>Total Iron Losses [W] @ 3000 rpm</td>
<td>1348</td>
<td>1354</td>
<td>1358</td>
<td>1346</td>
</tr>
</tbody>
</table>

The main goals of rounding are: to achieve a simple geometry for the manufacturing tool, to optimize the geometry for lower $L_q$, this gives higher saliency ratio, to achieve higher power factor and to reduce iron losses. Certainly, by rounding the sharp points some iron will be removed from the rotor and some air or insulation will be added to the rotor, therefore, $L_q$ will be reduced. However, iron losses are a function of the amount of iron in the rotor which is mainly dependent on the segment length and by just rounding the sharp corners will have little or negligible effect on iron losses. Rounding of the barrier’s end close to the airgap can reduce the iron losses, as well.

Based on these ideas and by considering that rounding will have very little effect on $L_d$ and consequently, on torque, the 3 barriers geometry in Figure 8.2 has been rounded. The resultant geometry is shown in Figure 8.4 (c). The corners of the
barriers have been rounded and the barrier leg part width in the d-axis has been increased, this width is gradually reduced when the barrier comes closer to the airgap. An attempt is made to keep the segment’s width as constant as possible.

The performance of this machine is compared with other designs in the Table 8.2. As can be seen from the table the q-axis inductance, saliency ratio, torque and power factor have been improved slightly in comparison to the original 3 barriers machine. Furthermore, the iron losses for the rounded geometry are reduced in comparison to the original geometry. This can be explained by the segment’s dimensions. Therefore, the rounded design is chosen for more analysis, we call this design 3 barriers rounded.

Figure 8.4: Summary of tuning steps, geometries.
8.2. FINE TUNING

Figure 8.5: *The improved machine design final rotor, stress distribution and deformation displacement (amplified by factor 100) in iron sheets at 4500 rpm, that shows 34% safety margin.*

8.2.3 Ribs effect

Due to mechanical aspects for high speed applications, here 4500 rpm, the rotor structure, see Figure 8.4 (c), is not self-sustained and needs to be supported with additional radial ribs, see Figure 8.4 (d). These ribs directly increase the q-axis flux but do not have a significant effect on the d-axis flux. It is evident that adding the radial ribs, 1.8 mm in barrier one and 1 mm in barrier two, increases the q-axis inductance by almost 15%, but does not change the d-axis inductance, see Table 8.2. Consequently, the power factor and saliency ratio is reduced. Torque is also dropped by about 2.8%. All these negative effects must be weighted against the mechanical benefit for high speed application.

8.2.4 Mechanical refinement

The improved machine design final rotor (M600-50A material) [72] is simulated with FEM, to investigate the mechanical safety margins at 4500 rpm. Disregarding possible calibration factors between the software and reality simulation shows that there is a 34% hotspot stress safety margin with respect to the iron material’s Yield Strength (300 MPa), see Figure 8.5. A safety margin of 34% means that the machine speed can go up to 5400 rpm without forcing the material characteristics to move from the elastic to the plastic region and with a 0% hotspot safety margin with respect to the material’s Yield Strength.
8.3 Heat-run test on SynRM and IM

Measurement and test are the best methods to evaluate the effectiveness of the design and fine tuning procedures of the SynRM that are discussed in chapters 6, 7 and 8. For this purpose, the final optimized and tuned rotor geometry, refer to Figure 8.4 (d) and Figure 8.5 is prototyped and it’s performance is measured and compared to it’s IM counterpart. The laminations and complete machine of the improved machine design are shown in Figure 8.6 and the test-bench for the measurements are shown in Figure 8.7. The results from the measurements are presented in this section.

The test conditions are similar to the first test situations in section 5.4 on page 96 of chapter 5. However, instead of a laboratory inverter that has a controller based on the Field Orientation Control (FOC), an industrial inverter is used during the test. The inverter control in this system is not based on FOC, rather it has a built in direct torque control (DTC) that is modified for SynRM’s MTPA control. No speed sensor is used in the test set-up. Instead the industrial drive built for sensorless control is used. The inverter operates with an average switching frequency of 4 kHz.

After thermal steady-state is reached, the most important mechanical and electrical parameters are read from the instruments.

To enable a comparison with the heat-run test in section 5.4 on page 96 on the initial machine design, see chapter 5 on page 93, the system performance of the whole drive, machine and motor, is measured for the machine that has the improved machine design during the test. For this purpose, an extra power analyzer is used at the inverter input in order to measure the inverter performance. By this means, the inverter input power and consequently, the inverter losses are evaluated by measuring the output power of the inverter that is the input power of the machine. Therefore, the overall system performance such as system efficiency and inverter
8.3. HEAT-RUN TEST ON SYNRM AND IM

Both machine’s shaft temperatures, in the drive and non-drive ends, are measured by a mobile temperature probe immediately after the heat-run test. This gives an overall picture regarding the rotor’s temperature for both the SynRM and IM. The test results on SynRM (improved machine design) and it’s counterpart IM, at constant-torque conditions and at 1000, 1500 and 2500 rpm, are summarized in Table 8.3 - columns (2,5,8) and - columns (1,4,7), respectively. The SynRM performance before prototyping and measurements that is evaluated by the model of the machine and methods that are presented in chapter 2 are shown in Table 8.3 - columns (3,6,9).

8.3.1 Overall measured performances of the SynRM and the IM machines

Torque capability of the SynRM closely follows the IM, as is expected, see the $T/I$ row of Table 8.3 - columns (1,2), (4,5) and (7,8). The expected efficiency improvements for the SynRM in comparison to the IM according to the Figure 2.11 for output powers of 9, 13.5 and 22.5 kW are around 3.5, 2.9 and 2.4 %-units, respectively. The measurements show the corresponding efficiency improvements of 3.6, 2.2 and 1.9 %-units, respectively, which are in good agreement with expectation.

The SynRM has lower winding temperature rise than the IM for the same shaft power, by 5 – 12 $K$ depending on speed, due to the lower copper loss. The SynRM machine’s shaft temperature is also lower than the IM for the same shaft power, by 9 – 19 $K$ depending on speed. Lower temperatures in both windings and shaft can directly increase the lifetime of the SynRM in comparison to the IM machine, see chapter 5. The SynRM housing is cooler than the IM, roughly by 10 $K$, as well. Power factor of the SynRM is lower than the IM by 5 – 9 %-units. However, the
### Table 8.3: Heat-run test measurements on SynRM (improved machine design) and IM, 15kW M machine.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine Type</td>
<td>IM SynRM</td>
<td>SynRM ImprM</td>
<td>IM SynRM</td>
<td>SynRM ImprM</td>
<td>IM SynRM</td>
<td>SynRM ImprM</td>
<td>IM SynRM</td>
</tr>
<tr>
<td>Operation Type</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
<td>VSD VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>1000</td>
<td>1003</td>
<td>1500</td>
<td>1500</td>
<td>2499</td>
<td>2501</td>
<td>2500</td>
</tr>
<tr>
<td>fs [Hz]</td>
<td>34,4</td>
<td>33,4</td>
<td>33,3</td>
<td>33,3</td>
<td>51</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>3,160</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>ns: no. of cond. / slot</td>
<td>12</td>
<td>12</td>
<td>12</td>
<td>12</td>
<td>12</td>
<td>12</td>
<td>12</td>
</tr>
<tr>
<td>cs: winding connection</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Volts [V], Phase</td>
<td>147</td>
<td>153</td>
<td>147</td>
<td>147</td>
<td>215</td>
<td>205</td>
<td>219</td>
</tr>
<tr>
<td>I rms [A], Phase</td>
<td>29</td>
<td>30</td>
<td>31</td>
<td>31</td>
<td>29</td>
<td>31</td>
<td>31</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>1138</td>
<td>741</td>
<td>839</td>
<td>1280</td>
<td>215</td>
<td>205</td>
<td>219</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>534</td>
<td>560</td>
<td>608</td>
<td>536</td>
<td>613</td>
<td>611</td>
<td>607</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>52</td>
<td>52</td>
<td>52</td>
<td>52</td>
<td>86</td>
<td>86</td>
<td>78</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>552</td>
<td>129</td>
<td>179</td>
<td>658</td>
<td>229</td>
<td>338</td>
<td>1165</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>88,2</td>
<td>87,8</td>
<td>87,6</td>
<td>88,3</td>
<td>87,7</td>
<td>87,2</td>
<td>86,8</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>9,2</td>
<td>9,2</td>
<td>9,2</td>
<td>13,9</td>
<td>13,8</td>
<td>13,7</td>
<td>12,7</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>10,4</td>
<td>10,0</td>
<td>10,0</td>
<td>15,1</td>
<td>14,7</td>
<td>14,7</td>
<td>14,7</td>
</tr>
<tr>
<td>Sdn [kVA]</td>
<td>12,6</td>
<td>13,7</td>
<td>13,8</td>
<td>18,6</td>
<td>19,2</td>
<td>20,6</td>
<td>20,6</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>89,0</td>
<td>92,6</td>
<td>91,6</td>
<td>91,5</td>
<td>93,7</td>
<td>93,0</td>
<td>91,9</td>
</tr>
<tr>
<td>Efficiency - PF1 [*]</td>
<td>0,821</td>
<td>0,729</td>
<td>0,723</td>
<td>0,815</td>
<td>0,765</td>
<td>0,714</td>
<td>0,807</td>
</tr>
<tr>
<td>1 / (Efficiency - PF1) [*]</td>
<td>1,37</td>
<td>1,48</td>
<td>1,51</td>
<td>1,34</td>
<td>1,39</td>
<td>1,50</td>
<td>1,35</td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>70</td>
<td>58</td>
<td>61</td>
<td>66</td>
<td>61</td>
<td>61</td>
<td>74</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1,ns=1)</td>
<td>0,26</td>
<td>0,24</td>
<td>0,23</td>
<td>0,26</td>
<td>0,23</td>
<td>0,23</td>
<td>0,25</td>
</tr>
<tr>
<td>Housing Temp. Rise [K]</td>
<td>46</td>
<td>35</td>
<td>42</td>
<td>36</td>
<td>49</td>
<td>40</td>
<td></td>
</tr>
<tr>
<td>Inv. Avr. Switch. freq. [kHz]</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td></td>
</tr>
<tr>
<td>Inverter input power [kW]</td>
<td>10,8</td>
<td>10,4</td>
<td>15,6</td>
<td>15,2</td>
<td>25,5</td>
<td>25,0</td>
<td></td>
</tr>
<tr>
<td>Inverter losses [W]</td>
<td>405</td>
<td>398</td>
<td>442</td>
<td>442</td>
<td>699</td>
<td>770</td>
<td></td>
</tr>
<tr>
<td>Inverter efficiency [%]</td>
<td>96,2</td>
<td>96,2</td>
<td>97,2</td>
<td>97,1</td>
<td>97,3</td>
<td>96,9</td>
<td></td>
</tr>
<tr>
<td>System efficiency [%]</td>
<td>85,7</td>
<td>89,0</td>
<td>88,9</td>
<td>91,0</td>
<td>89,3</td>
<td>90,9</td>
<td></td>
</tr>
</tbody>
</table>

The measured inverter performance of the SynRM and IM drive, see Table 8.3, clearly shows that the machine type does not affect the inverter at the tested operating points. The inverter efficiency is the same for both machines with the same output power and speed. Consequently, better SynRM efficiency directly increases the overall system efficiency as well. The improvement of the SynRM based drives system efficiency in comparison to the IM drive for output powers of 9, 13.5 and 22.5 kW are around 3.3, 2.1 and 1.6 %-units, see Table 8.3.
Part III

Possible Improvements
Chapter 9

SynRM: Optimized machine design

The rotor geometry design procedure regarding the improved machine design has been developed in chapter 6 on page 105 and chapter 7 on page 125. The optimized machine design (SynRM OptM) to achieve an optimum performance of the synchronous reluctance machine (SynRM) rotor geometry will be discussed in this chapter with some new ideas regarding the flux line’s shape in the solid rotor. Naturally, to have an anisotropic structure the q-axis flux must somehow be blocked as much as possible and simultaneously the d-axis flux must flow smoothly, see chapter 8 on page 135 and [16].

One possibility to achieve this is to align the barrier edges along the d-axis natural flux lines in the solid rotor. Fortunately, this shape can be expressed with a simple mathematical equation by using N. E. Joukowski airfoil potential function [73] and thus can be used for optimization purposes. Implementing the new rotor general shape will help to further automate the design procedure, reduce the finite element modeling time and also improve the machine performance, compared to the previous designs.

According to the new procedure, a rotor geometry has been designed and its calculated performance compared with previous designs, see chapter 5 on page 93 and chapter 8 on page 135, which shows promising improvement in almost all machine performances. The optimized machine design procedure is evaluated by measurements. For this purpose a prototype of the final optimized machine design SynRM is manufactured. The performance of this machine is measured and compared with the improved machine design SynRM (SynRM ImprM), see its geometry in chapter 8, with the same machine structure. Test conditions are similar to the heat-run test in section 8.3 on page 144 of chapter 8. This test confirms the analytical results. New set of measurement results on the improved machine design SynRM and its counterpart Induction Machine (IM) are also reported in this chapter.
9.1 General theory

9.1.1 Field in the solid rotor and reluctance concept

The main characteristic of an anisotropic rotor structure in SynRM is to block completely the quadrature-axis (q-axis) flux and simultaneously, fully conduct the direct-axis (d-axis) flux. The d-axis flux lines in the solid rotor, when there is no insulation barrier inside the rotor, is shown in Figure 9.1 (left). If the cross-coupling effect between the d- and the q-axis is disregarded, the q-axis flux lines will be completely perpendicular to the d-axis flux lines due to symmetry inside the rotor, see Figure 9.1 (right).

However, inserting the barriers into the rotor as is shown in [2] is the best way to create an anisotropic structure. It was also shown earlier that there are a lot of different ways to introduce a certain amount of insulation inside the rotor by using different shapes of insulation layers see e.g. [3], [16] and [57]. One important rule must always be considered. The q-axis reluctance must be increased as much as possible, without greatly affecting the d-axis permeance path. The method by which this can be achieved is completely related to the shape of each barrier and the amount of insulation. As is known in fluid dynamics the best way to block a fluid flow (the flux lines in the solid rotor are similar to the equi-velocity lines in a fluid flow) is to put the blocker perpendicular to the flow lines.

In a similar way this strategy can also be applied to the q-axis flux in the rotor of the SynRM. Therefore, if the barrier edges are set so that they are parallel to the d-axis flux and perpendicular to the q-axis flux then the above anisotropic rule will be satisfied, and the d-axis flux distortion will be at a minimum, while the q-axis flux will be effectively blocked. This means that the best shape for barriers is the
9.1. GENERAL THEORY

9.1.2 Effect of shaft

If the machine shaft is made from a non-magnetic material, then the d- and q-axis flux lines will preferably not go inside the shaft and therefore, are bended near the shaft. This situation is shown in Figure 9.2 (left). Still with high accuracy the d- and q-axis field lines can be considered orthogonal, in particular for the lines that are far from the shaft surface.

9.1.3 Analytical approach

Fortunately, the d-axis flux lines in the solid rotor can be described analytically by a simple mathematical equation. By using some simple conformal mapping concept in complex analysis theory [73], and using N. E. Joukowski airfoil potential function, the following equation can be derived for describing the flux lines inside the solid rotor:

\[
r(\theta) = \left( \frac{D_{\text{shaft}}}{2} \right) \cdot \sqrt{\frac{C + \sqrt{C^2 + 4 \cdot \sin^2 (p\theta)}}{2 \cdot \sin (p\theta)}}. \tag{9.1}
\]

In Equation (9.1), \( p \) is the machine pole pair number, \( r \) is the radius and \( \theta \) is the mechanical angle from the d-axis in polar coordinates, \( D_{\text{shaft}} \) is the shaft diameter, see also Figure 9.3, and \( C \) is a constant, which is a function of the point coordinates that the curve is passing through.
These curves can be expressed based on angle $\theta(r)$ according to the following equation:

$$
\theta(r) = \frac{1}{p} \cdot \sin^{-1} \left[ \frac{C \cdot \left( \frac{r}{D_{\text{shaft}}/2} \right)^p}{\left( \frac{r}{D_{\text{shaft}}/2} \right)^{2p} - 1} \right]. 
$$

(9.2)

Actually, if $r$ and $\theta$ (for a point) are known then $C$ can be calculated as follows:

$$
C = \frac{\sin (p\theta) \cdot \left( \frac{r}{D_{\text{shaft}}/2} \right)^{2p} - 1}{\left( \frac{r}{D_{\text{shaft}}/2} \right)^p}. 
$$

(9.3)

Equations (9.1) and (9.2) accurately describe the shape of the field lines and is compatible with the FEM calculations, see Figure 9.2 (right).

9.2 Design procedure based on the field lines in solid rotor

9.2.1 General rotor arrangement

Based on the flux line shape and Equation (9.1), the suggested general rotor shape with $(k)$ barriers is developed and shown in Figure 9.4 (left). The critical points of
Figure 9.4: (left) Positioning $k$ barriers inside the rotor, using the natural flux line in the solid rotor as guide line. (right) Definition of the important points belonging to the barrier geometries.

The optimization procedure in chapters 6 and 7, that is used for improved machine design, can be modified with this new arrangement and the $d$-axis insulation ratio can be omitted. In the following the old procedure together with some modifications will be adapted to the design procedure for the optimization of machine.

### 9.2.2 End point’s angles and rotor slot pitch

The starting point is to calculate the rotor slot pitch. The constant rotor slot pitch is introduced to minimize the torque ripple. An extra angle, $\beta$, is also used as a rotor slot pitch controller, see Figures 9.4 (right), 9.5 and 9.6. As a first step assume that $\beta$ is known. Then, based on the number of barriers $k$ and assumed $\beta$, the position of end points of barriers in the airgap, points $D_i$, will be as shown in Figure 9.4 and Figure 9.5. In this conditions the rotor slot pitch (for without-cut-off rotor structure), $\alpha_m$, can be determined by Equation (9.4).
9.2.3 Barriers sizing and positioning

The insulation ratio in the q-axis together with the barrier’s shape is the next most important macroscopic parameter of an anisotropic structure.

\[
k_{wq} = \frac{\text{Total Insulation}}{\text{Total Iron}}_{\text{along q-axis}} = \frac{l_a}{l_y} = \frac{\left( \frac{D}{2} - \frac{D_{\text{shaft}}}{2} - g \right) - \sum_{h=1}^{k+1} S_h}{\sum_{h=1}^{k+1} S_h} (9.5)
\]

Insulation ratio in the q-axis, \( k_{wq} \), is defined according to Equation (9.5), as the ratio of total width of the insulation layers (barriers) along the q-axis, \( l_a \), over the total width of the conducting layers (segments), \( l_y \), along the q-axis, see also Figure 9.6.
The key step is to optimally determine the coordinates of points $B_{1k}$ and $B_{2k}$ along the q-axis, see Figure 9.4 (right). Then, calculating the curve potentials $C_{1k}$ and $C_{2k}$ of the edges of each barrier, see Figure 9.6, by using Equation (9.3) is straightforward. To do so, the width of the barriers and segments along the q-axis have to be determined.

**Segments width in the q-axis**

The calculation of segment’s width has the same procedure as has been discussed in detail in chapters 6 and 7. Briefly, the following system of equations has to be solved.

\[
\frac{2S_1}{S_2} = \frac{f_{d_1}}{f_{d_2}} \quad \kappa = \frac{S_h}{S_{h+1}} = \frac{f_{d_h}}{f_{d_{h+1}}} \quad h = 2, \ldots, k \quad (9.6)
\]

\[
\sum_{h=1}^{k+1} S_h = l_y = \left( \frac{D}{2} - \frac{D_{sh} f_1}{2} - g \right) \frac{1}{1 + k_{wq}} \quad (9.7)
\]

In Equation (9.6), $f_{d_k}$ is the average per unit MMF, $\cos(p\alpha)$, over segment $k$, if a d-axis MMF $f_d$ is applied to the machine, see Figure 9.7 (left). By solving Equations (9.6) and (9.7) the primary width of each segment in the q-axis can be determined as a function of $k_{wq}$ and indirectly also the parameters $k$ and $Î²$.

**Figure 9.7:** (right) Per-unit MMF distribution over segments when the MMF is applied in the q-axis, (left) per-unit MMF distribution over the segments when the MMF is applied in the d-axis, for the geometry in Figure 9.6, (without-cut-off rotor structure), see Figure 7.2 too.
Barrier width in the q-axis

Similarly for barrier’s width $W_{1_k}$ the following system of the equations has to be solved.

\[
\frac{W_{1_h}}{W_{1_{h+1}}} = \left( \frac{\Delta f_h}{\Delta f_{h+1}} \right)^2 \quad h = 1, \ldots, k - 1 \tag{9.8}
\]

\[
\sum_{h=1}^{k} W_{1_h} = l_a = \frac{\left( \frac{D}{2} - \frac{D_{\text{shaft}}}{2} - g \right)}{1 + \frac{1}{k_{\text{wq}}}} \tag{9.9}
\]

Equation (9.8) is a combination of constant barrier’s permeance condition together with the rule for optimal distribution of total insulation $l_a$ within the $k$ barriers, as described by Vagati [54]. In this equation, $\Delta f_k$ is the difference in the average per-unit MMF, $\sin (p\alpha)$, over barrier $k$, where $p$ is pole pair number. If a q-axis magneto-motive force $f_q$ is applied to the machine, $\Delta f_k = f_{q_{k+1}} - f_{q_k}$, for details refer to [3], [5], [54] and [57], see Figure 9.7 (right). Solution of Equations (9.8) and (9.9) will give each barrier’s width in the q-axis as a function of $k_{\text{wq}}$ and indirectly also the parameters $k$ and $\beta$.

9.3 Simulation and comparison

Based on the last section optimization procedure a new rotor geometry for 15kWM Machine is optimized. The optimization steps and a discussion around the characteristic and performance of different rotor geometries will be presented in this section.

9.3.1 Step by step design and optimization example

A 4 barriers rotor geometry optimization with end adjusting procedure, using parameter $Y_{q_k}$, and using the field aided design is presented in this sub-section. First the insulation ratio is kept constant and the rotor slot pitch is optimized. Then the insulation ratio is optimized when the rotor slot pitch is constant. The total number of geometries that have been simulated using FEM is 10. Some extra geometries to show the effect of insulation ratio in more detail are also modeled.

Ripple minimization, angle $\beta$ optimization

For torque maximization the insulation ratio should be a number between 0, 1 to 1. Therefore, as an initial guess, the insulation ratio is kept 0, 5 and $\beta$ is changed. The current angle is set to 45°, see chapter 6 on the effect of electrical parameters, and the simulations are performed at nominal stator current. The starting point for $\beta$ can be a value that gives the fully equal rotor slot pitch. This implies that $2\beta = \alpha_m$ for without-cut-off rotor structure arrangement, see Figures 9.5 and 9.6.
9.3. SIMULATION AND COMPARISON

Figure 9.8: Effect of angle $\beta$ on torque and torque ripple for 4 barriers rotor geometry at $kwq = 0.5$. End points are adjusted by $Yq_k$ parameter.

Now for each $\beta$ and $kwq = 0.5$, the width of the barriers and segments in the $q$-axis are calculated using Equations (9.8) and (9.6), respectively. $Yq_k$ and points $B1_k$ and $B2_k$ are also determined. Then, $Yq_k$ is adjusted while the width of the barriers is kept constant in order to adjust the end points. Points $B1_k$ and $B2_k$ are determined for the best $Yq_k$ and the barrier edges line potentials $C1_k$ and $C2_k$ are calculated, see Figures 9.4, 9.5 and 9.6. Finally the resultant geometry is simulated with Finite Element Method (FEM). The effect of varying $\beta$ on torque and torque ripple is shown in Figure 9.8. Clearly $\beta = 7^\circ$ minimizes the ripple.

Torque maximization, $kwq$ optimization

Torque in SynRM is essentially a function of the insulation ratio $kwq$ in the rotor. Now to maximize the torque $kwq$ is varied and each geometry is analyzed with FEM. The rotor slot pitch $\alpha_m$ and $\beta$ are kept constant and equal to the optimum value that minimizes the machine’s torque ripple. The effects of $kwq$ on machine torque, IPF and torque ripple are shown in Figure 9.9. There is an optimum $kwq$ value around $0.5 - 0.6$ for maximum torque. $kwq$ is varied within a wider range from $0.1 - 1.9$ to show clearly the importance of this parameter on the rotor anisotropic structure and the machine’s performance characteristic. The study in the last two sections shows that with a limited number of FEM simulations, a rotor geometry for the SynRM can be designed and optimized.

9.3.2 Result comparison and discussion

A short comparison between different rotor geometries is done to verify the effectiveness of the proposed design procedure. These geometries are those that have been designed in the previous chapters. The initial machine design is studied in chapter 5, and the improved machine design is analyzed in chapter 8. A field aided
design rotor is optimized for the optimized machine design, with adjustment of end points, as described in the last sub-sections. These machine’s structures are shown in Figure 9.10 (top).

In the optimized machine design $k_wq$ is slightly increased from its MTPA value, $k_wq = 0.5$, in order to compensate for the effect of radial ribs on the power factor.

Figure 9.9: Effect of $k_wq$ on torque, IPF and torque ripple for 4 barriers rotor at $\beta = 7^\circ$. End points are adjusted by varying the $Y_{qk}$ parameter.

Figure 9.10: (top) Different designs, initial, improved and optimized, and (bottom) their final structure for prototypes.
9.4. HEAT-RUN TEST ON SYNRM (IMPROVED AND OPTIMIZED MACHINE DESIGNS) AND IM

<table>
<thead>
<tr>
<th>Design Type</th>
<th>Initial Machine</th>
<th>Improved Machine</th>
<th>Optimized Machine</th>
</tr>
</thead>
<tbody>
<tr>
<td>No. of Barriers</td>
<td>5</td>
<td>3</td>
<td>4</td>
</tr>
<tr>
<td>Current Angle, $\theta$ [º]</td>
<td>60</td>
<td>60</td>
<td>60</td>
</tr>
<tr>
<td>Current, I $[\text{A rms}]$</td>
<td>50</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td>Back EMF, E [V pick]</td>
<td>290</td>
<td>296</td>
<td>282</td>
</tr>
<tr>
<td>Back EMF Angle [º]</td>
<td>105.5</td>
<td>104.2</td>
<td>103.4</td>
</tr>
<tr>
<td>$\approx \frac{L_{dm}}{mH}$</td>
<td>24.02</td>
<td>24.74</td>
<td>23.58</td>
</tr>
<tr>
<td>$\approx L_{qm} [\text{mH}]$</td>
<td>2.93</td>
<td>2.69</td>
<td>2.28</td>
</tr>
<tr>
<td>$\approx \xi = \left( \frac{L_{dm}}{L_{qm}} \right)$</td>
<td>8.2</td>
<td>9.2</td>
<td>10.3</td>
</tr>
<tr>
<td>$\approx (L_{dm} - L_{qm}) [\text{mH}]$</td>
<td>21.1</td>
<td>22.1</td>
<td>21.3</td>
</tr>
<tr>
<td>IPF [*]</td>
<td>0.744</td>
<td>0.759</td>
<td>0.771</td>
</tr>
<tr>
<td>$T_{ag} [\text{Nm}]$</td>
<td>146.3</td>
<td>149.0</td>
<td>151.3</td>
</tr>
<tr>
<td>Torque Ripple [%]</td>
<td>16</td>
<td>22</td>
<td>12</td>
</tr>
</tbody>
</table>

Table 9.1: Different designs, initial, improved and optimized, performance comparison, see Figure 9.10 (top) for the structure of these machines.

of the machine, see Figure 9.9. Therefore, $k_{wq} = 0.75$ is chosen for the final rotor structure, see Figure 9.10 (bottom). The performance of the three designs are shown in Table 9.1 for comparison. The results clearly indicate that field aided design (optimized machine design) can slightly improve the performance. If adjustment of the end points is adopted the ripple can be reduced to a value that is even lower than the initial machine design. The main advantage of the optimized machine design is its higher power factor in comparison to other designs. This is due to the suitable barrier’s shape which effectively blocks the q-axis flux.

9.4 Heat-run test on SynRM (improved and optimized machine designs) and IM

The optimized machine design procedure is evaluated by measurements. For this purpose a prototype of the final optimized machine design SynRM, see its geometry in Figure 9.10 (bottom-right), is manufactured, using M400-50A [72] instead. The performance of this machine is measured and compared with the improved machine design SynRM, see its geometry in Figure 9.10 (bottom-middle) and chapter 8, with the same machine structure but using M600-50A [72]. The tests conditions are almost the same as the heat-run test in section 8.3 on page 144, except for the difference that both machines are now supplied from a sinusoidal supply. Heat-run test results are summarized in Table 9.2 - columns (7,8).

As was mentioned earlier, the main advantage of the optimized machine design in comparison to the improved machine design is its higher power factor. The measurements on these machines clearly indicate a higher power factor of the optimized
### Table 9.2: Heat-run test measurements on SynRM (improved and optimized machine designs) and IM machines, 15kW M machine, SynRM ImprM (improved machine design, see Figure 9.10 (bottom-middle)) is made with M600-50A and SynRM OptM (optimized machine design, see Figure 9.10 (bottom-right)) is made with M400-50A.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Ref. IM Pout at 1500 rpm [kW]</td>
<td>15</td>
<td>15</td>
<td>15</td>
<td>15</td>
<td>15</td>
<td>15</td>
<td>15</td>
</tr>
<tr>
<td>Machine Type</td>
<td>IM</td>
<td>SynRM</td>
<td>IM</td>
<td>SynRM</td>
<td>IM</td>
<td>SynRM</td>
<td>IM</td>
</tr>
<tr>
<td>Operation Type</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>DOL</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>1499</td>
<td>1498</td>
<td>2997</td>
<td>3000</td>
<td>4497</td>
<td>4500</td>
<td>4504</td>
</tr>
<tr>
<td>Fs [Hz]</td>
<td>50,9</td>
<td>49,9</td>
<td>100,9</td>
<td>100</td>
<td>150,8</td>
<td>150</td>
<td>150,1</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>1,840</td>
<td>0</td>
<td>1,008</td>
<td>0</td>
<td>0,618</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>61</td>
<td>57</td>
<td>120</td>
<td>117</td>
<td>187</td>
<td>177</td>
<td>208</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>119</td>
<td>128</td>
<td>121</td>
<td>127</td>
<td>118</td>
<td>126</td>
<td>157</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>2378</td>
<td>1269</td>
<td>3930</td>
<td>2352</td>
<td>5107</td>
<td>3497</td>
<td>5006</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>102</td>
<td>101</td>
<td>103</td>
<td>102</td>
<td>103</td>
<td>102</td>
<td>146</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>16,1</td>
<td>15,9</td>
<td>32,3</td>
<td>32,1</td>
<td>48,3</td>
<td>48,0</td>
<td>69,0</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>18,4</td>
<td>17,2</td>
<td>36,2</td>
<td>34,5</td>
<td>53,4</td>
<td>51,5</td>
<td>74,1</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>22,4</td>
<td>22</td>
<td>43,9</td>
<td>45</td>
<td>66,3</td>
<td>67</td>
<td>98,2</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>87,1</td>
<td>92,6</td>
<td>89,1</td>
<td>93,2</td>
<td>90,4</td>
<td>93,2</td>
<td>93,2</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0,822</td>
<td>0,770</td>
<td>0,825</td>
<td>0,766</td>
<td>0,806</td>
<td>0,769</td>
<td>0,754</td>
</tr>
<tr>
<td>Efficiency - PF1</td>
<td>0,716</td>
<td>0,713</td>
<td>0,735</td>
<td>0,714</td>
<td>0,729</td>
<td>0,717</td>
<td>0,703</td>
</tr>
<tr>
<td>1/(Efficiency · PF1) [*]</td>
<td>1,40</td>
<td>1,40</td>
<td>1,36</td>
<td>1,40</td>
<td>1,37</td>
<td>1,40</td>
<td>1,42</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1, ns=1)</td>
<td>0,33</td>
<td>0,30</td>
<td>0,33</td>
<td>0,31</td>
<td>0,34</td>
<td>0,31</td>
<td>0,36</td>
</tr>
<tr>
<td>Brng D-side Temp. Rise [K]</td>
<td>56</td>
<td>26</td>
<td>72</td>
<td>34</td>
<td>72</td>
<td>41</td>
<td>103</td>
</tr>
<tr>
<td>System efficiency [%]</td>
<td>82,4</td>
<td>87,1</td>
<td>86,6</td>
<td>90,1</td>
<td>88,6</td>
<td>91,0</td>
<td>N.A.</td>
</tr>
</tbody>
</table>

The improved machine design SynRM performance is also compared with its counterpart Induction Machine (IM) performance, using the same test conditions for both machines similar to the tests in section 8.3 on page 144 of chapter 8. The measurement results at the same shaft torque are summarized in Table 9.2 - columns (1,2), (3,4) and (5,6) for speeds 1500, 3000 and 4500 rpm, respectively.

The improved machine design SynRM performance in comparison to IM shows similar behavior, in terms of torque capability, expected efficiency, windings temperature rise, shaft temperature, power factor, inverter current and overall performances of the system and inverter to tests discussed earlier and presented in chapters 5 and 8.
Chapter 10

SynRM: Pole number effect

The effect of the number of poles on Synchronous reluctance machine (SynRM) performance, is discussed in this chapter. This subject is only briefly addressed in the literature. In this chapter a comprehensive study on this subject will be reported.

Firstly, for each number of poles a rotor geometry is optimized by methods that have been discussed in chapter 9 on page 149. The 15kWM machine has been used in this study. Then, optimized machine performances have been compared at constant torque condition over a wide speed range suitable for normal variable speed drive (VSD) applications. Separately, a comparison at constant temperature rise and constant speed condition is also done and presented.

The study shows that the 4-pole machine has the best performance at constant torque condition and for a wide speed range (0 − 6000 rpm), in terms of efficiency, power factor and temperature rise, compared to the other pole numbers. Equivalently, if temperature rise is set equal for all pole numbers, then the 4-pole machine advantage is evident by higher output power, higher efficiency and shaft torque. The power factor of the 4-pole machine is slightly lower than the corresponding induction machine (IM), due to the negative effect of the q-axis flux that is needed in SynRM.

Finally, it can be concluded that in VSD applications, speed range 0 − 6000 rpm, the 4-pole machine has the best performance, if the existing stator, 15kWM machine, of the corresponding IM is used for the SynRM.

10.1 Background, methods and tools

The effect of pole number on SynRM performance, has been qualitatively discussed in [3] and [16]. The reported study shows that the number of pole has to be kept as low as possible. This subject is briefly studied in some literatures [3], [21], [45], [74] and [76], but is not addressed comprehensively. In [74] the pole number effect on a very simple and new salient pole machine is analytically discussed. This study
shows that the 2-pole new salient pole machine has the best performance.

The stall torque capability of SynRM, brushless DC and IM at the same power dissipation is deeply discussed and compared in [3], [21], [45] and [76] and briefly in [16]. In these studies a comparison is made between 4- and 6-pole machines, however, for other pole numbers no analysis has been presented. For example, the stall torque optimization results for 4- and 6-pole SynRM machines are reported in [76], based on two independent design parameters, inner over outer stator diameter ratio and the flux density. This study shows that the stall torque capability of the 4-pole machine is always lower than the 6-pole machine if the mechanical iron ribs are not considered. The torque increase from 4- to 6-pole machine is not significant due to the increase in the magnetizing current similar to the IM [6]. If ribs, which are required in practice, are considered then there is no practical difference between 4- and 6-pole machine in stall torque capability [76].

The flux ratio ($\lambda_q/\lambda_d$) at rated condition in [76] is also discussed. Increasing the pole number increases the required q-axis flux and has therefore a negative effect on the power factor. Ribs increase the drawback of higher pole number even further, thus lower pole number is preferable [75]. In fact, the best pole number choice comes from a trade-off between $L_d$ and $L_q$ inductances. Few poles do not allow an effective shaping of the q-axis barriers, while too many poles reduce the pole span and consequently the $L_d$ value [75]. In addition, the impact of the leakage flux due to rotor ribs can also be related to pole number. In fact, the rib width should decrease with increasing number of poles, through a proper mechanical redesign. However, minimal widths are imposed by punching, while the pole flux decreases linearly with increasing pole number. Therefore, the chosen pole number is derived from some tentative designs of the reluctance structure [75].

A meaningful comparison between different pole numbers is achievable if the subjective machines are set-up in equivalent conditions. The first step is the rotor geometry optimization. The number of barriers is set to 4 that is a maximum practical value, see Figures 6.6, 6.7 and 8.1. The optimized machine design procedure for SynRM rotor design in chapter 9 on page 149 is used, for the 4-pole machine the end point adjusted procedure is also applied. The optimization current is increased by 30 % compared to the correspondent IM 15kWM machine. The current angle is also set to 45° during optimization, except for the 8-pole which is set to around 30°, see also the effect of electrical parameters on performance in section 6.4 on page 116. The inner diameter, outer diameter and shaft diameter are also set to the correspondent IM to keep the stator structures unchanged. However, the airgap length and machine active length are set to the 4-pole correspondent IM values. The airgap length is practically determined and set from the knowledge of the active length of the machine and the maximum operating speed. The speed range considered here for VSD application is 0 – 6000 rpm. Certainly, radial ribs also have to be implemented if these machines are to be practically tested over the entire speed range. Based on the discussion in this section and literature review it is evident that the effect of ribs is significant. Therefore, for more realistic results a 1 mm tangential rib is applied to all barriers in all rotor structures, however, the
radial ribs are disregarded.

The second step is the calculation of performance of the different machines for the above speed range. This purpose is realized using a specific calculation sheet, which scales the machine dimensions from a base IM machine. Iron losses and machine flux density and load angle are calculated by the Finite Element Method (FEM) using Flux2D and have been calibrated with measurements in chapter 5. The machine temperature rise and stator resistance are also estimated in this sheet by using Broström’s formula and three iterations [58] simultaneously using the stator resistance at 20°C and temperature rise factor $k_{cs}$ of the stator. In these calculations the temperature rise factor $k_{cs}$ is also scaled for the new stator of each machine by active length and outer and inner diameter and total copper area in each stator slot.

The stator winding temperature rise, $\Delta T$ in (K) is related to the machine’s total loss, $P_{\text{total}}^{\text{losses}}$ and stator copper loss, $P_{\text{copper}}^{\text{losses}}$ by temperature rise geometrical factor $k_{cs}$ based on Broström’s idea, according to Equation (10.1).

$$\Delta T = k_{cs} \cdot \sqrt{P_{\text{copper}}^{\text{losses}} \cdot P_{\text{total}}^{\text{losses}}}$$  \hspace{1cm} (10.1)

According to [77] a more detailed model is required to obtain accurate results. Equation (10.1) can be further modified by including the machine speed and three coefficients $x$, $y$ and $z$ for $P_{\text{copper}}^{\text{losses}}$, $P_{\text{total}}^{\text{losses}}$ and speed $n$, respectively according to Equation (10.2).

$$\Delta T = k_{cs} \cdot \sqrt{(P_{\text{copper}}^{\text{losses}})^x \cdot (P_{\text{total}}^{\text{losses}})^y \cdot n^z}$$  \hspace{1cm} (10.2)

The temperature rise estimation technique, based on Equation (10.1), show acceptable results when they are compared with measurements, so the measurements reported in section 5.4 on page 96, see Table 5.1, are used for calibration and comparison in this study. All calculations in this sheet have been done based on the SynRM vector model that has been developed in chapter 2 on page 21.

### 10.2 Design and optimization

The technique of following the natural flux lines inside the solid rotor, was used for designing the rotor for different pole number machines, for a detailed description of the design procedure refer to chapter 9.

The detailed design steps are demonstrated in Figure 10.2. Firstly, torque ripple was minimized by the rotor slot pitch angle controller, $\beta$, see Figure 10.2 (left), then the insulation ratio in the q-axis, $k_{wq}$, was optimized for maximum torque, see Figure 10.2 (right). As is shown in Figure 10.2 a high performance rotor geometry can be designed by sensitivity analysis of less than 10 different geometries by FEM. The final optimized rotors for different pole number machines are shown in Figure 10.1.
10.3 Performance comparison

There are some ways to compare the performances of machines with different pole numbers. Here two different comparisons are presented. Constant torque condition for a wide speed range and constant temperature rise condition at a certain speed. The machines performance with the same winding structure, speed and torque is also calculated to compare the effect of pole number on d- and q-axis inductances.
10.3. PERFORMANCE COMPARISON

Figure 10.2: Optimization steps of rotor geometry for different pole numbers. (left) Torque ripple minimization using rotor slot pitch controller, $\beta$, and (right) torque maximization by insulation ratio in the q-axis, $k_{wq}$, for 2- to 8-pole machines from top to bottom. $I_{\text{SynRM}}^s = 1.3 \times I_{IM}^s$ and current angle $45^\circ$. Note that the torque values in 8-pole machine is given in Nm and for other machines in Nm/2p.
10.3.1 Constant torque condition

The shaft output torque is set to a constant value at 1500 rpm for different pole number machines, this torque is equal to the nominal 4-pole machine shaft torque, see Table 10.1. The reluctance machine current in this condition is 30% higher than the corresponding IM.

As can be noted from Figure 10.3 (top-left) the shaft torque in the different machines is changing with speed slightly, because the compensation of losses specially iron losses has not been implemented for simplicity. But this issue does not affect the generality of the results. Torque decreases with increasing pole number as the speed increases, see torque graph in Figure 10.3.

The simulation results are summarized in Figure 10.3. As can be seen from the figure the 4-pole machine has the highest efficiency over the entire speed range, and the 6-pole machine closely follows the 4-pole machine. Power factor is also maximum for 4-pole machine except at low speeds where the 2-pole machine is less reactive compared to the 4-pole machine. Normally, increasing the pole number reduces the power factor due to the negative effect of the magnetizing current that is needed for higher pole number machines [76].

The multiplication of efficiency and power factor of the 4-pole machine is also maximum and the temperature rise of this machine over the entire speed range is minimum. If the temperature rise is limited to 120 $K$ then the machine speed can be increased up to 3500 rpm and for the 6-pole machine up to 2500 rpm. The 2- and 8-pole machines temperature rise are not acceptable for this shaft output torque condition.

The effect of pole number on machine performance can be summarized by a factor that is the product of power factor and efficiency divided by temperature rise, this factor for the 4-pole machine is also maximum for the whole speed range. The 6-pole machine closely follows the 4-pole machine, however the drawback of poor power factor affects the 6-pole machine performance.

In summary, the 4-pole machine has the best performance over a wide speed range without field weakening if the existing stator, 15kWM machine, of the corresponding IM is going to be used for SynRM. Optimum performance of the 4-pole machine can be explained through Figure 10.4 and Table 10.1. Figure 10.4 shows the flux density distribution and flux lines contour at 1500 rpm and constant shaft torque, 134.5 $Nm$ for different pole number machines.

A first survey of the flux density distribution in both the rotor and the stator, shows that the iron material is optimally utilized in the 4-pole machine. The hot spot magnetic areas (regions with very low or very high flux density) for the 4-pole machine are less than the other geometries. A large area of the stator back and inside the rotor segment is saturated in the 2-pole machine, while this phenomenon is not evident in the other pole number machines. However, the numbers of low flux density areas increase with higher number of poles both in the rotor and the stator compared to the 4-pole machine.

Table 10.1 shows data for the machines performance at 1500 rpm and constant
Figure 10.3: Different pole number machines, see Figure 10.1, simulated performances over a wide range of speed and below base speed (without field weakening) at almost constant torque. All calculations have been calibrated with the 4-pole machine measurement in chapter 5 reported in section 5.4 on page 96, see Table 5.1.
Figure 10.4: (left) Flux density distribution in different pole number machines and (right) flux line contour, at 1500 rpm and output shaft torque of 134.5 Nm. Color bar corresponds to 0 – 3 $T$ for colors from dark blue to yellow, respectively.
### Table 10.1: Calculation sheet at constant torque and 1500 rpm with the same winding structure for different pole numbers.

<table>
<thead>
<tr>
<th>Pole number</th>
<th>2</th>
<th>4</th>
<th>6</th>
<th>8</th>
<th>10</th>
</tr>
</thead>
<tbody>
<tr>
<td>Speed</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>n</td>
<td>1500</td>
<td>1500</td>
<td>1500</td>
<td>1500</td>
</tr>
<tr>
<td>Flux density, peak, FEA</td>
<td>B1p</td>
<td>0.77</td>
<td>1.07</td>
<td>1.06</td>
<td>1.00</td>
</tr>
<tr>
<td>Flux density angle, FEA</td>
<td>Delta</td>
<td>21.40</td>
<td>10.24</td>
<td>12.26</td>
<td>13.62</td>
</tr>
<tr>
<td>Active length stator</td>
<td>L</td>
<td>205</td>
<td>205</td>
<td>205</td>
<td>205</td>
</tr>
<tr>
<td>Number of cond. per slot</td>
<td>nS</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>Winding connection factor</td>
<td>cs</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Electrical frequency</td>
<td>f</td>
<td>25</td>
<td>50</td>
<td>75</td>
<td>100</td>
</tr>
<tr>
<td>Phase current, rms, airgap</td>
<td>I</td>
<td>78.2</td>
<td>50.0</td>
<td>55.1</td>
<td>67.9</td>
</tr>
<tr>
<td>Main inductance, d</td>
<td>Lmd</td>
<td>27.05</td>
<td>24.08</td>
<td>12.88</td>
<td>6.37</td>
</tr>
<tr>
<td>Leakage inductance, d</td>
<td>Lsld</td>
<td>1.56</td>
<td>1.09</td>
<td>0.98</td>
<td>0.94</td>
</tr>
<tr>
<td>Main inductance, q</td>
<td>Lmq</td>
<td>3.86</td>
<td>2.41</td>
<td>1.82</td>
<td>1.21</td>
</tr>
<tr>
<td>Leakage inductance, q</td>
<td>Lsllq</td>
<td>1.56</td>
<td>1.09</td>
<td>0.98</td>
<td>0.94</td>
</tr>
<tr>
<td>Temp. rise factor (fan speed = const.)</td>
<td>kcs</td>
<td>0.0826</td>
<td>0.0718</td>
<td>0.0788</td>
<td>0.0816</td>
</tr>
<tr>
<td>Phase resistance, Y</td>
<td>Rs</td>
<td>0.2750</td>
<td>0.1318</td>
<td>0.1207</td>
<td>0.1990</td>
</tr>
<tr>
<td>Inductance difference, airgap</td>
<td>Lmd - Lmq</td>
<td>23.19</td>
<td>21.67</td>
<td>11.06</td>
<td>5.16</td>
</tr>
<tr>
<td>Saliency ratio, airgap</td>
<td>Lmd / Lmq</td>
<td>7.0</td>
<td>10.0</td>
<td>7.1</td>
<td>5.3</td>
</tr>
<tr>
<td>Equivalent Pfe losses resistor</td>
<td>Rs</td>
<td>0.2750</td>
<td>0.1318</td>
<td>0.1207</td>
<td>0.1990</td>
</tr>
<tr>
<td>Iron losses current, d, peak</td>
<td>icd</td>
<td>-0.385</td>
<td>-0.191</td>
<td>-0.255</td>
<td>-0.393</td>
</tr>
<tr>
<td>Iron losses current, q, peak</td>
<td>icq</td>
<td>0.983</td>
<td>1.059</td>
<td>1.172</td>
<td>1.260</td>
</tr>
<tr>
<td>Stator terminal current angle</td>
<td>Theta</td>
<td>70.4</td>
<td>61.5</td>
<td>57.6</td>
<td>52.8</td>
</tr>
<tr>
<td>Current, phase, rms, Y</td>
<td>Is</td>
<td>78.8</td>
<td>50.6</td>
<td>55.7</td>
<td>68.6</td>
</tr>
<tr>
<td>Voltage, L-L, rms</td>
<td>VLL</td>
<td>261.6</td>
<td>350.9</td>
<td>363.0</td>
<td>368.5</td>
</tr>
<tr>
<td>Required DC voltage</td>
<td>Udcreq</td>
<td>336</td>
<td>450</td>
<td>466</td>
<td>473</td>
</tr>
<tr>
<td>Resistance voltage drop</td>
<td>ur</td>
<td>30.6</td>
<td>9.4</td>
<td>9.5</td>
<td>19.3</td>
</tr>
<tr>
<td>Reactance voltage drop</td>
<td>ux</td>
<td>191.6</td>
<td>279.5</td>
<td>290.1</td>
<td>290.1</td>
</tr>
<tr>
<td>Copper losses</td>
<td>Pcu</td>
<td>5117</td>
<td>1012</td>
<td>1122</td>
<td>2813</td>
</tr>
<tr>
<td>Iron losses, calc. FEM</td>
<td>PfeFEM</td>
<td>273</td>
<td>425</td>
<td>474</td>
<td>609</td>
</tr>
<tr>
<td>Friction losses</td>
<td>Pf friction</td>
<td>66</td>
<td>95</td>
<td>59</td>
<td>59</td>
</tr>
<tr>
<td>Total losses</td>
<td>Pf total</td>
<td>5456</td>
<td>1533</td>
<td>1655</td>
<td>3480</td>
</tr>
<tr>
<td>Shaft torque</td>
<td>Tout</td>
<td>134.5</td>
<td>134.5</td>
<td>134.6</td>
<td>134.3</td>
</tr>
<tr>
<td>Mechanical power</td>
<td>Pout</td>
<td>21.1</td>
<td>21.1</td>
<td>21.1</td>
<td>21.1</td>
</tr>
<tr>
<td>Electrical power</td>
<td>Pin</td>
<td>26.6</td>
<td>22.7</td>
<td>22.8</td>
<td>24.6</td>
</tr>
<tr>
<td>Apparent power</td>
<td>S</td>
<td>35.7</td>
<td>30.7</td>
<td>35.0</td>
<td>43.8</td>
</tr>
<tr>
<td>Power factor</td>
<td>PF</td>
<td>0.745</td>
<td>0.737</td>
<td>0.651</td>
<td>0.561</td>
</tr>
<tr>
<td>Temperature rise</td>
<td>DeltaT</td>
<td>385</td>
<td>85</td>
<td>103</td>
<td>237</td>
</tr>
<tr>
<td>Efficiency</td>
<td>eff</td>
<td>79.5</td>
<td>93.2</td>
<td>92.7</td>
<td>85.8</td>
</tr>
<tr>
<td>Efficiency x PF</td>
<td>eff x PF</td>
<td>0.592</td>
<td>0.687</td>
<td>0.604</td>
<td>0.482</td>
</tr>
<tr>
<td>Efficiency x PF / Temp. Rise</td>
<td>eff x PF / dT</td>
<td>1.54</td>
<td>8.11</td>
<td>5.87</td>
<td>2.03</td>
</tr>
</tbody>
</table>

The table compares the performance characteristics of different pole numbers at a constant torque and 1500 rpm with the same winding structure. The d-axis inductance is reduced by increasing the pole number, as expected. The q-axis inductance follows the same trend. Highly saturated areas in the 2-pole machine force the MTPA current angle to increase in comparison to the other pole numbers.

However, the flux density for the 2-pole machine cannot reach the optimum value which is 1 T for this size, see the airgap flux densities of the other pole number machines in Table 10.1. Therefore, the current has to be increased for compensation. Note that the shaft torque of all machines is equal. Consequently,
temperature rise increases and also the stator resistance. This causes very high ohmic losses in the 2-pole machine compare to the other pole numbers. High copper losses justify higher machine power factor for the low frequency and speed conditions in comparison to the 4-pole machine, see Figure 10.3 for this conditions and speeds lower than 2000 rpm.

The q-axis flux cannot be optimally blocked in 2-pole machine due to the difficulties of arranging barriers, therefore $L_q$ increases for this pole number. Observe in Figure 10.4 on how the main pole flux crosses the first barrier in the 2-pole machine which does not take place in the other pole number machines, see marked area with a black circle in Figure 10.4. This affects the machine saliency ratio, see Table 10.1. In higher pole number machines, more than 4, the drawback of the lower magnetizing inductance and higher required q-axis flux due to the reduction in the d-axis inductance is quite evident in Table 10.1, these drawbacks also affect the saliency and power factor of these machines.

Even if factor $(p/2) \cdot (L_d - L_q)$ is considered, the 4-pole machine has the highest value of this factor. The change in this factor is 23, 42, 33 and 20 mH when the pole number increases from 2 to 8 respectively, refer to Table 10.1.

All these facts, explain why the 4-pole machine can produce the same shaft torque with minimum stator current and consequently lower temperature rise and maximum power factor, or maximum efficiency and minimum inverter rating, see Table 10.1. Very close values of efficiency for the 4- and 6-pole machines show the drawback of the tangential ribs. Greater disadvantages are also evident for the 6-pole machine power factor, in comparison to the 4-pole machine, due to the introduction of these ribs.

10.3.2 Constant temperature rise condition

The advantages of the 4-pole machine compared to the other pole number machine can be investigated if the temperature rise of all machines, here 85 K, is set equal at constant speed, here 1500 rpm. Such comparison is demonstrated in Figure 10.5. The corresponding 4-pole IM performance is also added to this figure for comparison. The advantages of the SynRM 4-pole machine is evident in this figure. Higher output power, efficiency and shaft torque are the main advantages of the 4-pole machine in comparison to the other machines including the IM. The power factor of the 4-pole machine, of course, is lower compared to the 2-pole machine. This issue was discussed in the earlier sub-section. The power factor is also lower in comparison to the IM, because the q-axis flux of the SynRM affects the power factor and reduces the power factor to a value that is even lower than in the IM.

10.4 Final comments

The 4-pole structure is the best choice for the SynRM in variable speed drives (VSD) application, if the existing stator, $15kW$ machine, of the corresponding
IM is going to be used for the SynRM, speed range 0—6000 rpm, because utilization of the magnetic material for this pole number is optimum.

Increasing the pole number increases both the torque and required q-axis flux. Therefore, torque density is increased and at the same time the power factor is reduced. This is due to a trade-off between $L_d$ and $L_q$ inductances. Few poles do not allow an effective shaping of the q-axis barriers, while too many poles reduce the pole span and consequently the d-axis inductance $L_d$.

Ribs, that are required for mechanical stability, also affect the performance. The increase in torque for higher pole numbers can be canceled out by adding ribs when the pole number is increased. However, another drawback of the ribs is their effect on the q-axis flux, which further contributes negatively on power factor in higher pole number machines.

Therefore, low pole number is suggested. However, creating a good anisotropic rotor structure in a 2-pole machine is a difficult task, due to the high pole flux path length in both the rotor and the stator and also the arrangement of the barriers. Furthermore, the power factor is reduced if the pole number is increased, for pole number higher than 2.
Chapter 11

Secondary effects in SynRM

One of the most important secondary effect in electrical machines, torque ripple, is discussed in chapter 9, specifically in Synchronous Reluctance machines (SynRM). Other secondary effects are briefly pointed out. In this chapter some of the most important of these effects are studied.

Skew and torque quality, are other important issues in SynRM. A simplified method of studying the effects of skewing on torque ripple is discussed. The best skewing angle and steps are investigated to reduce the torque ripple. A Matlab code is developed to calculate the effects of skewing.

The finite element method (FEM) aided optimization of the SynRM, see chapters 6, 7, 8 and 9, is based on a sinusoidal current source supply. The possible effects of alternative source supplies on torque and iron losses is studied. Due to the induced space harmonics, the choice of the rotor slot pitch will affect the machine torque ripple. In SynRM the flux density space harmonics not only affect the rotor surface but also the entire segments deep inside the rotor and consequently, increases the iron losses in the rotor.

The purpose of this chapter is also to present the start-up and short-circuit locked rotor tests performed on the standard IM and the prototype SynRM.

Tolerances are inherent in the production of electrical machines. An important effect is that the airgap of the machine is not uniform and due to the off-center rotor or stator or both, the airgap is no longer constant around the rotor periphery. The effect of such eccentricity is studied and presented in this chapter as well.

The phase number variation of SynRM and IM provides an opportunity in multi-motor (SynRM + IM) drive systems, e.g. in traction applications [132], to control each machine independently. Increasing or reduction of the machine phase number can affect both the system cost as well as machine performance, e.g. torque ripple. However, this issue is out of the scope of this thesis.
11.1 Optimum skew

A theoretical treatment of SynRM torque ripple and skew is presented in [3] and [37]. Also some design techniques for ripple reduction is discussed in [43] and [55], see chapter 7, as well. Literature study shows that skewing with almost one stator slot pitch will reduce the torque ripple by a factor $3 - 4$ in comparison to the unskewed machine [78]. Study and measurements, reported in [38], show that the effect of skew on the iron losses in the machines is almost negligible.

The effect of the skew on SynRM’s performance will be discussed in this section by first developing a simple method for skew effect evaluation and then analyzing the torque quality of the skewed machine and comparing the results with the unskewed alternative. A sensitivity analysis of the number of the skew steps and the skew angle is done to determine a suitable skew for the improved machine design of the SynRM, whose performance is discussed in chapters 8 and 9, see Figure 8.4 (d).

11.1.1 Optimal skew angle and skewing steps analysis

Based on technique that is explained in [58] for evaluation of the skew effect, a simple 2D simulation based procedure for calculating the effect of the skewing angle and skewing steps on torque ripple, is developed.

In this procedure the machine is divided into $n$ (number of the steps) sub-machines and the base unskewed machine torque is shifted in time proportional to

![Figure 11.1: Torque ripple vs. skewing angle and skewing steps for the improved machine design SynRM, see its rotor geometry in chapter 8 and Figure 8.4 (d), 15kWM machine.](image-url)
its space shift from the base sub-machine that is the unskewed one. Finally, the resultant torque is calculated as an average of the all sub-machine torques. By this procedure, the effect of the axial flux and the current angle shifting of each machine compared to the unskewed machine are disregarded.

The simulation result for the improved machine design SynRM is shown in Figure 11.1, using this method. In this graph the machine torque ripple (in %) is drawn for different skewing angle and different number of skewing steps. The unskewed machine ripple is 27.6% and with 10° skewing in minimum 4 and maximum continuously or 340 steps, which is equal to machine length \(L\) divided by the lamination thickness \(340 = 170 \text{ mm} / 0.5 \text{ mm}\), it falls to less than 9.5%.

The suitable skewing angle here is 10° which is equal to one stator slot pitch in this machine with \(Q_s = 36\) and \(2p = 4\), and is compatible with the results presented in the literature.

### 11.1.2 Torque and ripple comparison before and after skew

The torque curves of a skewed and unskewed machines vs. time for one electrical period are shown in Figure 11.2. As is explained in [16], [37], due to the interaction between d- and q-axis fluxes, skew does not eliminate the ripple completely and further ripple reduction requires an investigation on other possible measures such as the airgap permeance modulation capability [55]. The stator’s structure also affects
the machine’s torque ripple.

11.1.3 Comparison of different skewing calculation techniques

Torque ripple calculation in the last sub-sections is based on two assumptions. Firstly, the axial flux and the airgap flux distortion are disregarded in the analysis and secondly, the current angle displacement for different sub-machines is also disregarded. The first assumption can be considered by 3D simulation, which is out of the scope of this chapter, however, the second assumption can be exactly considered by calculating the torque for each sub machine by means of the FEM calculation tool.

Due to the high number of sub-machines in continuous skewing, assumed to be 340 in this simulation, the number of skewing steps is limited to 10 (10 sub-machines). This assumption does not affect the result significantly, because the minimum number of the steps is around 4, see Figure 11.1. Increasing the number of steps higher than 4 has negligible effect on the ripple. The effect of skewing, $10^\circ$ in 10 steps, using different calculation techniques are shown in Figure 11.3.

Direct calculation technique using FEM-Flux2D software, uses ten different angles starting from $-5$ to $+5$ degrees increasing in steps of $+1^\circ$ assuming the same current angle, that is here $60^\circ$. The first geometry is shown in Figure 11.4 (left) at $-5^\circ$ and the last one is shown in Figure 11.4 (right) at $+5^\circ$. In the Matlab calculation the torque corresponding to the geometry with starting angle $0^\circ$ is used and the torque of the other machines is just obtained by shifting this curve by $+1^\circ$ in

![Comparison of different skewing technique](image)

Figure 11.3: Comparison of different techniques to calculate the effect of skew on torque and torque ripple for the improved machine design SynRM with $10^\circ$ skewing in 10 steps.
11.2 Supply effect

The synchronous reluctance machine (SynRM) finite element method (FEM) aided optimization in chapter 9, and related sensitivity analysis in chapters 4, 6, 7, 8, 9, 10 and [16] were based on having a sinusoidal current source supply. This means that in the simulation a sinusoidal phase current is set in each winding in the

10 steps. In the calculation of average torque the skewing factor \( \frac{\sin (\alpha/2)}{(\alpha/2)} \) [58] is also considered. In this equation \( \alpha \) is the total skewing angle in electrical degrees.

The estimated torque ripple for the improved machine design SynRM after skewing by direct FEM-Flux2D and Matlab code are very close, 9.37% in comparison to 9.24%, respectively. However, the average torque from the FEM-Flux2D calculation is 24.07 \( Nm/2p \), which is lower than the Matlab calculation which is 24.47 \( Nm/2p \), because the current angles for the different sub-machines are different in reality and not the same as assumed in the FEM simulations.

It seems that if the axial flux effect and the airgap flux distortion are neglected, the torque shifting technique used for torque ripple calculation for skewed rotor (Matlab calculation) is sufficiently accurate in this case. Finally, some real time torque vs. time curves for different skewing angles and continuous skewed rotor (Matlab calc.) are shown in Figure 11.5. The sensitivity of the torque calculations for steps in skewing, from 1 step to 340 steps (continuous) and skewing angle of 10°, are also shown in Figure 11.6 for the improved machine design SynRM machine at nominal current and 3000 rpm.

11.2 Supply effect

The synchronous reluctance machine (SynRM) finite element method (FEM) aided optimization in chapter 9, and related sensitivity analysis in chapters 4, 6, 7, 8, 9, 10 and [16] were based on having a sinusoidal current source supply. This means that in the simulation a sinusoidal phase current is set in each winding in the
Figure 11.5: Torque vs. time before and after skewing for one electrical period, sensitivity analysis of the skewing angle.
11.2. SUPPLY EFFECT

Figure 11.6: Torque vs. time before and after skewing for one electrical period, skewing angle $10^\circ$, sensitivity analysis of the skewing steps.
11.2.1 SynRM synchronization with voltage source

Simulation of a SynRM supplied from a voltage source is different from simulation with a current source supply. Torque will be developed in the machine if the corresponding steady-state phase current is flowing in the windings of the machine. If a current source is used for the simulation, then the steady state current can be directly set in the model. However, if a voltage source is used there will be a transient behavior due to the transition period of the back EMF which has to build up to its steady state value.

The transient behavior of the machine during synchronization with a voltage source is simulated using the improved machine design SynRM, see chapter 8. The voltage values and angles are estimated by using the calculation sheet data for terminal voltage of the machine in nominal conditions, $1500 \text{ rpm}$ and $15 \text{ kW}$, and performing the iterations. The resultant instantaneous torque vs. time is shown in Figure 11.8, blue curve. Due to the poor damping the stator resistance has been set to $1 \text{ Ω/phase}$ instead of the actual value of $0.193 \text{ Ω/phase}$. In this condition the rotor speed is set to $1500 \text{ rpm}$ and is kept constant all along the simulation.

The voltage source for each phase is connected to the machine at $t = 0$, $0.01 \text{ s}$ by activating suitable switches in the electrical circuit shown in Figure 11.7 (left).

Due to the elimination of the mechanical dynamic from the simulations which is achieved by keeping the rotor speed constant, the transient is very fast and only shows the electrical transition during synchronization. In reality it is like having a flywheel on the shaft and at nominal load. Then, by rotating the shaft at nominal

![Figure 11.7](image-url)
11.2. SUPPLY EFFECT

The machine is run in VSD mode by an inverter up to the desired operation condition, then the machine terminal voltage under VSD supply is synchronized with the sinusoidal voltage grid and finally at a suitable moment the machine supply is switched from VSD inverter to the grid. A mathematical explanation of the transients that normally follow is that the switching over to the voltage source should take place when the magnetic and electric operating conditions in the iron and in the current respectively are exactly the same as their values at steady-state. Otherwise there will be an electrical transient as we can see in Figure 11.8, blue curve.

To reduce the transients in the simulation a current supply is used before connecting the machine to the voltage source for a similar method implementation. For this purpose the machine is first supplied with the current source, the current angle and amplitude are taken from the calculation, and then the supply is changed to the voltage source as before at $t = 0.01$ s. The simulated torque in this condition is shown in Figure 11.8, pink curve, the phase resistance is set to its correct value.

Figure 11.8: Torque per pole vs. time transient behavior under different synchronization simulation methods at nominal conditions, 1500 rpm and 15 kW.
of 0, 193 Ω/phase here. As is shown in the figure the transient torque overshoot is reduced, which shows that the machine is well synchronized before and after the voltage supply connection.

To reduce the transient duration time another scenario can be used as follows: First, the machine is run with a current supply, then the supply is switched to a voltage source at \( t = 0,01 \) s together with a high resistance of 1 Ω/phase, till \( t = 0,025 \) s. At this instance the resistance is changed to the correct value of 0,193 Ω/phase. The resultant torque transient is shown by the red curve in Figure 11.8. As can be seen in the simulations, the changing resistance causes again a transient in the machine torque. Therefore, using a damper resistance does not entirely solve the problem.

In order to eliminate the transient the current and voltage sources have to be completely synchronized. For this purpose the machine fundamental current is calculated under steady state voltage supply and these values of current and current angles are set in the current supply. The machine resistance is set to 1 Ω/phase. The simulated machine torque in this condition is shown in Figure 11.8, green curve. As can be seen from this curve the transient condition is almost eliminated in this condition.

### 11.2.2 Effect of voltage and current sinusoidal sources on iron losses

The voltage and current supply sources were synchronized in the last sub-section. The iron losses under current and voltage sinusoidal supply conditions are calculated and shown in Table 11.1. If the current and voltage supplies are completely synchronized then there is negligible change in the iron losses when switching from

<table>
<thead>
<tr>
<th>Supply Type</th>
<th>I-Source</th>
<th>V-Source</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotor Eddy [W]</td>
<td>55,6</td>
<td>55,0</td>
</tr>
<tr>
<td>Rotor Hysteresis [W]</td>
<td>0,0</td>
<td>0,0</td>
</tr>
<tr>
<td>Rotor Excess [W]</td>
<td>30,3</td>
<td>29,9</td>
</tr>
<tr>
<td>Rotor Total Iron Losses [W]</td>
<td>86,0</td>
<td>84,9</td>
</tr>
<tr>
<td>Stator-Back Eddy [W]</td>
<td>33,5</td>
<td>32,0</td>
</tr>
<tr>
<td>Stator-Back Hysteresis [W]</td>
<td>68,9</td>
<td>68,9</td>
</tr>
<tr>
<td>Stator-Back Excess [W]</td>
<td>33,8</td>
<td>32,5</td>
</tr>
<tr>
<td>Stator-Back Total Iron Losses [W]</td>
<td>136,3</td>
<td>133,4</td>
</tr>
<tr>
<td>Stator-Teeth Eddy [W]</td>
<td>49,3</td>
<td>48,6</td>
</tr>
<tr>
<td>Stator-Teeth Hysteresis [W]</td>
<td>44,1</td>
<td>44,1</td>
</tr>
<tr>
<td>Stator-Teeth Excess [W]</td>
<td>34,0</td>
<td>33,7</td>
</tr>
<tr>
<td>Stator-Teeth Total Iron Losses [W]</td>
<td>127,5</td>
<td>126,4</td>
</tr>
<tr>
<td>Total Eddy [W]</td>
<td>138,5</td>
<td>135,5</td>
</tr>
<tr>
<td>Total Hysteresis [W]</td>
<td>113,1</td>
<td>113,1</td>
</tr>
<tr>
<td>Total Excess [W]</td>
<td>98,2</td>
<td>96,1</td>
</tr>
<tr>
<td>Total Iron Losses [W]</td>
<td>349,7</td>
<td>344,7</td>
</tr>
</tbody>
</table>

Table 11.1: Iron losses under voltage and current synchronized supplies at rated conditions, 15kW M machine.
Figure 11.9: Machine torque, phase current and back EMF vs. time under voltage and current synchronized supplies at rated conditions, 1500 rpm and 15 kW, 15kWm machine.
Figure 11.10: Machine stator back, stator teeth and rotor iron losses vs. time under voltage and current synchronized supplies at rated conditions, 1500 rpm and 15 kW, 15kWM machine.
11.3 Flux variation inside the rotor segments and iron losses

Due to the induced space harmonics, the rotor slot pitch affects the machine torque ripple. There is a strong tie between torque ripple and the airgap flux density harmonics [66], [67] and [79]. In SynRM the flux density harmonics not only affect the rotor surface area but also the entire iron of the flux segments deep inside the rotor. Both of these effects increase the iron losses in the rotor and stator. This issue will be examined in this section.

The interaction between the rotor and stator slot pitch has three extreme states. The first is when the rotor slot pitch is lower than the stator slot pitch. In this condition the stator slot has the possibility to block each rotor segment when the a current source to a voltage source, as is shown in Table 11.1. The airgap torque, current and winding back-EMF variation versus time when switching from current to voltage source are shown in Figure 11.9.

The airgap torque has a small transient due to the incomplete synchronization of the voltage and current supplies. The current and torque in the voltage supply condition are very close to the current and torque in the sinusoidal current supply condition. This justifies the current source selection for machine optimization in chapters 4, 6, 7, 8, 9, 10 and [16].

Phase current under voltage supply still has a very low harmonic content due to the back-EMF variation, see Figure 11.9 (bottom) and consequently also in the voltage drop variation over the stator resistance and the winding leakage. Iron losses in the different regions; rotor, stator back and teeth, versus time have been calculated and are shown in Figure 11.10. Switching from current to voltage supply does not have any significant impact on the average iron losses, especially in the rotor, but reduces the instantaneous iron losses in the stator, see Figure 11.10 (top and middle).

This is not the case for the rotor and as is shown in Figure 11.10 (bottom), even the instantaneous variation of iron losses is the same before and after the switching, because the airgap magnetization inductances are filtering the time harmonics of the supply current so that the variation of flux in the rotor from these harmonics is negligible. Therefore, it can be concluded that the time harmonics in the supply mainly affect the stator and has very little effect on the rotor flux density variation and consequently iron losses in that region. This is due to the high filtering capability of airgap magnetizing inductances as long as the current has a low harmonic content in this condition.

Simulation also shows that supply source type does not have any effect on the calibration factors that have been used for the torque and the iron losses calculations as long as sinusoidal supply is used in the simulations. The time harmonic effect on the machine performance and losses under PWM-VSD supply has been studied earlier and reported in [82] and [83].
segment passes in front of the slot, thus, the iron losses in the rotor will be mainly affected by the space harmonics. There is a specific condition when the rotor slot pitch is half of the stator one. In this condition the flux variation from the airgap will not have any effect deep into the rotor iron because the permeances at the two ends of the segment will compensate each other’s variation [38]. The second state is when the slot pitches of the rotor and stator are equal. In this condition the iron losses are expected to be low [38]. The third is when the rotor slot pitch is larger than the stator slot pitch. This situation is similar to the first condition but the flux variation shifts from the rotor to the stator and mainly to the stator teeth and increases these losses [38].

The flux variation in the rotor segments will be simulated with FEM and its close relation to the iron losses will be shown in this section by implementing suitable search coils in the rotor segment as is shown in Figure 11.7 (right). Relatively high iron losses in the SynRM rotor have been qualitatively discussed in [16] and it has been related to the high flux density variation deep inside the rotor and in the conducting iron segments, see also [38].

The calculation of flux variation inside the rotor and at a point inside the rotor is not a straightforward task in Flux2D software, because the rotor is rotating. To solve this problem semi-measurement conditions are simulated in the model. For this purpose two identical search coils are implemented in the model, one perpendicular to, ORT winding, and one parallel with, PARA winding, the segment inside the rotor as is shown in Figure 11.11 (right). Then, these coils are connected through an external circuit to a current source with zero current as is shown in Figure 11.11 (left).

The open circuit voltage of these coils, \( v_{ORT}(t) \) and \( v_{PARA}(t) \), respectively, which are also voltages across the current sources \( i_{ORT} \) and \( i_{PARA} \), are directly connected to the flux linkage and flux density variation in each coil according to Equations (11.1) and (11.2).

\[
\begin{align*}
\lambda_{ORT}(t) &= \int_0^t v_{ORT}(t) \cdot dt + c_1, \\
\lambda_{PARA}(t) &= \int_0^t v_{PARA}(t) \cdot dt + c_2, \\
B_{ORT}(t) &= \frac{\lambda_{ORT}(t)}{N \cdot A}, \\
B_{PARA}(t) &= \frac{\lambda_{PARA}(t)}{N \cdot A}. 
\end{align*}
\]  

In Equations (11.1) and (11.2), \( N \) is the number of turns per coil which is one here, \( A \) is the coil area, which is \( L \times d = 170 \times 12,467 \text{ mm}^2 \), where \( L \) is the
11.3. **FLUX VARIATION INSIDE THE ROTOR SEGMENTS AND IRON LOSSES**

![Diagram of electrical circuit and coils connection]

Figure 11.11: *(left)* Electrical circuit that is implemented in FEM-Flux2D software, *(right)* coils connection for the calculation of flux density variation in the segment inside the rotor.

Machine length and \( d \) is the segment width at the position of the orthogonal coil. \( c_1 \) and \( c_2 \) parameters are related to the mean value of the flux linkage and flux density of the search coils, and are here set to zero.

To solve Equations (11.1) and (11.2) a simulink model has been used. In this model the open circuit voltage of the orthogonal and parallel search coils are integrated, according to Equation (11.1) and then by considering the geometry of the coil the flux density variation in each coil according to Equation (11.2) has been calculated. The open circuit voltage of the search coils vs. time and the same conditions as Figures 11.9 and 11.10, have been calculated and are shown in Figure 11.12 (top). Both search coils have exactly the same structure and the only difference between them is their position in the segment of the rotor, see Figures 11.7 and 11.11 (right).

Flux 2D calculation, shows that the orthogonal coil experiences a voltage which is a factor of \( 11 - 12 \) higher than the parallel coil, see Figure 11.12 (top). The flux linkage variation of each coil that has been calculated by implementing Equation (11.1) in a Simulink Model, shows the same behavior as the open circuit voltage, see Figure 11.12 (Middle). The mean values are eliminated from graphs. Similarly, the flux density variation in each direction, orthogonal and parallel with the segment, has been calculated based on Equation (11.2) and is shown in Figure 11.12 (bottom). For comparison, the rotor instantaneous iron losses are also shown in this figure, red curve, which shows a very close correlation between iron losses in the rotor and flux density variation deep inside the iron segment of the rotor body and in the \( d \)-axis flux path direction.

As has been discussed in [38], the flux density inside the rotor not only is not constant as is expected due to the synchronous rotation of the rotor with respect to the stator field, but is also varying inside the rotor due to the interaction of the rotor and stator slots. The peak value of this variation can reach \( 0.24 \, T \) for the investigated SynRM.
Figure 11.12: Open circuit voltage, flux linkage and flux density variation in the orthogonal and parallel search coils vs. time at nominal condition; 1500 rpm and 15 kW, see also Figures 11.10 and 11.11 for 15kW M machine. $dT = 0, 1 \times T$. 

CHAPTER 11. SECONDARY EFFECTS IN SYNRM
11.4 Expected SynRM behavior at start-up and short circuit locked rotor conditions

The SynRM response to abnormal supply conditions is an important practical issue specially regarding the control and protection [80] and [81]. In this section two critical operating conditions of the SynRM are investigated; start-up and locked rotor short circuit conditions.

First, a start-up at no-load is studied in order to investigate the transient behavior of SynRM’s magnetic circuit. For this purpose the start-up of the improved machine design SynRM, 15kWM machine, see its rotor geometry in chapter 8 and Figure 8.4 (d), is simulated with FEM and the machines current and torque are studied. The nominal load condition of this machine is shown in Figure 11.9. Second, the SynRM with the optimized machine design structure, 3kWM machine is prototyped and its locked rotor short circuit current is measured and compared with its counterpart IM. From these investigations, an overview of the behavior of the magnetic structure of the SynRM in transient conditions is obtained and compared with the IM.

In SynRM, due to the absence of the rotor circuit, it is expected that the transient electro-magnetic energy of the machine is lower than the IM and consequently, the machine exhibits a lower transient current and torque, while in the IM the rotor current and the large rotor time constant can increase the transient response time and amplitude.
Figure 11.13: Machine torque, back EMF, phase current vs. time under voltage and current synchronized supplies at almost no-load condition, 1500 rpm and \( \approx 0 \text{ kW} \), 15kW M machine.
11.4. EXPECTED SYNRM BEHAVIOR AT START-UP AND SHORT CIRCUIT LOCKED ROTOR CONDITIONS

11.4.1 SynRM behavior at start-up

The method used is similar to the method explained in section 11.2, where a no-load start-up is simulated at 1500 rpm. Small modifications are made for this purpose, first, the current source is set to zero, and second, the voltage source is adjusted to give almost no torque at the end of the start-up transient.

The simulated torque, back-EMF and current are shown in Figure 11.13. The nominal full load phase current of the machine is around 50 $A_{\text{peak}}$ and the full load torque is around 27 $Nm/2p$, see Figure 11.9. The transient start-up current, is around 150 $A_{\text{peak}}$ and the torque, is around 80 $Nm/2p$, thus both are almost 3 times larger than the full load values. The transient magnetization of the machine lasts less than one period. The energy that is absorbed by the machine during this time is almost equivalent to three times the full load energy in one period.

The start-up current in the case of an IM can reach up to 5 to 8 times the nominal current of the machine because both the magnetic circuit and the electrical circuit of the rotor have to be energized during start-up [33] and [58].

11.4.2 SynRM behavior in short-circuit locked-rotor operation

An IM of the sizes discussed here generally has a cast aluminum cage inside the rotor slots of the iron lamination and in the end rings, while the SynRM has only iron laminations and air slot barriers. The IM does not have a magnetic saliency, since it consists of many symmetrically distributed slots within each pole, while the SynRM does have a strong magnetic saliency, since it has a direct axis of high magnetic conductivity and a quadrature axis of very low magnetic conductivity for each pole. These machine characteristics make the short-circuit current behaviors quite different, despite the fact that the stators for the two rotors are identical.

When an alternating voltage source is suddenly applied to the stator windings of an induction machine at rest, the rotor (and the stator of course) will be exposed to a magnetic field with the network frequency and the rotor cage will react by the induced currents in the conducting bars, which are opposing the applied field. The opposing field in the rotor blocks the stator field so that it will not penetrate the rotor, but will instead mainly be in the vicinity of the rotor surface and will cross the rotor slots before returning back to the stator core to close the magnetic flux path. The rotor reaction results in a low transient inductance with relatively high starting currents.

In the SynRM on the other hand, there is no rotor cage that reacts against the alternating fields from the stator, so the stator winding produced flux penetrates the rotor iron segments creating a long magnetic flux path, with relatively small eddy-current reactions. The transient inductance is therefore higher than for the IM and the short-circuit currents are therefore lower. Moreover, since the SynRM has a strong magnetic saliency, the inductance is rotor position dependent, which means that the magnitude of the short-circuit currents in the different phases is dependent of the winding position in relation to the rotor position in a locked rotor.
Figure 11.14: Machine torque, back EMF and phase current vs. time under voltage and current synchronized supplies at locked-rotor condition, 0 rpm and 0 kW, 15kW M machine.
11.4. EXPECTED SYNRM BEHAVIOR AT START-UP AND SHORT CIRCUIT LOCKED ROTOR CONDITIONS

Test, and they are consequently unsymmetrical.

The no-load start-up simulation that is shown in Figure 11.13 for 15kWM machine, improved machine design SynRM, is repeated but the rotor speed is set to zero in the simulation. This represents the locked rotor condition. The simulation results are shown in Figure 11.14. These results show that the transient locked rotor short circuit current is around 190 $A_{\text{peak}}$ and the torque is around 105 $Nm/2p$, thus both are almost 4 times larger than the full load values. The time span for the transient state is just less than one period.

After the transient state is passed, the short circuit locked rotor current will flow in the machine windings. The maximum steady state current is around 180 $A_{\text{peak}}$ but is not symmetrical. The lowest current is around 60 $A_{\text{peak}}$. The back-EMF and currents are not sinusoidal. A pulsating torque between $-20 Nm/2p$ and $80 Nm/2p$ is also developed in the machine due to the salient structure of the rotor.

11.4.3 Short-circuit locked-rotor tests on IM and SynRM

The purpose of this sub-section is to present the short-circuit locked rotor tests performed on the standard induction motor and the prototype synchronous reluctance machine of type 3kWM machine.

Short-circuiting the induction motor to the sinusoidal voltage supply network is a normal condition that takes place every time the motor is started in direct-on-line (DOL) operation. The starting current rises to a high level which is many times greater than the nominal current, and when the rotor catches up speed and runs almost in synchronism with the stator field of normally 50 Hz with a slip speed of a very few percent, the current settles down to nominal current at rated load. However, in a locked rotor test, the rotor will always be exposed to a 50 Hz magnetic field from the stator, and will consequently have starting conditions continuously and very high steady-state currents. These starting currents are well known to the induction motor manufacturer. The aim with the experiments described in this report is to benchmark the induction motor with the SynRM with regard to the short-circuit currents.

The machine used in the tests is a standard IM and the rotor is also exchanged with a SynRM rotor from the optimized machine design. The same identical stator has been used for all tests. The following steps are taken in the locked rotor tests:

1. The test machine is fastened on the test table.

2. The rotor of the test machine is locked to the table using a lever between the shaft and the table.

3. The winding is connected to the 400 V AC network through a breaker for start and stop.
4. An oscilloscope is connected to the machine terminals in order to study the phase voltages and currents. The oscilloscope is set to trigger on a phase voltage.

5. The breaker is given a start signal, and one second later, a stop signal is given.

6. The oscilloscope results are registered and stored in the computer.

**IM**

The phase voltages and the phase short-circuit currents of the first locked rotor test of the induction motor are shown in Figure 11.15 (top-left). The phase currents are symmetrical and have $\text{rms}$ values of 42 A. The short-circuit current is consequently more than six times greater than the nominal current of 6.6 A.

The results of a second similar test are shown in Figure 11.15 (top-right), which show almost identical values as the first test. The currents in all the three phases are all symmetrical. Steady-state condition is reached within the second period. An induction motor can only survive a test of this nature for a very short time (in seconds or minutes, depending on motor size, material content and design), because especially the rotor joule losses are extremely high when a slip frequency of 100% is permanently present. The stator winding joule losses are also more than 36 times higher than at rated condition, due to the quadratic current dependence.

**SynRM**

After the tests with the induction motor, the induction rotor was exchanged with a synchronous reluctance rotor, and new tests were carried out. Eight locked rotor tests at three different rotor positions were performed for the synchronous reluctance motor.

The first and second tests were both performed at the first arbitrarily chosen rotor position. The maximum magnitudes of the short-current were both times in phase $a$, with an $\text{rms}$ value of less than 32 A, which can be seen in Figure 11.15. The phase which has the highest current magnitude depends on the rotor position. The SynRM short-circuit locked rotor current behavior in test number 8 is very similar to the simulation results in Figure 11.14.

**Summary**

A summary of the test results on the induction and synchronous reluctance motor is given in Table 11.2. As described earlier, the induction motor has symmetrical phase currents and the synchronous reluctance motor has rotor position dependent phase currents in a locked rotor short-circuit test, due to the geometrical structures of the rotors. The highest phase current magnitude is in the phase coils that have the lowest inductance, e.g. are facing the rotor along a magnetic axis that has the
11.4. EXPECTED SYNRM BEHAVIOR AT START-UP AND SHORT CIRCUIT LOCKED ROTOR CONDITIONS

Figure 11.15: The phase voltages and phase short-circuit currents of the induction motor and synchronous reluctance motor at different rotor positions, 3kWM machine.
CHAPTER 11. SECONDARY EFFECTS IN SYNRM

<table>
<thead>
<tr>
<th>Locked rotor test</th>
<th>Induction rotor</th>
<th>SynRM rotor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Network supply</td>
<td>Network supply</td>
<td>Network supply</td>
</tr>
<tr>
<td>Date</td>
<td>Date</td>
<td>Date</td>
</tr>
<tr>
<td>Clock</td>
<td>Clock</td>
<td>Clock</td>
</tr>
<tr>
<td>Rotor position</td>
<td>Rotor position</td>
<td>Rotor position</td>
</tr>
<tr>
<td>U1, RMS (V)</td>
<td>U1, RMS (V)</td>
<td>U1, RMS (V)</td>
</tr>
<tr>
<td>U2, RMS (V)</td>
<td>U2, RMS (V)</td>
<td>U2, RMS (V)</td>
</tr>
<tr>
<td>U3, RMS (V)</td>
<td>U3, RMS (V)</td>
<td>U3, RMS (V)</td>
</tr>
<tr>
<td>I1, RMS (A)</td>
<td>I1, RMS (A)</td>
<td>I1, RMS (A)</td>
</tr>
<tr>
<td>I2, RMS (A)</td>
<td>I2, RMS (A)</td>
<td>I2, RMS (A)</td>
</tr>
<tr>
<td>I3, RMS (A)</td>
<td>I3, RMS (A)</td>
<td>I3, RMS (A)</td>
</tr>
<tr>
<td>AVG(I1,I2,I3) (A)</td>
<td>AVG(I1,I2,I3) (A)</td>
<td>AVG(I1,I2,I3)</td>
</tr>
<tr>
<td>Short-circuit current reduction (highest phase values)</td>
<td>Short-circuit current reduction (highest phase values)</td>
<td>Short-circuit current reduction (highest phase values)</td>
</tr>
<tr>
<td>Short-circuit current reduction (lowest phase values)</td>
<td>Short-circuit current reduction (lowest phase values)</td>
<td>Short-circuit current reduction (lowest phase values)</td>
</tr>
<tr>
<td>Short-circuit current reduction (average phase values)</td>
<td>Short-circuit current reduction (average phase values)</td>
<td>Short-circuit current reduction (average phase values)</td>
</tr>
</tbody>
</table>

Table 11.2: Summary of short-circuit locked rotor test results on the SynRM and the IM, see Figure 11.15, 3kWm machine.

highest reluctance. As can be seen in Table 11.2 the highest current magnitude shifts to different phases, when the rotor position is shifted.

The highest current magnitude for the induction motor is 42 Arms. The highest magnitude for the phase with the maximum value for the SynRM is less than 32 Arms, which means a short-circuit current reduction of more than 25% compared to the induction motor. The average phase short-circuit current for the synchronous reluctance motor is 50% lower than for the induction motor.

Unlike the induction motor, the synchronous reluctance motor does not have Joule losses in the rotor, only some iron losses similar to the stator in a locked rotor test. Since also the stator winding losses are lower than the induction motor, the synchronous reluctance motor can survive a locked rotor test much longer. The impacts during short-circuit fault conditions are also substantially reduced compared to the standard IM motor, which means that the synchronous reluctance motor is more robust for variable speed drives.

11.5 SynRM with eccentricity

In the production processes of electrical machines there are always tolerances. An important effect is that the airgap of the machine is not uniform and due to the off-center position of the rotor or stator or both, the airgap is no longer constant, see e.g. [136]. If the rotor rotates slightly off-center then the machine will experience dynamic eccentricity, see Figure 11.16, and consequently the total electro-magnetic forces on the rotor will not balance each other. The result is a force which is acting on rotor and is rotating with the rotor. However, if the stator is off-center then the machine will experience static eccentricity and the resultant force will in a similar manner act on the rotor, but it will not rotate with the rotor, see Figure 11.16.

Very probably, both of these forces can take place simultaneously in the machine. The effect of such eccentricity on machine performance is presented in this section by simulation on a specific optimized machine design SynRM, 15kWm machine,
11.5. SYNRM WITH ECCENTRICITY

see its rotor cross section in Figure 9.10 (bottom-right) in chapter 9.

11.5.1 Electro-magnetic forces in SynRM with eccentricity

The electro-magnetic forces in the machine airgap, in the radial and tangential directions, can be evaluated by Equations (11.3) and (11.4), respectively [58] and [84], in Pa.

\[ \sigma_n(x,t) = \frac{1}{2\mu_0} \left( \hat{B}_n^2(x,t) - \hat{B}_t^2(x,t) \right) \] \hspace{1cm} (11.3)

\[ \sigma_t(x,t) = \frac{1}{\mu_0} \hat{B}_n(x,t) \cdot \hat{B}_t(x,t) \] \hspace{1cm} (11.4)

In Equations (11.3) and (11.4), \( \hat{B}_n(x,t) \) and \( \hat{B}_t(x,t) \) are the flux densities in the radial and tangential directions at a certain arc length distance \( x \) from the d-axis and time \( t \), respectively, and \( \mu_0 \) is the permeability of vacuum.

A FEM force calculation result, at a certain instance of time, for the specific optimized machine design SynRM without eccentricity, 15kWM machine, see Figure 9.10 (bottom-right), is shown in Figure 11.17. The airgap flux densities are calculated by FEM, over an arc at the middle of the airgap that starts from the d-axis. If the total force in Figure 11.17 is integrated over the rotor periphery and along the machine then due to symmetry the total force acting on the rotor will be almost zero (not exactly zero, due to the stator slots).

11.5.2 Total electro-magnetic forces in SynRM with eccentricity

Four scenarios are considered: 1) machine without eccentricity, 2) machine with 20 % dynamic eccentricity, 3) machine with 20 % static eccentricity and finally, 4) machine with 10 % dynamic and 10 % static eccentricity. For each machine, the total forces, amplitude and direction, that act on the rotor are simulated using geometries shown in Figure 11.16 and the method described above.
Figure 11.17: Magnetic pressures in the airgap of the SynRM, see Figure 9.10 (bottom-right), without eccentricity at a certain instance of time.
11.5. **SYNRM WITH ECCENTRICITY**

To create an eccentric machine in the Flux-2D software a small simulation trick has been used to simulate both the rotor (Dynamic) and the stator (Static) eccentricity. The airgap of the machine is divided into three equal sub-regions. The middle sub-region is fixed and the rotating airgap is assigned to this area. The remaining regions are assigned to the rotor and the stator, respectively. This trick enables the model to simulate both dynamic and static eccentricity, because the rotating airgap in FEM-Flux2D software must have a fixed center, see Figure 11.16. The simulation results are shown in Figure 11.18. Note that the machine length here is different from the machine length in chapter 9.

The maximum force on the rotor is just a function of the total eccentricity in the machine and independent of the eccentricity type, dynamic, static or both. Thus,
it is the total eccentricity that determines the maximum force. For example in this case, 20% eccentricity can create around 2 kN on the rotor, which is quite high. Such a force can damage the bearings of the machine.

An important information concerns the direction of the force. With static eccentricity, the maximum force acts only in one direction, whereas in dynamic eccentricity the total force rotates with the rotor as well. In the case of both dynamic and static eccentricities, sometimes the forces from the dynamic and static eccentricities cancel each other and sometimes they strengthen each other while the rotor is rotating. Therefore, in this condition, not only the direction of the force is rotating but also its amplitude is changing with time. This can increase the fatigue effects of the forces on the bearing even more.

Another effect concerns the stator slots. The number of stator slot in this case is 36. The ripple on top of the total force on the rotor in the case of dynamic eccentric is evident in Figure 11.18. There is a ripple in force with 36 time the rotational frequency of the machine with considerable amplitude. Such forces can create considerable noise in the machine when the rotor is rotating.

### 11.5.3 Iron losses in SynRM with eccentricity

Three cases are considered, 1) without eccentricity, 2) with 20% dynamic eccentricity and 3) with 20% static eccentricity for the SynRM in Figure 9.10 (bottom-right). Iron losses have been calculated when the rotor material is M400-50A and the stator material is M600-50A at 4500 \( \text{rpm} \) and the machine is of standard length \( L \). The results are shown in Table 11.3. No significant difference in iron losses from eccentricity is evident.

<table>
<thead>
<tr>
<th>Eccentricity Type</th>
<th>No Eccent.</th>
<th>20% Static</th>
<th>20% Dynamic</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotor Total Iron Losses [W]</td>
<td>817</td>
<td>841</td>
<td>842</td>
</tr>
<tr>
<td>Stator-Back Total Iron Losses [W]</td>
<td>1004</td>
<td>1000</td>
<td>970</td>
</tr>
<tr>
<td>Stator-Teeth Total Iron Losses [W]</td>
<td>753</td>
<td>763</td>
<td>771</td>
</tr>
<tr>
<td>Total Iron Losses [W]</td>
<td>2575</td>
<td>2604</td>
<td>2582</td>
</tr>
</tbody>
</table>

Table 11.3: *Effect of eccentricity on the iron losses of the SynRM, 15kW M machine, see Figure 9.10 (bottom-right).*
Part IV

Verification by Measurements on SynRM
Chapter 12

Validation by measurements

12.1 Introduction

There are mainly three technological challenges and factors that influence the variable speed drives front-line today. These are DC-drives commutation, the state-of-the-art power electronics components and the sophisticated control techniques.

The environmental concerns pushes for efficient use of electricity, especially in developing countries. This requires more control over operation of electrical machines, where most part of the produced electricity is consumed/converted by Induction Machines (IM) used in General Purpose Applications (GP-A) like pumps and fans, see chapter 1. Seeking new solutions is unavoidable. The strong challenge is, how can new solutions be developed and stand up against traditional low-cost low-efficient solutions? Permanent Magnet (PM) based electrical machines are well known and have attracted both academic and to some extent industrial attentions as a possible solution. It may seem that PM can pave the way for drives with ideal performances. The challenge here is the availability of PM rare-earth materials, which indicates high costs.

Old solutions can repeat their historical role to bring an intermediate step towards an ideal drive [2]. Could the reluctance concept be a solution? An overview of the academic works since 1923 till present time show the difficult challenges ahead that have to be considered [1] - [3], [13], [14], [19] - [26], [39], [45], [54], [57], [58], [76] and [108] - [113]. The technical issues in depth have been studied from theoretical [2], [3], [13], [14], [20], [21], [39], [45], [54], [57], [76] and [108] - [110], both theoretical and experimental [1], [22], [24] and [26] and experimental [19], [23], [25], [111], [112] and [113] point of view. Despite some exceptions [1] - [3], [13], [14], [19] - [22], [25], [26], [39], [45], [54], [76] and [108] - [111] the presented picture is distorted and not clear, especially from an industrial and practical point of view.

Providing a clear overview performance comparison between IM and Synchronous Reluctance Machines (SynRM) over a wide power range in GP-A is the main goal of this chapter. The SynRM [2] solution with the same standard stator size as the
IM is a natural way of utilizing the reluctance concept. Thus, the new concept will be as much as possible production compatible and it can be easily produced from the same production line as the IM to provide industrial production compatibility.

Acceptable and competitive performance comparison of the SynRM with its IM counterpart is under such circumstances a matter of great importance. This chapter will attempt to make such a comparison. Thus, three IM machines with different International Electrotechnical Commission (IEC) standard size have been selected, 3 kW (3kWM machine), 15 kW (15kWM machine) and 90 kW (90kWM machine). Then, for each machine, a high performance SynRM rotor has been designed and prototyped.

Finally, under Variable Speed Drive (VSD) conditions their performances are measured. The IM and SynRM measured performances are compared in this chapter. Using these measurements, an introduction to thermal performance and full scale performance evaluation of SynRM are presented.

12.1.1 Loss distribution and power capability of IM - SynRM concepts

Loss distribution

A 15 kW standard IM loss distribution at 1500 rpm (VSD) is schematically shown in Figure 12.1 (right). The rotor Joule losses are the most dominant part of the rotor losses. In an IM, these losses are unavoidable, because of the rotor cage. Of course, these losses depend on the overall design optimization, which must consider limitations in the type of material that can be used (Aluminum (Al) or copper (Cu) etc.), rotor cage structure and dimensions and the total Joule losses distribution between stator and rotor as well as the starting torque characteristic in Direct-On-Line (DOL) applications.

The reluctance concept gives a unique opportunity to eliminate the Joule losses

![Figure 12.1: (right) Loss distribution of a typical IM and (left) its counterpart SynRM, 15 kW at 1500 rpm, based on the initial design machine, see Table 5.1 in chapter 5.](image)
in the rotor. Especially when it has been shown that the torque capability of the SynRM, for certain current loading, can be as high as for the IM [1], [3], [14], [19] - [21], [23] - [26], [39], [45], [108] and [110]. This can be achieved by implementing suitable control of the SynRM in VSD. Measured losses in prototype SynRM in VSD operation are shown schematically in Figure 12.1 (left), see Table 5.1 in chapter 5. It shows that a considerable loss reduction is possible.

Increasing machine power capability

Eliminating the rotor cage, not only reduces the machine losses especially in the rotor, but also makes the machine control more advanced. Another advantage of the SynRM concerns the machine’s thermal capability. An optimal electrical machine design is a compromise between two completely different limitations, magnetic limitation and thermal limitation, for good examples see [1], [3], [19], [21], [23], [25], [26], [39], [45], [58], [108] and [111].

In the case of a SynRM, a study has shown that the machine can be magnetically loaded up to two to three times the nominal (thermal) current, see sub-section 2.3.3 in chapter 2. Thus, the magnetic limitation is, at least by a factor 2 greater than the thermal limitation, while keeping the same relative inverter rating. This gives a guide-line to how much the thermal capability can be increased by pushing the machine’s thermal limitations to higher values. Actually, the power capability of SynRM is, in 15kWM machine, around 15 % larger than the corresponding IM simply because the rotor copper losses do not exist.

Two main factors push the thermal limitation and characteristic of an electrical machine, reducing machine losses \( P_{\text{Loss}} = P_{\text{Cu}} + P_{\text{Fe}} + P_{\text{other}} \) and increasing the machine structure capability to transfer these losses to the ambient and cooling system. In case of the IM losses, the total copper losses \( P_{\text{Cu}} = 3R_{s}I_{s}^{2} + 3R_{r}I_{r}^{2} \) can not change significantly because a large current loading, large \( I_{s} \) and \( I_{r} \), is essential to achieve high torque density and power. The only possibility is to reduce \( R_{s} \) by increasing for example the copper fill factor in the stator slots, and by accurate manufacturing the windings to keep \( R_{s} \) as low as possible. However, in case of the SynRM the Joule losses for the same power level can be reduced significantly. Thus, the current and power of the SynRM machine can be increased for the same thermal limitation as the induction machine.

12.2 Design optimization of rotor geometry, 90kWM machine

The same method of analysis that has been implemented in chapter 10 is used in this section to design, optimize and evaluate the performance of the new SynRM for 90kWM machine. The natural flux lines inside the solid rotor technique, was used for designing the rotor for 90kWM machine, for details of the design procedure refer to chapter 9. End-adjusted and non-adjusted concept in chapter 9 are used
separately for designing the rotor with different number of barriers (3 – 4 barriers), 1 mm tangential rib is introduced in all barriers and geometries. The final optimized rotors performances are compared with the initial design machine of this machine size that is discussed in chapter 1, see Figure 1.7.

12.2.1 Promising designs and optimization

The detailed design steps are demonstrated in Figure 12.3. Firstly, the torque ripple was minimized by varying the rotor slot pitch angle parameter, $\beta$, see Figure 12.3 (left), then the insulation ratio in the q-axis, $k_{wq}$, was varied for maximum torque, see Figure 12.3 (right). The final optimized rotors, with the different number of barriers, are shown in Figure 12.2.

A performance comparison between the different rotor geometries is presented in Figure 12.4. Internal Power Factor (IPF) is calculated based on the simulated voltage and current vector calculation in FEM by implementing the external circuit in the FEM model, the end winding and stator resistance are not considered in these calculations.

All rotors have enhanced airgap torque of around 3.8 – 6.2 % compared to the initial design machine. The torque ripple is also reduced significantly by a factor of around 2 to 7. IPF, when $R_s = 0$ and end-winding leakage $I_{end}^{sl} = 0$, is also improved compared to the initial design machine SynRM, see chapter 1, except for rotor number 5, see Figure 12.4.
12.2. DESIGN OPTIMIZATION OF ROTOR GEOMETRY, 90KWM MACHINE

Figure 12.3: Optimization steps of rotor geometry with different number of layers. (Left) Torque ripple minimization using rotor slot pitch parameter, $\beta$, and (right) torque maximization by varying insulation ratio in the q-axis, $k_{wq}$, $n_s = 10$, $C_s = 1 Y$, $\theta = 45^\circ$, $I_s = 56.6$ A.
Figure 12.4: Performance comparison between different optimized machines and the initial design machine SynRM, see chapter 1, normalized with respect to the initial design machine SynRM, 90kW machine, \( n_s = 10 \), \( C_s = 1 \) Y, \( \theta = \) best angle at MTPA, \( I_s = 56, 6 \) A and 1500 rpm (FEM).
12.2. DESIGN OPTIMIZATION OF ROTOR GEOMETRY, 90KWM MACHINe

For maximum torque capability, minimum torque ripple and maximum IPF are to be considered simultaneously then geometries number 3, with 8 layers (3 barriers + 1 cut-off rotor structure), and 4, with 9 layers (4 barriers), see Figure 12.2, when absolutely constant rotor slot pitch has been used, are the best designs, see Figure 12.4. Consider that the initial design machine SynRM does not have any ribs here, as well.

A design with cut-off rotor structure is also considered (geometry number 3) because the cut-off barrier region can be used e.g. to weld together the iron laminations of the whole rotor structure along the axial direction. The low flux density in this region will probably not induce significant currents in the semi-cage structure created by the welding, but this has to be experimentally verified.

12.2.2 Mechanical stress calculation and ribs dimensioning

The mechanical stress study is used to dimension the radial ribs in the best promising rotor structures. The tangential ribs (TR) width is set to the minimum practical value of 0.0034 pu with respect to the airgap diameter, considering cutting limitation tolerances. Increasing this width is not suggested, instead it is recommended to increase the width of the radial ribs (RR) as the centrifugal forces are acting mostly in the radial direction. Nevertheless, electro-magnetically the difference between radial and tangential ribs is insignificant.

The stress study result for the chosen geometries is summarized in Figure 12.5 and it shows a safety margin of around 40 % for both chosen geometries for the maximum speed of 3000 rpm, if TR = 0.0034 pu, RR1 = 0.0078, RR2 = 0.0041, RR3 = 0.0034 pu with respect to the airgap diameter. Eliminating the radial rib of the 4th barrier in the 4 barrier geometry does not change the safety margin in this structure, because without RR4 the maximum stress level in the airgap near the outer flux path is 146 MPa. This is the same level as in ribs 1 and 2. Thus, without rib 4 the maximum stress from the centrifugal forces on the rotor is the
same. This elimination improves the electro-magnetic performance of the machine, thus it is removed.

12.2.3 Fine tuning

The best geometries, see Figure 12.6 (top), can still be improved by some fine tuning ideas. This tuning is mainly focused on correcting the shape of the first barrier in the first and second segment. The first segment width in the d-axis can be kept constant and equal to the smallest width of this segment in the d-axis. This changes the lower edge curve of barrier 1 to a straight line parallel to the d-axis, see Figure 12.6 (bottom-left). The second segment width is in the non-tuned design not constant, see Figure 12.6 (top). In the fine tuned design the upper curve of the first barrier is somewhat modified so the second segment width is kept constant along the segment. These two modifications remove some iron from the rotor without affecting the performance. The final geometries are shown in Figure 12.6 (bottom-left and middle).

These geometries are the best, tangential and radial ribs are also introduced in the rotor. The performance of these rotors are calculated and compared with the initial design machine SynRM (without any ribs) in Figure 12.7 at constant stator current and at the best current angle for MTPA. The 4 barriers rotor has the maximum output power, terminal power factor and efficiency. Its (un-skewed) torque ripple is minimum (less than 3 %).

The iron losses are about 8 % higher than the initial design machine SynRM,
12.2. DESIGN OPTIMIZATION OF ROTOR GEOMETRY, 90KWM MACHINE

Figure 12.7: Performance comparison of the final fine tuned rotors (with ribs) with the initial design machine SynRM (without ribs), \( n_s = 10, C_s = 2\sqrt{3} \Delta \Delta, \theta = \text{best angle at MTPA}, I_s = 197 \, \text{A} \) and 1500 rpm. The study shows that the 4 barriers rotor with minimum practically possible rounding in the airgap side of the barriers is the best (see 4 Bar).
because in this machine the end part of the barriers are not rounded as they are in
the reference machine. This rounding reduces the flux density fluctuation inside the
rotor due to the stator and rotor slot interaction but at the same time it increases
the q-axis inductance. To have a feeling about the rounding effect this issue is
implemented in the 4 barriers geometry, see Figure 12.6 (bottom-right) and its
performance is compared in Figure 12.7 with the other rotors. Rounding reduces
the iron losses at the expence of other performances which are reduced for this
machine.

12.3 Prototypes iron sheets

The production method affects the iron sheet material of the electrical machines
[115], [116] and [117], see [135] as well. Almost all electrical, magnetical, mechanical
and thermal behavior of the iron sheet are affected by different cutting techniques
such as punching and laser cutting. Normally, laser cutting increases the iron
losses as well as reduces the magnetic property of the material. The iron sheet
of the prototyped SynRM machine of this report are laser cut. Using conventional
punching tool in IM production line can improve the material performance.

A mechanical negative effect of the laser cutting concerns the cutting edges of
the iron sheets, specially at the end of the barriers in SynRM, near the airgap, due
to the un-smooth surface of the cutting. Despite the above-mentioned drawbacks
of the laser cutting technique, iron sheets of all the designed machines in this report
are cut using laser cutting tool. The final prototyped rotor iron sheet for 90kWM
machine is shown in Figure 12.8.

Figure 12.8: Prototyped rotor of the optimized machine design SynRM, 90kWM machine.
12.4 An introduction to thermal performance of SynRM

The thermal performance comparison of Synchronous Reluctance Machines (SynRM) and its counterpart IM has been studied earlier e.g. see experimental reports in [1], [19], [22], [23], [24], [25], [26], [111], [112] and [113]. These studies are mainly focused on the winding temperature difference between the two machines and the higher power capability of the SynRM in comparison to the IM. The thermal behavior of the SynRM itself is briefly discussed in these experiments.

A more detailed picture regarding the thermal performance of the SynRM machine is presented in this section. For this purpose, infra-red cameras, temperature sensors, PT100 and infra-red, in different parts of the machine, rotary and stationary are used to provide a better picture highlighting the importance of this issue and experimentally pointing some advantages and disadvantages of the SynRM in comparison to the IM from a thermal performance standpoint.

12.4.1 Infra-red picture of SynRM and IM under operation

The improved machine design SynRM, 15kWM machine is designed and experimentally studied in chapters 6, 7, 8 and 9, respectively, see Tables 8.3 and 9.2. This machine, see its rotor sheet in Figure 8.6 (left), at nominal conditions can deliver 13,7 kW at 1500 rpm. The thermal performance of this machine is compared with a 6-pole IM using an infra-red camera.

The tests are run on two test benches in parallel, running at an estimated same working point. The torque is measured directly only in the SynRM test bench. Similar load machines, IM 4-pole, 30 kW and similar four-quadrant inverters are used in the test benches.

The working point is 1000 rpm, checked with an optical sensor and 68,2 Nm measured on the shaft of the SynRM giving an output power of 7,142 kW. The load inverters are both torque controlled using similar torque reference signals. The test motors are speed controlled using two similar inverters, but of course with different DTC control softwares for the SynRM and the IM. Electrical power is measured on both test objects on both sides of the inverters. The consumed electrical energy is also measured by two energy meters at the input to the test inverters. The shaft and housing temperatures of both machines in hot-spot area are measured with a mobile prob directly after the test as well. The test-bench is shown in Figure 8.7.

The IM DTC-control in the inverter has an optimization function for the flux of the machine. Test on the IM is done twice, one with the activated optimization and the other without activation of the flux optimizer. The main function of the optimizer is to adjust the MTPA in steady-state operation. The flux optimization technique is discussed in chapter 2. Clearly, both the shaft and housing temperatures of the SynRM are lower than the IM at almost half of the nominal power. The shaft is colder by 11°C, see Figure 12.9 (bottom), and housing is cooler by 5°C, see Figure 12.9 (top). The hot spot area of the housing lies in the middle just below the terminal box of the machine as is clear from Figure 12.9 (top).
A field-test of these two machines for around 1100 hours operation at this operating point shows an energy saving of 3.2 – 5.9 % if SynRM machine is used instead of IM.

### 12.4.2 SynRM detailed different parts temperatures

The optimized machine design SynRM shown in Figure 9.10, SynRM–2 – OptM, is equipped with sensors. These are placed to investigate the effect of loading on the temperatures at different parts of the machine.

The position of these sensors are shown in Figure 12.10. Each sensor position is carefully selected to help create a picture of the critical points and distinguishing different temperature gradients inside the machine in both the rotor and stator as well as the heat interface gaps inside the machine. The actual position of the rotary temperature sensors in the rotor are shown in Figure 12.11.

The machine is operated in DTC-VSD condition as in earlier test conditions with speed control in a back-to-back test set-up similar to Figure 8.7. Three different load conditions are considered at 3000 rpm and 100 Nm, corresponding to class B, 140 Nm, corresponding to class F and 170 Nm, corresponding to class H. Each operating point was measured several times and the results are averaged. The
measurement results are shown in Figure 12.12 (average values) and in detail in Table 12.1.

The hottest part of the machine is in the middle of the machine on the rotor surface at rotor sensor #7, see Figure 12.10, temperature in Figure 12.12 and Table 12.1 - $T_r^7mg$. The part of the non-drive-end bearing is the coldest part of the machine, see stationary sensor #2 in Figure 12.10, temperature in Figure 12.12 and Table 12.1 - $T_n^2$. The cooling fan at the non-drive-end of the machine cools this region very well.

The corresponding IM class F operation at 3000 rpm and 97 Nm is measured. The machine torque is comparable and is almost the same as SynRM−2 − OptM in class B operating point, shown in Figure 12.12 and Table 12.1. Clearly, the winding of SynRM−2 − OptM is cooler than the IM winding by 30°C. In this condition, the drive-end bearing temperature of the IM and the SynRM−2 − OptM are 95°C and 70°C, respectively. The non-drive-end bearing temperature of the two machines are 67°C and 49°C, respectively.

The drive-end bearing temperature of the IM is high, 95°C, and is near the

Figure 12.10: Optimized machine design SynRM, detailed positions of temperature sensors in different parts of the machine.
critical temperature of the bearing. This limit does not allow the machine to run at a higher temperature class of operation. In contrast, if the SynRM−2 − OptM is run at class $H$ then the machine drive-end bearing temperature will be $94^\circ C$, see Figure 12.12 and Table 12.1.

The temperature difference in the rotor in the radial direction at class $B$ is around $7^\circ C$, compare temperatures of rotor sensors #7 and #10. The rotor surface temperature difference in the axial direction between the middle and non-drive-end of the rotor at class $B$ is around $18^\circ C$, compare temperatures of rotor sensors #7 and #8. The rotor surface temperature difference in the axial direction between the middle and drive-end of the rotor at class $B$ is around $6^\circ C$, compare temperatures of rotor sensors #7 and #3. The rotor shaft temperature difference in the axial direction between the middle and non-drive-end of the rotor at class $B$ is around $34^\circ C$, compare temperatures of rotor sensors #10 and #6. The rotor shaft temperature difference in the axial direction between the middle and drive-end of the rotor at class $B$ is around $34^\circ C$, compare temperatures of rotor sensors #10 and #1. These temperature gradients can create different thermal expansion in rotor structure.

The temperature difference between machine air in the drive- and non-drive ends at class $B$ is roughly around $36^\circ C$, compare temperatures of stationary sensors #3 and #4. The temperature difference between the machine end-windings in the drive- and non-drive ends at class $B$ is roughly around $17^\circ C$, compare temperatures of stationary sensors #7 or #8 or #9 and #10. If somehow the air at two end regions
12.4. AN INTRODUCTION TO THERMAL PERFORMANCE OF SYNRM

Figure 12.12: Optimized machine design SynRM, 15kW M machine, SynRM-2 – Opt M, detailed measured temperatures at different spots in the machine as function of load at 3000 rpm.
of the machine are mixed through the rotor barriers or by a similar method then the average temperature will be around 80°C and consequently the drive-end of the machine will be cooler and the inside of the machine will have a more homogeneous temperature distribution. It will also help cooling the end-winding at the drive-end of the machine.

The temperature difference across the airgap at the non-drive-end bearing at class B is around 36°C, compare temperatures of rotor sensor #6 and stationary sensor #2. The temperature difference at the drive-end bearing at class B is around 14°C, compare temperatures of rotor sensor #1 and stationary sensor #1. The non-drive-end of the machine bearing experiences larger temperature gradients than the drive-end of the machine. Therefore, the non-drive-end bearing will be more venerable to the load, which could probably lead to faulty conditions more frequently. The temperature difference over the machine airgap in the middle of the machine at class B is roughly around 29°C, compare temperatures of rotor sensor

<table>
<thead>
<tr>
<th>T (Nm)</th>
<th>wN-10</th>
<th>Tr7mg</th>
<th>Tr10ms</th>
<th>Tr2dp</th>
<th>Tr8n</th>
<th>Th20</th>
<th>Tab17</th>
<th>TrShd</th>
<th>TrShld</th>
<th>Tr3Id</th>
<th>Tr6shn</th>
</tr>
</thead>
<tbody>
<tr>
<td>103</td>
<td>86.1</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>90.3</td>
<td>97.3</td>
<td>X</td>
<td>X</td>
<td>68.4</td>
<td>X</td>
<td>84.1</td>
</tr>
<tr>
<td>103</td>
<td>85.4</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>88.8</td>
<td>95.9</td>
<td>X</td>
<td>84.2</td>
<td>68.7</td>
<td>117.6</td>
<td>83.7</td>
</tr>
<tr>
<td>103</td>
<td>86.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>90.1</td>
<td>97.3</td>
<td>85.0</td>
<td>68.0</td>
<td>120.3</td>
<td>84.2</td>
<td></td>
</tr>
<tr>
<td>103</td>
<td>84.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>87.7</td>
<td>95.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>103</td>
<td>84.9</td>
<td>125.3</td>
<td>117.9</td>
<td>116.5</td>
<td>108.4</td>
<td>89.1</td>
<td>96.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td></td>
<td>103</td>
<td>85.4</td>
<td>125.3</td>
<td>117.9</td>
<td>116.5</td>
<td>108.4</td>
<td>89.2</td>
<td>96.4</td>
<td>84.6</td>
<td>119.0</td>
<td>84.0</td>
</tr>
<tr>
<td>138</td>
<td>104.8</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>107.6</td>
<td>115.4</td>
<td>X</td>
<td>75.5</td>
<td>X</td>
<td>95.7</td>
<td></td>
</tr>
<tr>
<td>138</td>
<td>102.7</td>
<td>141.6</td>
<td>136.3</td>
<td>X</td>
<td>121.0</td>
<td>106.7</td>
<td>114.2</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>138</td>
<td>100.4</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>103.0</td>
<td>111.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td></td>
<td>138</td>
<td>102.6</td>
<td>141.6</td>
<td>136.3</td>
<td>X</td>
<td>121.0</td>
<td>105.8</td>
<td>113.6</td>
<td>X</td>
<td>75.5</td>
<td>95.7</td>
</tr>
<tr>
<td>172</td>
<td>128.3</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>128.1</td>
<td>134.9</td>
<td>X</td>
<td>85.1</td>
<td>X</td>
<td>108.6</td>
<td></td>
</tr>
<tr>
<td>172</td>
<td>125.3</td>
<td>164.3</td>
<td>155.0</td>
<td>X</td>
<td>138.6</td>
<td>125.9</td>
<td>134.6</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>172</td>
<td>122.4</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>121.7</td>
<td>131.0</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td></td>
<td>172</td>
<td>125.0</td>
<td>164.3</td>
<td>155.0</td>
<td>X</td>
<td>138.6</td>
<td>124.6</td>
<td>133.5</td>
<td>X</td>
<td>85.1</td>
<td>108.6</td>
</tr>
</tbody>
</table>

Table 12.1: Optimized machine design SynRM, SynRM−2 − OptM, 15kW M machine, 
detailed measured temperatures at different spots in the machine as a function of load at 3000 rpm.
The rotor surface in the middle of the machine not only has higher loss concentration, but this region of the rotor is also more difficult to cool. There are three main heat flow paths for the rotor surface losses. First, through the machine airgap to the stator from the rotary sensor #7 to the stationary sensor #17, where there is a temperature difference of $29^\circ C$ then to the stationary sensor #20, with a temperature difference of $7^\circ C$ and finally to the ambient sensor #5, with a temperature difference of $67^\circ C$. Second, to the non-drive-end of the rotor region to the rotary sensor #5, where there is a temperature difference of $57^\circ C$ then to the end flange and end region internal air stationary sensors #4 and #2, with temperature differences of $6^\circ C$ and $19^\circ C$, respectively. Third, to the drive-end flange and the drive region internal air stationary sensors #3 and #1, with temperature differences of $27^\circ C$ and $55^\circ C$, respectively.

The temperature difference between machine stator and housing in the middle of the machine at class $B$ is roughly around $7^\circ C$, compare temperatures of stationary sensors #17 and #20. In general the iron losses in the machine specially in the rotor, have a strong influence on the temperatures in the different parts of the machine.

### 12.5 Full scale performance evaluation of SynRM

The performance of Synchronous Reluctance Machine (SynRM) and its counterpart Induction Machine (IM) for different sizes are discussed in this section. The earlier test results for $15kW$ machine in previous chapters are used and new measurements are not reported for this size. All IM and SynRM machines in this section have the same standard stator structure for each size. The almost MTPA control strategy is also used in the measurements presented in this section.

#### 12.5.1 SynRM and IM performance comparison at 1500 rpm

A full picture for IM and SynRM performance comparison at 1500 $rpm$ with the same standard stator structure is presented in this sub-section. The comparison is based on all measurements that have been discussed in this report at 1500 $rpm$ for all $3kW$, $15kW$ and $90kW$ machines, and all machine types, IM, SynRM and IPM. Such a comparison is shown in Figure 12.13.

The SynRM efficiency is always higher than the IM efficiency. This improvement for power ranges of $3kW$, $15kW$ and $90kW$ are around 5.5, 3.5 and 1.5 % $- units$, respectively, see Figure 12.13 (c). At the same time, the machine winding temperature of SynRM is lower than the IM. For power ranges of $3kW$, $15kW$ and $90kW$ it is around 15, 12 and 10 $K$, respectively, see Figure 12.13 (b). This means that the machine power can be increased roughly by 20 to 25 % for SynRM for the same winding temperature rise as the IM. The SynRM power factor is always lower than the IM, by almost 5 % $- units$, see Figure 12.13 (d). The efficiency improvement of SynRM in comparison to the IM with the same winding temperature is also higher.
Figure 12.13: A summary comparison of measurements on all machines presented in this report at 1500 rpm.
Figure 12.14: A summary comparison of the measurement on all sizes presented in this report (1/2).
Figure 12.15: A summary comparison of the measurement on all sizes presented in this report (2/2).
and independent of the increased power of the SynRM machine, see Figure 12.13 (f). Finally, the efficiency improvement based on measurements closely follow the predictions in chapter 1, Figure 1.6 and chapter 2, Figure 2.11.

The IPM machine efficiency improvement in comparison to the IM is also shown in Figure 12.13 (f) at the same winding temperature rise. The improvement for higher power does not follow the same pattern as low power in comparison to the SynRM efficiency improvement. However, this machine has higher efficiency than both the IM and the SynRM.

12.5.2 SynRM and IM performance comparison, all measurements summary

Finally, all the measurements that have been presented in this report are summarized in this sub-section, see Table 5.1 in chapter 5, Table 8.3 in chapter 8 and Table 9.2 in chapter 9. Consider that machines in these measurements have different speed as well as different winding temperature and sizes.

The comparative results are shown in Figure 12.14 and Figure 12.15. The IPM machine has the highest efficiency and IM has the lowest, see Figure 12.14 (a). The total losses of the machines are presented in Figure 12.15 (c), as well. On the other hand, the SynRM has the lowest power factor and the IPM has the highest, see Figure 12.14 (b).

The IPM machine has the lowest per-unit inverter current and the IM and SynRM have very close per-unit inverter currents, see Figure 12.14 (c). The machines torque per ampere are compared in Figure 12.15 (a). The IM and SynRM have almost similar torque per ampere, however, the IPM machine, with balance compensated structure, see chapters 5 and 3, has higher torque per ampere capability in comparison to the IM and SynRM.

The system efficiency of the IM and SynRM are compared in Figure 12.15 (b). The inverter efficiency of the IM and SynRM drives are almost the same, consequently the higher SynRM efficiency affects the system efficiency, as well. The system efficiency of the SynRM is always higher than the IM.
Chapter 13

Conclusion and future work

This thesis has been comprehensively dedicated to the theoretical and experimental reevaluation of the Synchronous Reluctance Machine (SynRM). The thesis has critically examined the important research that has been done on this subject since 1923 up to present time. These works have deeply contributed to the progress of this study and thesis. Hopefully, the work presented in this thesis will further contribute to extend the knowledge on this subject.

The main purpose with a project like this is to promote new technologies such as SynRM with a more competitive capability compared to the standard Induction Machine (IM) in terms of performance in Variable Speed Drives (VSD) operation for General Purpose (GP) applications. It has been shown that such promotion can be achieved by the development of a suitable and fast design technique for the SynRM machine and then carrying out a full scale performance comparison between conventional IM of standard size and range and its counterpart which is the optimized SynRM machine.

It is the author’s hope that this thesis can be of help and inspiration to engineers who work with research and development of electrical machines and to people who study possible new solutions for high efficient electrical machines. Apart from this thesis and report, results from the project have been published in a number of papers and articles listed in the introduction. These publications cover only a part of the results due to the time consuming procedure of writing journal and conferences publication and limited project time.

A clear overview performance comparison between IM and SynRM over a wide power range in GP applications has been provided in this thesis. The SynRM solution with the same standard stator as the IM is a natural way of utilizing the reluctance concept. Thus, the new concept will be as much as possible production compatible and it can be easily produced with the same stator as the IM to provide industrial production compatibility. Acceptable and competitive performance comparison of the SynRM with its IM counterpart is under such circumstances a matter of great importance. This thesis has attempted to make such a comparison. Thus,
three IM machines with different IEC standard sizes have been selected, 3 kW, 15 kW and 90 kW. Then, for each machine, a high performance SynRM rotor has been designed, using the advanced design tool that has been developed and presented in this Ph.D work, and prototyped. Finally, under VSD conditions their performances have been measured. The IM and SynRM measured performances have been compared in this thesis. Using these measurements, an introduction to thermal performance, different controls evaluation and full scale performance evaluation of the SynRM have been presented.

The analytical and experimental studies of SynRM in comparison to the IM in this thesis show that the SynRM machine has very promising performance in the low power ranges especially up to 10 kW, where certainly no inverter size increase is required for the SynRM in comparison to the IM. At the same time the machine has much better performance than the IM with much simpler rotor structure.

For higher power ranges, if a slightly larger size inverter can be tolerated, the higher power capability and efficiency as well as the lower bearing temperatures are quite attractive features of the SynRM. In such a condition the machine size can be sometimes reduced by one size for the SynRM in comparison to the IM. However, the network power factor of the whole drive is not affected with the machine power factor but with the drive load and it is independent of the machine type. This is due to positive effect of the DC-link capacitor and the drive rectifier performance. All these advantages can be achieved if the SynRM is designed properly. Two different novel design methods are discussed in this thesis and it has been shown both analytically and experimentally that they are powerful tools for SynRM machine design. If the SynRM is not designed properly, then the performance of the machine can easily be inferior to the corresponding IM performance in terms of torque, efficiency, power factor and torque ripple.

This work can be further extended by expanding the research topic to study e.g. the overall optimization including both rotor and stator structures of the SynRM, other applications such as traction, more in depth loss and thermal analysis of SynRM, verification of the findings regarding the PMaSynRM performance by measurement and the SynRM performance evaluation in field-weakening operation.
Part V

Attachments
SynRM, IM and IPM machines benchmarking measurements

These measurements are discussed in chapter 5. Please refer to Table 5.1, as well.

<table>
<thead>
<tr>
<th>Ref.</th>
<th>IM Pout at 1500 rpm [kW]</th>
<th>dT-rise ~ cte.</th>
</tr>
</thead>
<tbody>
<tr>
<td>IM</td>
<td>SynRM</td>
<td>IPM</td>
</tr>
<tr>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>516</td>
<td>514</td>
</tr>
<tr>
<td>fs [Hz]</td>
<td>18.4</td>
<td>17.1</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>6.6</td>
<td>0</td>
</tr>
<tr>
<td>ns: no. of cond. / slot</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>cs: winding connection</td>
<td>1,7321</td>
<td>1,7321</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>83</td>
<td>44</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>51</td>
<td>99</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>1990</td>
<td>1610</td>
</tr>
<tr>
<td>Pc u, Stator [W]</td>
<td>1184</td>
<td>1491</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>23</td>
<td>23</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>783</td>
<td>96</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>145.3</td>
<td>155.2</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>7.9</td>
<td>8.4</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>9.8</td>
<td>10.0</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>12.6</td>
<td>13.1</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>79.8</td>
<td>83.8</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0.78</td>
<td>0.76</td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0.622</td>
<td>0.64</td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1.61</td>
<td>1.57</td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>99</td>
<td>102</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1,ns=1)</td>
<td>0.32</td>
<td>0.30</td>
</tr>
</tbody>
</table>

Table 1: Heat-run test measurements on SynRM [22], IM and IPM machines, 15kW M machine at 500 rpm.
<table>
<thead>
<tr>
<th>Machine Type</th>
<th>IM</th>
<th>SynRM</th>
<th>IPM</th>
<th>IM</th>
<th>SynRM</th>
<th>IPM</th>
<th>IM</th>
<th>SynRM</th>
<th>IPM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operation Type</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>1523</td>
<td>1513</td>
<td>1510</td>
<td>1514</td>
<td>1516</td>
<td>1521</td>
<td>1514</td>
<td></td>
<td></td>
</tr>
<tr>
<td>fs [Hz]</td>
<td>51,6</td>
<td>50,4</td>
<td>50,3</td>
<td>51,6</td>
<td>50,5</td>
<td>50,7</td>
<td>50,5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Slip [%]</td>
<td>1,6</td>
<td>0</td>
<td>0</td>
<td>2,3</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td></td>
<td></td>
</tr>
<tr>
<td>ns: no. of cond. / slot</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td></td>
<td></td>
</tr>
<tr>
<td>cs: winding conection</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td></td>
<td></td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>201</td>
<td>112</td>
<td>111</td>
<td>125</td>
<td>122</td>
<td>125</td>
<td>120</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>37</td>
<td>66</td>
<td>52</td>
<td>81</td>
<td>94</td>
<td>91</td>
<td>68</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>1538</td>
<td>964</td>
<td>872</td>
<td>2471</td>
<td>1806</td>
<td>1971</td>
<td>1192</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>540</td>
<td>588</td>
<td>346</td>
<td>1014</td>
<td>1340</td>
<td>1260</td>
<td>609</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>70</td>
<td>70</td>
<td>69</td>
<td>70</td>
<td>70</td>
<td>70</td>
<td>70</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>928</td>
<td>307</td>
<td>457</td>
<td>1387</td>
<td>396</td>
<td>640</td>
<td>513</td>
<td></td>
<td></td>
</tr>
<tr>
<td>T [Nm]</td>
<td>94,1</td>
<td>95,1</td>
<td>96,1</td>
<td>129,8</td>
<td>146</td>
<td>172,7</td>
<td>129,2</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>15,0</td>
<td>15,1</td>
<td>15,2</td>
<td>20,6</td>
<td>23,1</td>
<td>27,5</td>
<td>20,5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>16,5</td>
<td>16,0</td>
<td>16,1</td>
<td>23,0</td>
<td>24,9</td>
<td>29,5</td>
<td>21,7</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>22,1</td>
<td>22,3</td>
<td>18</td>
<td>30,5</td>
<td>34,5</td>
<td>35</td>
<td>24,6</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>90,7</td>
<td>94,0</td>
<td>94,6</td>
<td>89,3</td>
<td>92,7</td>
<td>93,3</td>
<td>94,5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0,74</td>
<td>0,71</td>
<td>0,910</td>
<td>0,74</td>
<td>0,71</td>
<td>0,850</td>
<td>0,880</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0,671</td>
<td>0,67</td>
<td>0,86</td>
<td>0,661</td>
<td>0,658</td>
<td>0,79</td>
<td>0,832</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1,49</td>
<td>1,50</td>
<td>1,16</td>
<td>1,51</td>
<td>1,52</td>
<td>1,26</td>
<td>1,20</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>60</td>
<td>56</td>
<td>42</td>
<td>102</td>
<td>102</td>
<td>102</td>
<td>55</td>
<td></td>
<td></td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1,ns=1)</td>
<td>0,28</td>
<td>0,28</td>
<td>0,35</td>
<td>0,31</td>
<td>0,30</td>
<td>0,36</td>
<td>0,36</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 2: Heat-run test measurements on SynRM [22], IM and IPM machines, 15kW M machine at 1500 rpm.
### Table 3: Heat-run test measurements on SynRM [22], IM and IPM machines, 15kW M machine at 2500 rpm.

<table>
<thead>
<tr>
<th>Ref. IM Pout at 1500 rpm [kW]</th>
<th>11</th>
<th>12</th>
<th>13</th>
<th>14</th>
<th>15</th>
<th>16</th>
</tr>
</thead>
<tbody>
<tr>
<td>Machine Type</td>
<td>IM</td>
<td>SynRM</td>
<td>IPM</td>
<td>IM</td>
<td>SynRM</td>
<td>IM</td>
</tr>
<tr>
<td>Operation Type</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
<td>VSD</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>2503</td>
<td>2554</td>
<td>2505</td>
<td>2503</td>
<td>2512</td>
<td>2538</td>
</tr>
<tr>
<td>fs [Hz]</td>
<td>84,5</td>
<td>85,1</td>
<td>83,5</td>
<td>83,4</td>
<td>84,7</td>
<td>84,6</td>
</tr>
<tr>
<td>Slip [%]</td>
<td>1,244</td>
<td>9</td>
<td>0</td>
<td>9</td>
<td>0</td>
<td>1,098</td>
</tr>
<tr>
<td>ns: no. of cond. / slot</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
<td>9</td>
</tr>
<tr>
<td>cs: winding connection</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
<td>1,7321</td>
</tr>
<tr>
<td>V1rms [V], Phase</td>
<td>202</td>
<td>196</td>
<td>197</td>
<td>177</td>
<td>189</td>
<td>165</td>
</tr>
<tr>
<td>Irms [A], Phase</td>
<td>76</td>
<td>85</td>
<td>86</td>
<td>63</td>
<td>67</td>
<td>71</td>
</tr>
<tr>
<td>Total Losses [W]</td>
<td>2802</td>
<td>2260</td>
<td>2408</td>
<td>1480</td>
<td>2389</td>
<td>1539</td>
</tr>
<tr>
<td>Pcu, Stator [W]</td>
<td>888</td>
<td>1152</td>
<td>1125</td>
<td>527</td>
<td>648</td>
<td>710</td>
</tr>
<tr>
<td>Friction Losses [W]</td>
<td>123</td>
<td>126</td>
<td>123</td>
<td>123</td>
<td>123</td>
<td>125</td>
</tr>
<tr>
<td>Rest Losses [W]</td>
<td>1791</td>
<td>983</td>
<td>1160</td>
<td>830</td>
<td>1618</td>
<td>705</td>
</tr>
<tr>
<td>T [Nm]</td>
<td>114,9</td>
<td>131,7</td>
<td>159,3</td>
<td>114,9</td>
<td>97</td>
<td>97</td>
</tr>
<tr>
<td>Pout [kW]</td>
<td>30,1</td>
<td>35,2</td>
<td>41,8</td>
<td>30,1</td>
<td>25,7</td>
<td>25,8</td>
</tr>
<tr>
<td>Pin, total [kW]</td>
<td>32,9</td>
<td>37,5</td>
<td>44,2</td>
<td>31,6</td>
<td>28,1</td>
<td>27,3</td>
</tr>
<tr>
<td>Sin1 [kVA]</td>
<td>45,9</td>
<td>51,1</td>
<td>51</td>
<td>33,9</td>
<td>36,8</td>
<td>36</td>
</tr>
<tr>
<td>Efficiency [%]</td>
<td>91,5</td>
<td>94,0</td>
<td>94,6</td>
<td>95,3</td>
<td>91,5</td>
<td>94,4</td>
</tr>
<tr>
<td>PF1 [*]</td>
<td>0,717</td>
<td>0,734</td>
<td>0,867</td>
<td>0,931</td>
<td>0,762</td>
<td>0,759</td>
</tr>
<tr>
<td>Efficiency · PF1</td>
<td>0,656</td>
<td>0,69</td>
<td>0,82</td>
<td>0,888</td>
<td>0,698</td>
<td>0,716</td>
</tr>
<tr>
<td>1 / (Efficiency · PF1) [*]</td>
<td>1,53</td>
<td>1,45</td>
<td>1,22</td>
<td>1,13</td>
<td>1,43</td>
<td>1,40</td>
</tr>
<tr>
<td>Windings Temp. Rise [K]</td>
<td>104</td>
<td>103</td>
<td>103</td>
<td>58</td>
<td>83</td>
<td>74</td>
</tr>
<tr>
<td>T/I [Nm/A rms] (cs=1,ns=1)</td>
<td>0,29</td>
<td>0,30</td>
<td>0,36</td>
<td>0,35</td>
<td>0,28</td>
<td>0,26</td>
</tr>
</tbody>
</table>
Bibliography


1This work was partially supported by "High-Tech Research Center" Project for Private Universities: matching fund subsidy from Ministry of Education, Culture, Sports, Science and Technology, Japan, 2004-2008. The authors are with the (e-mail: fukami@neptune.kanazawa-it.ac.jp).


[110] J. R. Hendershot, T. J. E. Miller, "The synchronous reluctance motor for motion control applications".


generation scheme in IPM synchronous motor drives", Proceedings of the 12th
European Conference on Power Electronics and Applications, 2007, , Aalborg,
Denmark, pp: 1 - 10.

[122] P. Niazi, H. A. Toliyat, A. Goodarzi, "Robust Maximum Torque per Ampere
(MTPA) Control of PM-Assisted SynRM for Traction Applications", IEEE
Transactions on Vehicular Technology, Vol. 56, Issue 4, July 2007, pp:1538 -
1545.

[123] T. Matsuo, T. A. Lipo, "Field oriented control of synchronous reluctance
machine", Power Electronics Specialists Conference, PESC '93, 24th Annual

[124] R. Lagerquist, I. Boldea, T. J. E. Miller, "Sensorless-control of the syn-
chronous reluctance motor", IEEE Transactions on Industry Applications,

[125] A. Kilthau, J. M. Pacas, "Parameter-measurement and control of the syn-
chronous reluctance machine including cross saturation", Thirty-Sixth IAS
2309.

[126] A. Kilthau, J. M. Pacas, "Appropriate models for the control of the syn-
chronous reluctance machine", 37th IAS Annual Meeting, Industry Applica-

[127] C. Mademlis, "Compensation of magnetic saturation in maximum torque to
current vector controlled synchronous reluctance motor drives", IEEE Trans-

for interior permanent-magnet synchronous motor drives', IEEE Transactions

[129] E. M. Rashad, T. S. Radwan, M. A. Rahman, "A maximum torque per am-
pere vector control strategy for synchronous reluctance motors considering
saturation and iron losses", 39th IAS Annual Meeting, Industry Applications

[130] K. Malekian, M. R. Sharif, J. Milimonfared, 'An optimal current vector con-
trol for synchronous reluctance motors incorporating field weakening', 10th
IEEE International Workshop on Advanced Motion Control, 2008, AMC '08,
pp: 393 - 398.

[131] R. E. Betz, "Control of synchronous reluctance machines", IEEE Annual Meet-
462.


List of Figures

1.1 Consumed Electrical Energy for Different Applications .................. 2
1.2 Different Motor Types Classification .................................. 3
1.3 Different Motor Types Application .................................... 3
1.4 IEC Standard Efficiency Classes for 4-pole IM DOL Operation @ 50 Hz 5
1.5 DOL and VSD Controls Comparison of a Pump .......................... 7
1.6 The IM Slip and Estimated Efficiency Difference Between SynRM and IM with Same Stator, based on Nominal IM Slip ............ 9
1.7 Initial and Optimized SynRM Rotor Structures, 90kW M Machine .... 11

2.1 SynRM Equivalent Circuit ........................................ 22
2.2 Reluctance Concept ............................................. 23
2.3 Cross-Coupling Effect in SynRM ................................ 25
2.4 SynRM IPF, Saliency Ratio and Current Angle, Ideal ................. 26
2.5 SynRM Torque in Constant Flux and Constant Current Conditions, Ideal. 27
2.6 SynRM Operation Diagram (OpD), Ideal .......................... 29
2.7 Saturation Effect in Synchronous Reluctance Machine ................. 32
2.8 Possible Rotor Design for a SynRM ................................ 34
2.9 Historical Evolution of SynRM .................................... 35
2.10 First and its Related Modern Developed TLA-SynRM ................. 36
2.11 Estimated Efficiency Difference between SynRM and IM ............. 38
2.12 Synchronous Reluctance Machine Circle Operation Diagram ........ 40
2.13 Synchronous Reluctance Machine Field Weakening, Ideal and Non Ideal 42
2.14 SynRM, Power Factor Based Parameter Estimator .................... 44
2.15 SynRM, Overload Capacity ..................................... 45
2.16 Calculated Effect of $\theta$ on SynRM$-1-OptM$ Performances, 15kWM Machine .................................................. 47
2.17 Calculated Effect of Fundamental Phase Voltage on SynRM$-1-OptM$ Performances, 15kWM Machine .................................. 48
2.18 Calculated Effects of Fundamental Flux on SynRM$-1-OptM$ Performances, 15kWM Machine ...................................... 49
2.19 Calculated Effects of Fundamental Phase Current on SynRM$-1-OptM$ Performances, 15kWM Machine .................................... 50
List of Figures

2.20  Calculated Effects of Fundamental PF on SynRM−1−OptM Performances, 15kWM Machine .................................................. 51
2.21  Calculated Distribution of Losses in SynRM−1−OptM, 15kWM Machine 52

3.1  Possible PMaSynRM Rotor Structures ........................................ 57
3.2  PMaSynRM Nature (1) ................................................................. 58
3.3  PMaSynRM Nature (2) ................................................................. 60
3.4  SynRM, 3 Possible PM Introduction Methods in SynRM and Relation Between PM volume and $\lambda_{PM0}$ .............................. 62
3.5  SynRM and Six Different PMaSynRMs of Figure 3.4, Torque vs. Current Angle ................................................................. 64
3.6  SynRM and Six Different PMaSynRMs of Figure 3.4, Torque, IPF and Torque Ripple ............................................................... 66
3.7  SynRM and Six Different PMaSynRMs of Figure 3.4, MTPA and MIPFxPA Operating Characteristics .............................................. 67
3.8  SynRM and Six Different PMaSynRMs of Figure 3.4, Equi-Flux (Flux Contour) at Nominal MTPA ................................................ 68
3.9  SynRM and Six Different PMaSynRMs of Figure 3.4, Nominal MTPA Airgap Flux Density and Current Vectors .............................. 69
3.10 Six Different PMaSynRMs of Figure 3.4, Open Circuit Airgap Flux Density and Voltage ............................................................. 70
3.11 The Worst Case for PMaSynRM’s PM Demagnetization Situations .... 71
3.12 Typical $\lambda_{PM0}$ and IPF Relation in PMaSynRM at Constant Current and MTPA, in Ideal Conditions ........................................... 73
3.13 Typical $\lambda_{PM0}$ and IPF Relation in PMaSynRM at Constant Current and MTPA, in Saturated Machine ........................................... 74
3.14 Effect of $\lambda_{PM0}$ on PMaSynRM Performance in Medium Speed Range, MTPA ................................................................. 77
3.15 Effect of $\lambda_{PM0}$ on PMaSynRM Performance at 1500 rpm, MTPA (1) . 78
3.16 Effect of $\lambda_{PM0}$ on PMaSynRM Performance at 1500 rpm, MTPA (2) . 79

4.1  SP and One Interior Barrier SynRM Microscopic and Macroscopic Parameters Definition ......................................................... 84
4.2  SP and One Interior Barrier SynRM: Effect of Barrier Width .............. 86
4.3  One Interior Barrier SynRM: Effect of Barrier Position in q-axis .......... 88
4.4  One Interior Barrier SynRM: Effect of Barrier Width in the d-axis ....... 89
4.5  One Interior Barrier SynRM: Effect of Barrier Leg Angle ................. 90

5.1  SynRM Initial Design Machine and ref. IM and IPM ....................... 94
5.2  SynRM: Effect of Airgap length, Radial Rib Width and Radius .......... 95
5.3  Heat-Run Test bench of IM, IPM and SynRM ............................... 96
5.4  IM machine and Prototyped SynRM Rotor of the Initial Design Machine 99
List of Figures

6.1 SynRM: Proposed Multi-Barrier Rotor Geometry (with-cut-off Rotor Structure) ................................... 106
6.2 Per-unit MMF Distribution Over Segments in the q-axis and d-axis MMF (with-cut-off Rotor Structure) .................... 109
6.3 Effect of $k_{wq}$ and $k_{wd}$ on Torque (FEM) .................. 112
6.4 Effect of Barriers Width on Torque (FEM) .................. 113
6.5 SynRM With- and Without- Cut-off Patterns ............... 114
6.6 SynRM: Number of Barriers Effect ........................... 114
6.7 SynRM: Number of Barriers and Layers Effect .................. 115
6.8 Effect of $k_{wq}$ on (Torque, IPF) and $\left(L_d - L_q, \xi = L_d/L_q\right)$, Example .......................... 117
6.9 Effect of $k_{wq}$ and $\theta$ on Torque @ 100% of the Nominal Current ................. 118
6.10 Effect of $k_{wq}$ and $\theta$ on Power Factor @ 100% of the Nominal Current ................. 119
6.11 Effect of $k_{wq}$ and $\theta$ on Torque @ 75% of the Nominal Current .................. 120
6.12 Effect of $k_{wq}$ and $\theta$ on Power Factor @ 75% of the Nominal Current ................. 121
6.13 Effect of $k_{wq}$ and $\theta$ on Torque Ripple @ 100% of the Nominal Current ................. 122

7.1 SynRM: Proposed Multi-Barrier Rotor Geometry (without-cut-off Rotor Structure) ................................... 126
7.2 Per-unit MMF Distribution Over Segments in the q-axis and d-axis MMF (without-cut-off Rotor Structure) .................... 128
7.3 Effect of $\beta$ on Torque and Torque Ripple .................. 129
7.4 Effect of $\beta$ on Rotor Structure ............................ 130
7.5 Effect of Barriers Permeance on Torque Ripple .................. 131
7.6 Independent Effect of $\beta$ and $k_{wq}$ ...................... 132
7.7 Effect of $\beta$ on Ripple and Iron Losses, 1,5kWM Machine with 4 Barriers133

8.1 SynRM: Number of Barriers Effect on Torque .................. 136
8.2 Promising Designs after Torque and Torque Ripple Optimization ................. 138
8.3 Solid Rotor’s Natural Flux Path ............................. 140
8.4 Summary of Tuning Steps, Geometries .......................... 142
8.5 Stress Distribution in Improved Machine Design, Final Rotor .................. 143
8.6 Prototyped SynRM of Improved Machine Design ............... 144
8.7 Heat-Run Test bench of IM and SynRM of Improved Machine Design ................. 145

9.1 FEM-Calculated Flux Lines in Solid Rotor .................. 150
9.2 Flux Lines in Solid Rotor and Non-Magnetic Shaft, and Analytical and FEM Flux Lines .................. 151
9.3 Mathematical Definition of the Parameters Describing the Flux Line in a 2p-pole Machine ............................ 152
9.4 SynRM: Proposed Multi-Barrier Rotor Geometry Based on Flux Lines in Solid Rotor .................. 153
9.5 End Point’s Angles and Arrangement ..................... 153
9.6 Parameter Definition for Barrier Sizing and Positioning in the Rotor .... 154
List of Figures

9.7 Per-unit MMF Distribution Over Segments in the q-axis and d-axis
MMF (without-cut-off Rotor Structure) .......................... 155
9.8 Effect of $\beta$ on Torque and Torque Ripple (Example) ............ 157
9.9 Effect of $k_{wq}$ on Torque, IPF and Torque Ripple (Example) ......... 158
9.10 Initial, Improved and Optimized Machine Design Geometries .... 158

10.1 Different Pole Number - Optimized Rotors ............................. 164
10.2 Rotor Geometry Optimization Steps for Different Pole Number .... 165
10.3 Different Pole Number Machines - Simulated Performances ......... 167
10.4 Effect of Pole Number on Flux Density Distribution .............. 168
10.5 Comparison between Different Pole Number and IM at Constant dT-rise 171

11.1 Torque Ripple v.s. Skew Angle and Steps for Improved Machine Design
SynRM ..................................................................... 174
11.2 Torque vs. Time Before and After Skewing for Improved Machine Design
SynRM ..................................................................... 175
11.3 Comparison of Calculation Techniques for Skew in the Improved Ma-
chine Design SynRM .................................................. 176
11.4 First and Last Side Views of the Skewed Rotor in the Improved Machine
Design SynRM ......................................................... 177
11.5 Torque vs. Time Before and After Continuous Skew, Skewing Angle
Sensitivity for Improved Machine Design SynRM .................... 178
11.6 Torque vs. Time Before and After Continuous Skew, Skewing Steps
Sensitivity for Improved Machine Design SynRM .................... 179
11.7 Electrical Circuit Implemented in FEM-Flux2D and Coils Connection
(1) ...................................................................... 180
11.8 Comparison of Different Voltage Source Synchronization Simulation
Methods ................................................................. 181
11.9 Torque, Current and back-EMF vs. Time at Rated Conditions ... 183
11.10 Stator Back, Stator Teeth and Rotor Iron Losses vs. Time at Rated
Conditions ................................................................ 184
11.11 Electrical Circuit Implemented in FEM-Flux2D and Coils Connection
(2) ...................................................................... 187
11.12 Flux Density Variation in Rotor Segment vs. Time at Rated Conditions 188
11.13 Torque, Back-EMF and Current vs. Time at No-Load Conditions ... 190
11.14 Torque, Back-EMF and Current and vs. Time at Locked-Rotor Condi-
tions ................................................................... 192
11.15 IM and SynRM Short-Circuit Currents in Locked-Rotor Test .... 195
11.16 Dynamic and Static Eccentricity in Electrical Machine .......... 197
11.17 Magnetic Pressures for SynRM Machine without Eccentricity .... 198
11.18 Total Electro-Magnetic Force on SynRM Rotor in one Mechanical Pe-
riod, Eccentricity Effect ............................................ 199
12.1 Loss Distributions of a Typical IM and SynRM, 15kWM Machine, 15 kW at 1500 rpm ...................................................... 204
12.2 Promising Designs, 90kWM Machine .................................. 206
12.3 Promising Designs, Optimization Steps, 90kWM Machine ........ 207
12.4 Promising Designs, 90kWM Machine ................................... 208
12.5 Best Promising Designs, Stress Calculation and Ribs Dimensioning, 90kWM Machine .................................................. 209
12.6 Best Promising Designs, Fine Tuning, 90kWM Machine .......... 210
12.7 Final Tuned Rotors Performance Comparison with Initial Design Machine SynRM, 90kWM Machine ............................. 211
12.8 Prototyped Machine Iron Sheet ........................................ 212
12.9 SynRM and IM Infra-Red Picture in Steady-State Operation, 15kWM Machine ............................................................... 214
12.10 SynRM Detailed Different Parts - Temperatures, Sensors Position ... 215
12.11 SynRM Detailed Different Parts - Temperatures, Rotary Sensors Position 216
12.12 SynRM, 15kWM Machine, Detailed Different Parts - Temperatures, as Function of Load at 3000 rpm ........................................... 217
12.13 Summary Comparison of Measurements for All Machines at 1500 rpm . 220
12.14 Summary Comparison of Measurements for All Sizes (1) ........ 221
12.15 Summary Comparison of Measurements for All Sizes (2) ........ 222
List of Tables

1.1 Heat-Run Test on SynRM IniM, SynRM OptM and IM Machines, 90kWM Machine ........................................... 12

3.1 PM Material and Relative Performance Comparison for PMaSynRM Machines in Figure 3.8 .................................. 80

5.1 Heat-Run Test Result on SynRM, IM and IPM, 15kWM Machine ........ 98

8.1 Promising Designs - Performance Comparison .......................... 139
8.2 Promising Design Performance Comparison - Summary of Tuning Steps 141
8.3 Heat-Run Test Result on SynRM (Improved Machine Design) and IM, 15kWM Machine ........................................ 146

9.1 Initial, Improved and Optimized Machine Designs Performance Comparison .............................................. 159
9.2 Heat-Run Test Result on SynRM (improved and optimized machine designs) and IM, 15kWM Machine ...................... 160

10.1 Different Pole Number - Calculation Sheet ............................... 169

11.1 V and I Sources Effect on Iron Losses ................................. 182
11.2 Summary of Locked Rotor Test Results on SynRM and IM .......... 196
11.3 Effect of Eccentricity on Iron Losses ................................... 200

12.1 SynRM, 15kWM Machine, Detailed Temperatures in Different Parts, as Function of Load at 3000 rpm .......................... 218

1 Heat-Run Test Result on SynRM, IM and IPM, 15kWM Machine at 500 rpm ....................................................... 229
2 Heat-Run Test Result on SynRM, IM and IPM, 15kWM Machine at 1500 rpm ....................................................... 230
3 Heat-Run Test Result on SynRM, IM and IPM, 15kWM Machine at 2500 rpm ....................................................... 231
Index

Airgap Length, 94

$g$, 94

Amount of Insulation, 84, 85, 106, 108, 126, 154

$l_a$, 85

Optimal Distribution, 105, 107, 110, 131

Amount of Iron, 84, 85, 106, 110, 126, 154

$l_y$, 85

Optimal Distribution, 107, 110

Amplitude Invariance, 24

Anisotropic, 23, 85, 105, 149

- Structure Modeling, 105

Homogeneous - Structure, 110, 126, 131

Armature-Reaction, 33

Balance Compensation, 58, 65, 68, 69, 72, 94

BC, 58, 94

Barrier, 34–36, 84, 107, 113–115, 136

Length, 110

$S_b$, 106, 110, 126, 154

Number of - , 107, 113–115, 127, 136

$k$, 110

Shape, 84, 106, 108, 126, 154

Width

in d-axis, 84, 89, 106, 111–113, 126

in d-axis $W_d$, 84, 106, 126

in q-axis, 84, 86, 89, 106, 110, 112, 113, 126, 131, 154

in q-axis $W_1$, 84, 106, 126, 154

Barrier Leg Angle, 84, 89

$\alpha$, 84

Barrier Position in q-axis, 84, 87, 90, 106, 126, 154

$Y_q$, 84

Bearing, 100, 219

Closed-Loop, 22

Conformal Mapping, 151

Constant d-axis Current, 41

CDAC, 41

Constant Power Speed Region, 30, 33, 56, 58

Constant Power Speed Range, 39

CPSR, 30

F, 58

Copper Losses, 47–52

Loading Effect, 47–52

Cross-Coupling, 24, 33, 59, 101

Current Angle, 22, 25, 32, 39, 47–52, 116, 118–122

$\theta$, 25

d-axis Flux Cross-Section, 84, 106, 111, 126

$l_d$, 84, 106, 111, 126

DC-link Voltage, 56

Demagnetization, 56, 71, 72

Direct on Line, 4, 38, 159, 193, 204

DOL, 4, 38

Direct Torque Control, 22, 144

253
DTC, 22
Eccentricity, 196, 197
Dynamic, 197
Static, 197
Efficiency, 23, 38, 47–52, 97, 145, 160, 161, 219
η, 23
Electrical Parameters Effect, 107, 116
Electro Motive Force, 24, 25, 30, 56, 183, 185, 190, 192
EMF, 24
Estimation, 21, 44
Field Orientated Control, 22, 39, 144
FOC, 22
Field Weakening, 30, 41, 56
Finite Element Method, 21, 31, 55, 58, 83, 85, 105, 125, 135, 157, 163, 173
FEM, 21
Flux, 31, 32, 47–52, 58, 150
d-axis, 24, 30, 150
Linkage, 24, 31, 59
λm, 59
Permanent Magnet, 58
PM, 58
PM, 58
q-axis, 24, 30, 115, 150
Circulating Component, 115
Going-Through Component, 115
Stator, 24, 31, 47–52

General Purpose Application, 2, 126, 203
Compressor, 2
Fan, 2, 203
GP-A, 2, 126
Heating, Ventilation and Air-Conditioning, 2
HVAC, 2
Pump, 2, 203
Generator, 23

Heat-Run, 12, 45, 93, 96, 98, 135, 144, 146, 159, 160, 203, 213, 214, 217–222, 229–231
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Heating, Ventilation and Air-Conditioning, 2
HVAC, 2
High Efficiency, 100
HE, 100
High Output, 100
HO, 100
High Speed, 23

Inductance, 21, 115, 117
d-axis, 21, 24
Ld, 21, 24
Leakage, 24, 30
q-axis, 21, 24
Lq, 21, 24
Induction Machine, 3, 21, 22, 37, 55, 125, 135, 149, 160, 161, 203
Efficiency, 38
IM, 21
Loss Distribution, 204
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Power, 205
Secondary Effects in -, 125
Slip, 37
Temperature
Housing, 214
Shaft, 214
Test, 12, 38, 96, 98, 135, 144, 146, 159, 160, 193–196, 203, 213, 219, 229–231
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
\( k_w \), 87
\( d \)-axis - , 106, 126
\( k_{wd} \), 88, 89, 106, 126
\( q \)-axis - , 106, 126, 154, 163
\( k_{wq} \), 85, 106, 126, 154
Interior Permanent Magnet Machine, 55, 93
Balance Compensation, 58, 94
DC-link Voltage, 56
Demagnetization, 56, 71, 72
Equivalent Circuit, 58
IPM, 55, 93
Measurement, 96, 98, 229–231
15kWM Machine, 98, 229–231
Natural Compensation, 57, 58
Over Compensation, 58
Power Factor, 61, 66, 67
Internal Power Factor, 61, 66, 67
Test, 96, 98, 229–231
15kWM Machine, 98, 229–231
Torque, 61, 63, 64, 66, 67
Uncontrolled Generator Mode, 56
Under Compensation, 58
International Electrotechnical Commission
IEC, 204
Inverter, 47–52, 101, 144, 146, 160, 219
-Size, 47–52, 55, 101, 146, 160, 219
Per-Unit Current, 146, 160, 219
Iron Losses, 47–52, 65, 125, 132, 133, 173, 182, 187, 188, 196, 200
Loading Effect, 47–52
Torque Ripple, 132
Isotropic, 23
Layers, 115
Number of - , 115
Lifetime, 100, 145, 160
Load Angle, 22, 25, 47–52
\( \delta \), 25
Locked-Rotor Short Circuit, 173, 191, 193–196
Loss Distribution, 204
Losses, 47–52
Distribution, 47–52
Loading Effect, 47–52
Loading Effect, 47–52
Lumped Equivalent Magnetic Circuit, 105
Magneto Motive Force, 22, 37, 109, 110, 128, 155, 156
\( d \)-axis, 109–111, 128, 155
MMF, 22
\( q \)-axis, 109, 110, 128, 129, 155, 156
Maximum Efficiency, 40, 41, 46–52
ME, 40
Maximum Power Factor, 41, 46–52, 65, 67
Maximum Internal Power Factor, 65
MIPF, 65
MPF, 41
Maximum Rate of Change of Torque, 40, 41
MRCT, 40
Maximum Torque per Ampere, 28, 30, 41, 45, 47–52, 55, 58, 65, 67, 116, 117
MTPA, 28
Maximum Torque per kVA, 26, 30, 41, 46–52, 116, 117
MTPkVA, 26
Maximum Torque per Volt, 28, 30, 41, 47–52
MTPV, 28, 41
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Natural Compensation, 57, 58, 68, 69, 72
NC, 57, 58
Optimization Procedure
Effect of Electrical Parameters, 107, 116, 131
Over Compensation, 58, 68, 69, 72
OC, 58
Overload, 23, 44
Park’s Equations, 21, 24
Permanent Magnet, 4, 39, 55, 203
Flux, 59
$\lambda_{PM}$, 59
Open Circuit Back-EMF, 70, 72
Open Circuit Flux, 62
$\lambda_{PM0}$, 62
Open Circuit Flux Density, 70, 72
PM, 4, 39, 55
Remanence Flux Density, 63
$B_r$, 63
Permanent Magnet assist SynRM, 23, 39, 55, 57, 60
Balance Compensation, 58
Demagnetization, 71, 72
Equivalent Circuit, 58
Natural Compensation, 57, 58
Over Compensation, 58
PMaSynRM, 23, 55
Power Factor, 61, 66, 67
Internal Power Factor, 61, 66, 67
Torque, 61, 63, 64, 66, 67
Under Compensation, 58
Permeance, 110, 131
- of Barrier $k^{th}$ $p_k$, 110
Airgap -, 127
Constant -, 110, 131
Inverse -, 131
Pole Number, 161
$2p$, 161
Pole Pair Number
$p$, 161
Power, 47–52, 100, 161, 205, 219
Interior Permanent Magnet Machine, 61, 66, 67
Internal Power Factor, 26, 32, 61, 66, 67
Interior Permanent Magnet Machine, 61, 66, 67
IPF, 26
Permanent Magnet assist SynRM, 61, 66, 67
Optimization, 105, 116
Permanent Magnet assist SynRM, 61, 66, 67
PF, 21
Power Factor Angle, 22, 25
$\phi$, 25
Power Size, 100
Prototype, 32, 38, 46, 56, 93, 94, 99, 135, 144, 149, 158, 160, 195, 196, 204, 212
Iron Sheet, 212
15kWM Machine, 99, 144, 214–216
INDEX

90kWM Machine, 212

Reluctance, 23
Rotor Iron Losses, 173, 184, 185, 188
Rotor Position Angle, 30
\( \vartheta \), 30
Rotor Slot Pitch, 34, 83, 84, 106, 110, 126, 129, 137, 153, 154
\( \alpha_m \), 83
Constant, 110, 126, 137
Rotor Slot Pitch Controller, 110, 113, 129, 133, 137, 153, 154, 163
\( \beta \), 110, 126

Saliency Ratio, 21, 25–27, 58, 59, 115, 117
\( \xi \), 21
Segment, 34–36, 84, 106, 110, 111, 126
Sensorless, 33, 38
Skew, 34, 37, 173, 174
Angle, 174, 178
\( \alpha \), 174
Steps, 174, 179
\( n \), 174
Slot, 126
Number, 126
Rotor: \( n_r \), 126
Stator: \( n_s \), 126
Slotting Effect, 33
Solid Rotor, 150, 154
Flux Line in, 150, 154
Start-Up, 173, 191
Supply Effect, 173, 177
Synchronous Reference Frame, 24
Synchronous Reluctance Machine, 1, 4, 21, 22, 37, 45, 55, 125, 135, 149, 161, 173, 203, 213
- With Multiple Interior Barrier, 106, 126, 154
Axially Laminated Anisotropic, 34, 93, 106
ALA, 34
Circle Operation Diagram, 40

Control, 28, 39, 45, 47–52
Development History, 35, 85, 105
Drive, 135, 144
Efficiency, 38, 47–52, 97, 145, 160, 219
Equivalent Circuit, 22
FEM Sensitivity Analysis, 83
Field Weakening, 30, 33, 42
Inverter
- Size, 47–52, 101, 146, 160, 219
Lifetime, 100, 145, 160
Loss Distribution, 204
Losses, 47–52
Copper, 47–52
Distribution, 47–52
Iron, 47–52
Loading Effect, 47–52
Macroscopic Parameters, 84, 85, 106, 126, 154
15kWM Machine, 98, 146, 160, 229–231
3kWM machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Microscopic Parameters, 84, 106, 126, 154
One Interior Barrier, 84
Operation Diagram, 28, 29
OpD, 28
Optimization
90kWM Machine, 205
Optimization Procedure, 105, 107, 108, 126, 135, 152
Fast Rotor - , 107, 125, 149, 152
Fine Tuning, 135, 139
Ripple, 126, 152
Torque, 108, 152
Overload, 44, 45
Parameter Estimator, 44
Permanent magnet assist SynRM, 23, 55
PMaSynRM, 55
Pole Number Effect, 161
Possible Operating Points, 28, 45, 47–52
Power, 47–52, 100, 205, 219
Rib, 34, 39, 93, 95, 136, 143, 209
Radial-, 95, 143, 209
Tangential-, 95, 137, 209
Salient Pole, 34, 84, 108
SP, 34
Saturation in, 31, 32
Secondary Effects in - , 125, 173
Eccentricity, 196
Iron Losses, 47–52, 125, 132, 133, 173, 187, 188, 196, 200
Locked Rotor Short Circuit, 173, 189
Noise, 125
Rotor Iron Losses, 125, 173
Skew, 173
Start-Up, 173, 189
Supply, 173, 177
Torque Quality, 173
Torque Ripple, 125
Vibration, 125
Slotting Effect in, 33
SynRM, 1, 21
Temperature
-Rise, 46, 99, 145, 160, 163, 219
Bearing, 215
Housing, 214, 215
Internal Air, 215
Rotor, 215
Shaft, 214, 215
Shaft - Rise, 145, 160
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Torque, 32, 47–52, 97, 145, 160, 219
Optimization, 105, 207
Torque Ripple
Optimization, 125, 137, 152, 207
Transversally Laminated Anisotropic, 34, 106
TLA, 34
with-cut-off, 84, 106, 113, 114, 136
without-cut-off, 84, 113, 114, 126, 136, 154
System Performance, 47–52, 144, 146, 160, 219
Efficiency, 47–52, 144, 160, 219
Temperature, 46, 99, 145, 160, 219
-Factor, $k_{cs}$, 163
Shaft, 145, 160
Housing, 214, 215
Infra-Red Picture, 214
Internal Air, 215
Rotor, 215
Shaft, 214, 215
Temperature Rise Factor, 163
$k_{cs}$, 163
Test, 12, 38, 45, 96, 98, 135, 144, 146, 159, 160, 193–196, 203, 213, 214, 217–222, 229–231
15kWM Machine, 98, 146, 160, 229–231
3kWM Machine, 220–222
90kWM Machine, 12
All Sizes, 220–222
Torque, 27, 37, 47–52, 61, 63, 64, 66, 67, 97, 105, 114, 115, 117, 118, 120, 136, 145, 160, 161, 219
Interior Permanent Magnet Machine, 61, 63, 64, 66, 67
Number of Barriers Effect, 115, 136
Number of Layers Effect, 115
Optimization, 105, 112, 136, 152, 207
Permanent Magnet assist SynRM, 61, 63, 64, 66, 67
Quality, 173
Ripple, 34, 110, 114, 115, 122, 125, 137, 207
Controller, 133
Number of Barriers Effect, 115
Number of Layers Effect, 115
Optimization, 125, 137, 152, 207
Stall, 39
Torque Angle, 22, 25, 37
$\beta$, 25
Torque Density, 23

Uncontrolled Generator Mode, 56, 58
UCG, 56
Under Compensation, 58, 68, 69, 72
UC, 58

Variable Speed Drive, 6, 93, 135, 161
VSD, 6, 93
The Alley

On a moonlit night, once again
Through the alley, we wandered, side by side.
Wings wide-open, in cherished solitude, soaring.
For a time, by the brook, resting.
You, all the world’s secrets in your black eyes, I, by your glances, mesmerized.

That night, I recalled
Through the alley, we wandered, side by side.
Wings wide-open, in cherished solitude, soaring.
For a time, by the brook, resting.
You, all the world’s secrets in your black eyes, I, by your glances, mesmerized.

Clear skies, quiet night,
Faith smiling, time tamed.
Moonlight, grapes pouring down into the water.
Tree branches, fingers reaching up to the moon.
The night, the meadow, flowers and rocks,
Silently charmed by the nightingale’s song.

Your words of warning, I recalled,
Avoid this love!
Behold this brook for a while!
Water mirrors timid love.
Today, you care for a glance of your lover,
But, tomorrow, your heart will belong to another.
Leave this town,
Forget this love.

How would I avoid this love,
I do not know how, I said.
How would I leave your side,
I cannot now, nor ever, I said.

That first day, my heart became a bird of desire.
Like a dove, I perched on your roof,
Rocks, you cast at me,
I did not fly away.
I did not fall apart.

A prairie deer am I, you the hunter.
Round your traps I wander and wander,
For to be captured by you, to surrender.

From a branch, a teardrop, falling.
A bitter moan, an owl, flying.
Tears in your eyes, gleaming.
Moon, at your love, beaming.

You fell silent, I recall.
Covered by a blanket of gloom,
I did not fly away.
I did not fall apart.

Many a night have passed in melancholy darkness.
You have abandoned your tormented lover.
You would not set foot in that alley again.
Oh, but how, but how,
Through the alley, I wandered, without you.