R Scott Semken

LIGHTWEIGHT, LIQUID-COOLED, DIRECT-DRIVE GENERATOR FOR HIGH-POWER WIND TURBINES: MOTIVATION, CONCEPT, AND PERFORMANCE

Thesis for the degree of Doctor of Science [Technology] to be presented with due permission for public examination and criticism in Auditorium 1383 at the Lappeenranta University of Technology, Lappeenranta, Finland on the 6th of March, 2015, at 12:00 pm.

Acta Universitatis
Lappeenrantaensis 629
Supervisor
Professor Aki Mikkola
Department of Mechanical Engineering
School of Energy Systems
Lappeenranta University of Technology
Finland

Reviewers
Professor Petri Kuosmanen
Department of Engineering Design and Production
Aalto University
Finland

Professor Wan-Suk Yoo
Department of Mechanical Engineering
Pusan National University
Korea

Opponents
Professor Petri Kuosmanen
Department of Engineering Design and Production
Aalto University
Finland

Professor Ole Balling
Department of Mechanical Engineering
Aarhus University
Denmark

ISBN 978-952-265-751-0
ISBN 978-952-265-752-7 (PDF)
ISSN-L 1456-4491
ISSN 1456-4491

Lappeenranta University of Technology
Yliopistopaino 2015
Abstract

R Scott Semken

LIGHTWEIGHT, LIQUID-COOLED, DIRECT-DRIVE GENERATOR FOR HIGH-
POWER WIND TURBINES: MOTIVATION, CONCEPT, AND PERFORMANCE
Lappeenranta, 2015
136 pages

Acta Universitatis Lappeenrantaensis 629
Dissertation. Lappeenranta University of Technology

ISBN 978-952-265-751-0
ISBN 978-952-265-752-7 (PDF)
ISSN-L 1456-4491
ISSN 1456-4491

Thesis: A liquid-cooled, direct-drive, permanent-magnet, synchronous generator with helical, double-layer, non-overlapping windings formed from a copper conductor with a coaxial internal coolant conduit offers an excellent combination of attributes to reliably provide economic wind power for the coming generation of wind turbines with power ratings between 5 and 20 MW. A generator based on the liquid-cooled architecture proposed here will be reliable and cost effective. Its smaller size and mass will reduce build, transport, and installation costs.

Summary: Converting wind energy into electricity and transmitting it to an electrical power grid to supply consumers is a relatively new and rapidly developing method of electricity generation. In the most recent decade, the increase in wind energy’s share of overall energy production has been remarkable. Thousands of land-based and offshore wind turbines have been commissioned around the globe, and thousands more are being planned. The technologies have evolved rapidly and are continuing to evolve, and wind turbine sizes and power ratings are continually increasing.

Many of the newer wind turbine designs feature drivetrains based on Direct-Drive, Permanent-Magnet, Synchronous Generators (DD-PMSGs). Being low-speed high-torque machines, the diameters of air-cooled DD-PMSGs become very large to generate higher levels of power. The largest direct-drive wind turbine generator in operation today, rated just below 8 MW, is 12 m in diameter and approximately 220 tonne. To generate higher powers, traditional DD-PMSGs would need to become extraordinarily large. A 15 MW
One alternative to increasing diameter is instead to increase torque density. In a permanent magnet machine, this is best done by increasing the linear current density of the stator windings. However, greater linear current density results in more Joule heating, and the additional heat cannot be removed practically using a traditional air-cooling approach. Direct liquid cooling is more effective, and when applied directly to the stator windings, higher linear current densities can be sustained leading to substantial increases in torque density. The higher torque density, in turn, makes possible significant reductions in DD-PMSG size.

Over the past five years, a multidisciplinary team of researchers has applied a holistic approach to explore the application of liquid cooling to permanent-magnet wind turbine generator design. The approach has considered wind energy markets and the economics of wind power, system reliability, electromagnetic behaviors and design, thermal design and performance, mechanical architecture and behaviors, and the performance modeling of installed wind turbines.

This dissertation is based on seven publications that chronicle the work. The primary outcomes are the proposal of a novel generator architecture, a multidisciplinary set of analyses to predict the behaviors, and experimentation to demonstrate some of the key principles and validate the analyses. The proposed generator concept is a direct-drive, surface-magnet, synchronous generator with fractional-slot, duplex-helical, double-layer, non-overlapping windings formed from a copper conductor with a coaxial internal coolant conduit to accommodate liquid coolant flow. The novel liquid-cooling architecture is referred to as LC DD-PMSG.

The first of the seven publications summarized in this dissertation discusses the technological and economic benefits and limitations of DD-PMSGs as applied to wind energy. The second publication addresses the long-term reliability of the proposed LC DD-PMSG design. Publication 3 examines the machine’s electromagnetic design, and Publication 4 introduces an optimization tool developed to quickly define basic machine parameters. The static and harmonic behaviors of the stator and rotor wheel structures are the subject of Publication 5. And finally, Publications 6 and 7 examine steady-state and transient thermal behaviors.
There have been a number of ancillary concrete outcomes associated with the work including the following.

✓ Intellectual Property (IP) for direct liquid cooling of stator windings via an embedded coaxial coolant conduit, IP for a lightweight wheel structure for low-speed, high-torque electrical machinery, and IP for numerous other details of the LC DD-PMSG design

✓ Analytical demonstrations of the equivalent reliability of the LC DD-PMSG; validated electromagnetic, thermal, structural, and dynamic prediction models; and an analytical demonstration of the superior partial load efficiency and annual energy output of an LC DD-PMSG design

✓ A set of LC DD-PMSG design guidelines and an analytical tool to establish optimal geometries quickly and early on

✓ Proposed 8 MW LC DD-PMSG concepts for both inner and outer rotor configurations

Furthermore, three technologies introduced could be relevant across a broader spectrum of applications. 1) The cost optimization methodology developed as part of this work could be further improved to produce a simple tool to establish base geometries for various electromagnetic machine types. 2) The layered sheet-steel element construction technology used for the LC DD-PMSG stator and rotor wheel structures has potential for a wide range of applications. And finally, 3) the direct liquid-cooling technology could be beneficial in higher speed electromotive applications such as vehicular electric drives.

Keywords: cooling system, copper winding, design for dynamics, direct cooling, direct-drive, electrical machine, form-wound winding, fractional-slot winding, generator, layered sheet steel, liquid cooling, non-overlapping winding, permanent magnet, radial flux, reliability analysis, synchronous machine, thermal analysis, thermal design, thermal management, wheel structure, wind turbine
Acknowledgements

The work reported here was carried out at the Lappeenranta University of Technology between 2009 and 2014 by a research team made up of members and mentors from the School of Technology departments of Energy and Mechanical Engineering and from the School of Industrial Engineering and Management's Department of Innovation Management.

Over these past 5 years, I have benefited from help given freely by each member of this research team. More importantly, I have enjoyed the personal relationships and will always carry warm memories of this time in my life.

Matti Lehtovaara, studying Innovation Management with guidance from Professor Tuomo Kässi, helped us to better understand the economics and market realities of wind power. Pekka Röyttä and Professor Jari Backman from the Laboratory of Fluid Dynamics and Maria Polikarpova, Yulia Alexandrova, and Dr. Janne Nerg from the Laboratory of Electrical Drives Technology helped us to better understand the relevant thermal management issues for our proposed generator design. Yulia Alexandrova, with guidance from both Professor Juha Pyrhönen and Dr. Janne Nerg, helped us by developing and carrying out the complex analyses needed to determine the electromagnetic design of our novel generator concepts.

From the Laboratory of Machine Design, Professor Aki Mikkola and Professor Jussi Sopanen gave invaluable advice and analytical guidance as we conceptualized and studied mechanical aspects in terms of both structural and dynamic performance. My other colleagues within the laboratory, via numerous discussions and the exchange of many ideas, have also been an important asset and a very big help to me in this work.

I thank the Lappeenranta University of Technology for supporting my postgraduate studies and for providing a peaceful and beautiful venue for this research work. Furthermore, I am thankful for the substantial financial support made available to us by the Academy of Finland, the Finnish Funding Agency for Innovation (Tekes), and the European Commission.

I am especially grateful to my supervisor Professor Aki Mikkola, who gave me the opportunity to work at the university and persuaded me to pursue my postgraduate studies. Aki has always given of his time and energy and has worked to keep me focused on completing this most challenging of academic endeavors. This dissertation would not have been completed without his help. I also extend special thanks to Professor Juha Pyrhönen, who has been the tireless champion of the liquid-cooled, direct-drive wind turbine generator idea from the beginning.
My sincerest thanks to Professor Wan-Suk Yoo from Pusan National University and Professor Petri Kuosmanen from Aalto University. Each took the time to review this dissertation and offered valuable comments and constructive advice.

Finally, I want my wife Tiina and my children Kaija, Sakari, Jaakob, and Leila to know that I understand and appreciate the sacrifices they have had to make over the years as I focused so intently on the work.

Lappeenranta, March 2015

R Scott Semken
# Contents

1 Introduction 17
   1.1 Present Wind Energy Technology 18
   1.2 The Size Evolution of Wind Turbines 20
   1.3 Wind Turbine Economics 22
   1.4 The Low-Speed Generator Size Dilemma 24
   1.5 Outline of the Dissertation 25
   1.6 Methodology, Thesis, and Scientific Contributions 29

2 Configuration Basics and the Market Challenge 31
   2.1 Basic Requirements 31
   2.2 Market Challenge 40

3 Presentation of Proposed Generator Concept 45
   3.1 Benefits to Wind Power Generation (Pub 1) 45
   3.2 Proposed Embodiment of an 8 MW LC DD-PMSG 52

4 Analytical and Experimental Evaluation of LC DD-PMSG Concept 79
   4.1 Predicted Reliability of an LC DD-PMSG Design (Pub 2) 79
   4.2 Electromagnetic Characteristics of an LC DD-PMSG (Pub 3) 85
   4.3 Optimal EM Geometries for an LC DD-PMSG (Pub 4) 95
   4.4 Mechanical Behaviors for the Wheel Structures (Pub 5) 101
   4.5 Thermal Design and Analysis of the Concept (Pub 6) 110
   4.6 Thermal Behavior of the LC DD-PMSG (Pub 7) 117

5 Concluding Remarks and Recommendations 125

References 129

Publications

ACTA UNIVERSITATIS LAPPEENRANTAESIS
Publication 1


Publication 2


Publication 3


Publication 4


Publication 5


Publication 6

Publication 7

# NOMENCLATURE

## VARIABLES

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>availability</td>
<td></td>
</tr>
<tr>
<td>A</td>
<td>linear current density</td>
<td>A/m</td>
</tr>
<tr>
<td>a</td>
<td>number of functional elements</td>
<td></td>
</tr>
<tr>
<td>AJ</td>
<td>heating factor</td>
<td>A²/m⁳</td>
</tr>
<tr>
<td>B</td>
<td>magnetic flux density</td>
<td>T</td>
</tr>
<tr>
<td>b</td>
<td>number of nonfunctional elements</td>
<td></td>
</tr>
<tr>
<td>C_P</td>
<td>turbine rotor power coefficient</td>
<td></td>
</tr>
<tr>
<td>d</td>
<td>diameter</td>
<td>m</td>
</tr>
<tr>
<td>F</td>
<td>force</td>
<td>N</td>
</tr>
<tr>
<td>f</td>
<td>frequency</td>
<td>Hz</td>
</tr>
<tr>
<td>i</td>
<td>the index of a summation</td>
<td></td>
</tr>
<tr>
<td>J</td>
<td>current density</td>
<td>A/m²</td>
</tr>
<tr>
<td>j</td>
<td>index of an iteration</td>
<td></td>
</tr>
<tr>
<td>k</td>
<td>coefficient, factor, parameter</td>
<td></td>
</tr>
<tr>
<td>l</td>
<td>length</td>
<td>m</td>
</tr>
<tr>
<td>l/d</td>
<td>generator aspect ratio (ratio of rotor active length to rotor diameter)</td>
<td>m/m</td>
</tr>
<tr>
<td>m</td>
<td>mass</td>
<td>kg</td>
</tr>
<tr>
<td>m</td>
<td>number of phases</td>
<td></td>
</tr>
<tr>
<td>m*</td>
<td>active mass objective function (optimization algorithm)</td>
<td>kg</td>
</tr>
<tr>
<td>n</td>
<td>design variable order number (optimization algorithm)</td>
<td></td>
</tr>
<tr>
<td>n</td>
<td>rotor speed, synchronous speed</td>
<td>rpm</td>
</tr>
<tr>
<td>n</td>
<td>upper bound number of summation, design variable order number</td>
<td></td>
</tr>
<tr>
<td>p</td>
<td>number of rotor pole pairs</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>power</td>
<td>W</td>
</tr>
<tr>
<td>P</td>
<td>pressure</td>
<td>Pa</td>
</tr>
<tr>
<td>q</td>
<td>number of slots per pole and phase</td>
<td></td>
</tr>
<tr>
<td>R</td>
<td>reliability</td>
<td></td>
</tr>
<tr>
<td>R</td>
<td>resistance, thermal</td>
<td>K/W</td>
</tr>
<tr>
<td>r</td>
<td>radius</td>
<td>m</td>
</tr>
<tr>
<td>T</td>
<td>temperature</td>
<td>°C</td>
</tr>
<tr>
<td>t</td>
<td>time</td>
<td>s</td>
</tr>
<tr>
<td>U</td>
<td>voltage</td>
<td>V</td>
</tr>
<tr>
<td>UA</td>
<td>unavailability</td>
<td></td>
</tr>
<tr>
<td>w</td>
<td>width</td>
<td>m</td>
</tr>
<tr>
<td>α</td>
<td>decreasing scaling factor (optimization algorithm)</td>
<td></td>
</tr>
<tr>
<td>β</td>
<td>angle (of the wind turbine rotor blade)</td>
<td>rad</td>
</tr>
<tr>
<td>γ</td>
<td>design variable</td>
<td></td>
</tr>
<tr>
<td>γ</td>
<td>design variables matrix</td>
<td></td>
</tr>
<tr>
<td>δ</td>
<td>load angle</td>
<td>rad</td>
</tr>
<tr>
<td>Δ</td>
<td>discretization steps matrix</td>
<td></td>
</tr>
<tr>
<td>ζ</td>
<td>phase angle between the current and voltage</td>
<td>rad</td>
</tr>
<tr>
<td>Symbol</td>
<td>Description</td>
<td>Unit</td>
</tr>
<tr>
<td>--------</td>
<td>-----------------------------------------------------------------------------</td>
<td>-------</td>
</tr>
<tr>
<td>η</td>
<td>efficiency</td>
<td>%</td>
</tr>
<tr>
<td>λ</td>
<td>a ratio, a quotient</td>
<td></td>
</tr>
<tr>
<td>µ</td>
<td>repair intensity</td>
<td>yr⁻¹</td>
</tr>
<tr>
<td>ν</td>
<td>speed</td>
<td>m/s</td>
</tr>
<tr>
<td>ξ</td>
<td>calculated values of the design variables (optimization algorithm)</td>
<td></td>
</tr>
<tr>
<td>ξ</td>
<td>calculated values matrix of design variables (optimization algorithm)</td>
<td></td>
</tr>
<tr>
<td>ρ</td>
<td>electrical resistivity</td>
<td>Ωm</td>
</tr>
<tr>
<td>ρ</td>
<td>mass density</td>
<td>kg/m³</td>
</tr>
<tr>
<td>σ</td>
<td>stress</td>
<td>Pa</td>
</tr>
<tr>
<td>τ</td>
<td>torque</td>
<td>Nm</td>
</tr>
<tr>
<td>τₚ</td>
<td>pole pitch</td>
<td>m</td>
</tr>
<tr>
<td>Ω</td>
<td>angular velocity</td>
<td>rad/s</td>
</tr>
<tr>
<td>ω</td>
<td>failure intensity</td>
<td></td>
</tr>
</tbody>
</table>

**CONSTANTS**

π: the number Pi

**SUBSCRIPTS**

- air: air
- am: active materials
- cu: copper
- E: electromotive force
- fe: electrical steel
- flow: fluid flow
- gap: air gap
- gen: generator
- ini: initial
- inl: inlet
- J: current density
- max: maximum
- mid: middle
- new: new
- oil: oil coolant
- old: previous value
- out: outlet
- p: electromagnetic pole
- P: power
- peak: peak (highest value)
- pm: permanent magnet
- R: resistance
- r: generator rotor
- rad: radial
- rb: rotor blades of the wind turbine
s  generator stator
S  system
str  structural
t  tooth
tan  tangential
w  windings
we  windings, ends
wh  winding, harmonic
y  yoke
τ  torque

OTHERS
!

factorial operator
∞
direct proportionality operator
∞
infinity
∆
difference operator
∈
set membership operator

ACRONYMS

AEO  Annual Energy Output
ASTM  American Society for Testing and Materials
CARB  Compact Aligning Roller Bearing
CFD  Computational Fluid Dynamics
DD-PMSG  Direct-Drive Permanent-Magnet Synchronous Generator
DFIG  Doubly Fed Induction Generator
EESG  Electrically Excited Synchronous Generator
EMA  Experimental Modal Analysis
FE  Finite Element
GE  General Electric
HDPE  High Density Polyethylene
HTSC  High-Temperature Superconductor
IEC  International Electrotechnical Commission
IP  International Protection Marking; Intellectual Property
LC DD-PMSG  Liquid-Cooled Direct-Drive Permanent-Magnet Synchronous Generator
LCM  Least Common Multiple
LPTN  Lumped Parameter Thermal Network
MDT  Mean Down Time (hours)
MTBF  Mean Time Between Failure (years)
MTTF  Mean Time To Failure (years)
NdFeB  neodymium magnet material (neodymium, iron and boron)
NEMA  National Electrical Manufacturers Association (USA)
NREL  National Renewable Energy Laboratory (USA)
OEM  Original Equipment Manufacturer
<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Full Form</th>
</tr>
</thead>
<tbody>
<tr>
<td>PAO</td>
<td>polyalphaolefin (heat transfer fluid)</td>
</tr>
<tr>
<td>PMSG</td>
<td>Permanent-Magnet Synchronous Generator</td>
</tr>
<tr>
<td>SGS</td>
<td>Société Générale de Surveillance</td>
</tr>
<tr>
<td>SKF</td>
<td>Svenska Kullagerfabriken AB</td>
</tr>
<tr>
<td>SLDV</td>
<td>Scanning Laser Doppler Vibrometer</td>
</tr>
<tr>
<td>SRB</td>
<td>Spherical Roller Bearing</td>
</tr>
<tr>
<td>SS</td>
<td>Stainless Steel</td>
</tr>
<tr>
<td>USA</td>
<td>United States of America</td>
</tr>
</tbody>
</table>
Wind power has been used for millennia to carry out tasks such as grinding grain (windmills) and pumping water. However, generating electricity and transmitting it to an electrical power grid to supply consumers is relatively new. The first such wind turbine began operation in the 1940s in the United States of America (USA). The 1.25 MW unit fed electric power to a local utility network in Vermont during World War II. However, it was not until the 1970s, when oil prices began to rapidly rise, that interest in wind turbines grew and research into wind energy technologies accelerated [46, 62]. Wind energy technology for mainstream electrical power production is in its infancy, and technological developments are still being made at a rapid pace.

The recent growth of wind energy is remarkable, and it plays an increasingly important role in energy planning for Europe, the USA, and Asia. In 2007, the European Union endorsed the European Strategic Energy Technology Plan to accelerate the development of renewable energy technologies. Included in the plan are initiatives to increase wind energy’s share of overall European Union energy production to 20% by the year 2020 [23]. In 2009, there was more new power capacity from wind technology installed in European countries than from any other electricity production technology. Of a total 26 GW new capacity, 39% was wind power [24]. To reach the year 2020 goal, 100-200 GW over that capacity will be needed [6].

The USA is also pushing hard to increase the contribution of wind energy electricity production, calling for an increase to 20% of total US electricity production capacity by 2030 [60]. This represents an increase of approximately 300 GW from 2008 wind power production levels. In Asia, China plans to reach a wind power production capacity of nearly 100 GW by 2020 [69], and Japan and South Korea are both engaged in aggressive developments of additional wind power capacity. Other significant potential markets
include Latin America, the former Soviet Union, and Africa. These markets have been experiencing rapid growth in the demand for electricity, and their demand for wind power could surpass both Europe and the USA in the next 15 years [6].

1.1 Present Wind Energy Technology

A typical large electrical power generating wind turbine uses a drivetrain with a large three-bladed main rotor and an electrical generator sitting on top of a tall tubular tower. Normally, the drivetrain axis is horizontal, and the main rotor faces the wind. Variable speed operation with pitch control is standard [21]. The 3 MW wind turbine shown in Figure 1.1 is an example.

There is no clear consensus on the most appropriate drivetrain type or generator technology. In the product catalogs of major wind turbine manufacturers, there are systems based on doubly fed induction, electrically excited synchronous, or permanent magnet synchronous generator architectures. The multi-bladed main rotor may be coupled to a high-speed generator through a multiple-stage gearbox (1:100) or to a medium-speed generator through a single-stage gearbox (1:10). Several of the latest designs couple the main rotor directly to a high-torque generator designed to operate at low rotational speeds. These direct-drive systems do away with the gearbox altogether.

Most currently installed wind turbines are land based. However, there is a move to offshore wind farms to utilize the stronger offshore winds and to permit running at higher rotor blade speeds with less concern over noise pollution [38, 43]. This move is making high reliability and low maintenance operation increasingly important requirements [10, 21, 51, 56].

Many of the newest designs offered by major wind turbine manufacturers are based on the Permanent-Magnet Synchronous Generator (PMSG) architecture. For example, Vestas, GE Wind, Goldwind, Siemens, and Gamesa have all introduced large systems featuring PMSGs intended for offshore use. These large permanent magnet generators offer advantages including lower weight, improved thermal performance, and higher efficiency and energy yield [7, 8, 20, 37].

The number of direct-drive wind turbine installations seems to be increasing. Over the past 15 years, hundreds of direct-drive units have gone into operation in Germany, Denmark, and Spain. In the USA and China, many of the newer installations have also been based on direct-drive generator architectures. The latest offshore wind turbine product offerings from GE Wind, Goldwind, and Siemens all use Direct-Drive Permanent-Magnet
Figure 1.1 Horizontal axis wind turbine (courtesy of Vestas Wind Systems A/S)
Synchronous Generators (DD-PMSGs). Figure 1.2 is an artist’s rendering showing the main rotor blade hub, the generator, and the nacelle for a hypothetical direct-drive wind turbine.

Figure 1.2 Rendering of generic direct-drive wind turbine (model by Jeff Lewis)

1.2 The Size Evolution of Wind Turbines

As wind power technologies and the wind energy industry have matured over the past 40 years, power ratings have grown continuously. There are four important benefits that are driving this growth.

A wind turbine converts the power of the wind that passes through the swept area of its rotor blades. If the diameter of the swept area doubles, swept area increases by a factor of four. If wind speed doubles, wind power increases by a factor of eight (velocity cubed). Consequently, wind turbines are getting larger because 1) bigger wind turbines can accommodate larger rotor blade diameters, and 2) they stand higher, reaching up to where stronger winds blow [67].
1.2 The Size Evolution of Wind Turbines

Furthermore, 3) economies of scale are driving wind turbine growth. As wind turbines grow larger, their investment costs drop. For example, a 1% increase in size typically requires only a 0.6% increase in the costs of manufacture [12]. Finally, 4) as recent studies report, larger wind turbines result in a lower per megawatt environmental impact [11].

The average size of today’s new wind turbine offerings is moving rapidly to 6 MW and beyond [18, 61]. The largest units currently in operation are the 7.6 MW Enercon E-126 and the 8 MW Vestas V164. Several recent wind turbine development projects are targeting 6 MW and higher. Table 1.1 summarizes the more notable projects, both completed and in progress [4].

Table 1.1 Recent Large Wind Turbine Development Projects

<table>
<thead>
<tr>
<th>Manufacturer</th>
<th>Power</th>
<th>Generator</th>
<th>Drivetrain Type</th>
<th>Rotor $d_{rb}$</th>
<th>Availability</th>
</tr>
</thead>
<tbody>
<tr>
<td>Siemens</td>
<td>6.0 MW</td>
<td>PMSG</td>
<td>Direct-Drive</td>
<td>154 m</td>
<td>production</td>
</tr>
<tr>
<td>Sinovel</td>
<td>6.0 MW</td>
<td>DFIG¹</td>
<td>High-Speed</td>
<td>128 m</td>
<td>production</td>
</tr>
<tr>
<td>Alstom</td>
<td>6.0 MW</td>
<td>PMSG</td>
<td>Direct-Drive</td>
<td>150 m</td>
<td>2015</td>
</tr>
<tr>
<td>Senvion</td>
<td>6.2 MW</td>
<td>DFIG</td>
<td>High-Speed</td>
<td>152 m</td>
<td>production</td>
</tr>
<tr>
<td>Enercon</td>
<td>7.6 MW</td>
<td>EESG²</td>
<td>Direct-Drive</td>
<td>127 m</td>
<td>production</td>
</tr>
<tr>
<td>Vestas</td>
<td>8.0 MW</td>
<td>PMSG</td>
<td>Medium-Speed</td>
<td>164 m</td>
<td>production</td>
</tr>
<tr>
<td>AMSC</td>
<td>10.0 MW</td>
<td>HTSC³</td>
<td>Direct-Drive</td>
<td>190 m</td>
<td>2015</td>
</tr>
<tr>
<td>Sway Turbine</td>
<td>10.0 MW</td>
<td>PMSG</td>
<td>Direct-Drive</td>
<td>145 m</td>
<td>2015</td>
</tr>
<tr>
<td>Gamesa</td>
<td>15.0 MW</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
<td>2020</td>
</tr>
</tbody>
</table>

¹ Doubly-Fed Induction Generator
² Electrically Excited Synchronous Generator
³ High-Temperature Superconductor

Projections suggest that wind turbine size will continue to increase, reaching rated powers of 20 MW and blade diameters of 250 m by 2025. A 20 megawatt wind turbine is an impressively large structure. Figure 1.3 compares typical wind turbine sizes. Currently, wind turbines are typically in the 3-5 MW range.
1.3 Wind Turbine Economics

Wind energy is capital intensive. More than 75% of the total cost of energy for a wind farm is the initial capital expenditure for the wind turbines. Table 1.2 breaks down the cost structure for a typical 2 MW wind turbine installed in Europe in 2006 [36].

Actual cost-per-MW wind turbine prices are quite difficult to determine as they depend on numerous factors such as where the wind turbines are manufactured, where they will be installed, and how the deal is structured between the manufacturer and the wind farm developer. However, examining reported financial figures from various wind turbine manufacturers reveals some approximate pricing.

China’s Ming Yang, which delivers only land-based wind turbines, expects prices in 2015 to be around $650,000 per megawatt. Vestas, a Danish manufacturer delivering both land-based and offshore wind turbines, reports 2013 wind turbine prices of approximately
1.3 Wind Turbine Economics

Table 1.2 Cost Structure for a Typical Wind Turbine Installation in Europe

<table>
<thead>
<tr>
<th>% of Total</th>
</tr>
</thead>
<tbody>
<tr>
<td>Wind Turbine</td>
</tr>
<tr>
<td>Grid Connection</td>
</tr>
<tr>
<td>Foundation</td>
</tr>
<tr>
<td>Land Rental</td>
</tr>
<tr>
<td>Electric Installation</td>
</tr>
<tr>
<td>Consultancy</td>
</tr>
<tr>
<td>Financial Costs</td>
</tr>
<tr>
<td>Road Construction</td>
</tr>
<tr>
<td>Control Systems</td>
</tr>
<tr>
<td><strong>Total</strong></td>
</tr>
</tbody>
</table>

€970,000 per megawatt. And, Suzlon of India is delivering a mixture of land-based and offshore wind turbines at an average of €990,000 per megawatt. The USA National Renewable Energy Laboratory (NREL) reports premium pricing for offshore wind turbines of around $1,250,000 per megawatt [58].

The total capital cost of a wind turbine includes the cost of manufacture, the cost of logistics, the cost of assembly and installation, and the cost of decommissioning.

Manufacturing costs can be broken down across seven categories: 1) the main rotor and hub, 2) the generator and gearbox, 3) the variable speed electronics, 4) the electrical connections, 5) the nacelle structure and housing, 6) other auxiliary components, and 7) the tower. Table 1.3 lists these categories, giving their approximate percentage contribution to total manufacturing cost for a hypothetical 3 MW land-based wind turbine. Total cost of manufacture for this hypothetical turbine is $2.3 million. The wind turbine design cost and scaling model prepared for the NREL in 2006 was used to estimate the percentages shown in the table [26].

Table 1.3 Manufacturing Cost Breakdown for 3 MW Wind Turbine in 2006

<table>
<thead>
<tr>
<th>% of Total</th>
</tr>
</thead>
<tbody>
<tr>
<td>Main Rotor and Hub</td>
</tr>
<tr>
<td>Generator (Direct-Drive or with Gearbox)</td>
</tr>
<tr>
<td>Variable Speed Electronics</td>
</tr>
<tr>
<td>Electrical Connections</td>
</tr>
<tr>
<td>Nacelle Structure and Housing</td>
</tr>
<tr>
<td>Other Auxiliary Components</td>
</tr>
<tr>
<td>Wind Turbine Tower</td>
</tr>
<tr>
<td><strong>Total</strong></td>
</tr>
</tbody>
</table>
As the table reveals, the generator system makes up a full quarter ($620,000) of the total manufacturing costs for this typical land-based 3 MW unit.

To illustrate the major systems typical of a modern wind turbine, Figure 1.4 offers a nacelle cutaway view of the General Electric (GE) offshore direct-drive wind turbine with major components labeled.

Figure 1.4 A nacelle cutaway view for a direct-drive wind turbine showing major components (courtesy of GE Power and Water)

1.4 The Low-Speed Generator Size Dilemma

A direct-drive wind turbine generator based on a traditional air-cooled architecture must be very large in diameter to realize power ratings of 6 MW or more. With the generator rotor turning as slowly as the main rotor blade (10-20 rpm), the direct-drive generator must develop a very high level of torque to produce a high level of power. Until recently, the most practical way of producing this high torque level has been to increase generator
1.5 Outline of the Dissertation

The dissertation reports on research and development work carried out from 2010 to 2014 to develop an understanding of wind energy and the wind energy industry. Main goals included evaluating the state-of-the-art in wind turbine generator technology, identifying opportunities for improvement, and then proposing specific generator solutions to improve the costs, efficiencies, and overall usability of wind turbines.

The work has culminated in a proposed concept based on a direct-drive permanent-magnet synchronous generator. The unique attribute of the proposed concept is its reliance on direct liquid cooling of the stator windings for thermal management. The generator implied by the proposed concept is referred to as an LC DD-PMSG. The research and development team has concluded that the LC DD-PMSG architecture offers an excellent combination of attributes making it suitable for future wind energy demands. The proposed LC DD-PMSG concept features a simple electromagnetic topology that produces higher torque density, enables reliable liquid cooling, and results in minimal generator size and weight, workable logistics, and easy assembly.
Four additional chapters follow this introductory Chapter 1 to summarize the work reported in seven original publications. Chapter 2 describes how and why the basic requirements for a new wind energy generator solution were established. Chapter 3 discusses the benefits of taking a new approach (Publication 1) and presents the proposed conceptual embodiment of an 8 MW LC DD-PMSG. Chapter 4 describes the analytical and numerical predictions made and the experimentation carried out to examine the behaviors of the introduced LC DD-PMSG design in the form of summaries of Publications 2 through 7. And finally, Chapter 5 offers conclusions and recommendations for continued research.

The areas of research involved in the body of work reported here include:

- wind energy markets and the economics of wind power,
- system reliability,
- electromagnetic behaviors and design,
- thermal design and performance,
- mechanical architecture and behaviors, and
- performance modeling of installed wind turbines.

Publication 1 reviews the technological and economic benefits and limitations of direct-drive permanent-magnet synchronous generators. Their benefits and physical and economic limitations are examined, and their appropriateness as a key piece in the overall wind turbine system design is considered. The publication looks at why these generators are becoming so big, then proposes an architectural variation, a DD-PMSG that relies on direct liquid cooling of the stator windings, and promises a more compact, more economical, and more reliable wind turbine drivetrain.

R Scott Semken was the principal author for this publication, which was a joint effort by research team members under the mentorship of Professors Juha Pyrhönen, Aki Mikkola, and Jari Backman. R Scott Semken analyzed the current wind energy situation. Dr. Maria Polikarpova investigated wind turbine costs. Drs. Yulia Alexandrova and Janne Nerg, were responsible for electromagnetics content. Dr. Pekka Röyttä, Dr. Maria Polikarpova, and R Scott Semken were responsible for thermal engineering content. Airflow power analysis was carried out by Dr. Pekka Röyttä, and other mechanical engineering aspects were covered by R Scott Semken.

Publication 2 addresses the question of LC DD-PMSG reliability. It presents a reliability analysis for an 8 MW embodiment of the proposed generator architecture including primary and secondary liquid-cooling systems. LC DD-PMSG reliability is calculated analytically and assessed based on Mean Time Between Failures (MTBF), Mean Time To Failure (MTTF), Mean Down Time (MDT), failure intensity, and availability.
The publication was primarily the work of Dr. Maria Polikarpova with R Scott Semken providing technical guidance. Professor Juha Pyrhönen provided oversight. Prior to this publication, Advait Krishna carried out a similar reliability study for his Master’s degree thesis, which was entitled Reliability Analysis of Liquid Cooled Direct Drive Permanent Magnet Synchronous Generators. R Scott Semken was a co-examiner for Krishna’s thesis, which was a helpful precursor to the work reported by this publication.

**Publication 3** evaluates a proposed conceptual design solution for an 8 MW LC DD-PMSG and examines key aspects related to the design including tangential stress, current density, linear current density, heating factor, and generator efficiency at full and partial load. The performance characteristics of a variable-speed wind turbine are determined based on the proposed LC DD-PMSG drivetrain in terms of annual energy production and load factor for a particular set of wind conditions.

Dr. Yulia Alexandrova was the principal author and investigator for this publication and is responsible for its analyses. Professor Juha Pyrhönen supervised the work and helped to refine the publication. R Scott Semken developed the overall LC DD-PMSG architecture and designed, set up, and carried out the test procedures for a small-scale prototype stator-cooling loop and data acquisition system used to validate analytical predictions.

**Publication 4** describes the development of a simple MATLAB®-based tool that employs a direct search method with variable step size to define, based on performance requirements, the basic parameters needed to begin the design of an LC DD-PMSG optimized for minimum material cost and mass. The output of this tool, combined with an existing set of LC DD-PMSG design guidelines, makes for quick convergence on an appropriate generator geometry.

Dr. Yulia Alexandrova was responsible for the development of the MATLAB®-based tool and is the primary author of the publication. The work was supervised by Professor Juha Pyrhönen, who also helped to refine the publication. Dr. Maria Polikarpova covered thermal engineering aspects. R Scott Semken helped to establish the basic requirements for the tool, developed the mechanical design guidelines, and defined the LC DD-PMSG conceptual design architecture.

**Publication 5** examines the mechanical performance aspects of the unique wheel structures designed into the proposed LC DD-PMSG concept. The dominant forces in a large operating PMSG are the magnetic attraction forces that act radially and the torque forces that act tangentially between the rotor and stator. The stator and rotor wheel structures must withstand these large forces and maintain a constant and uniform rotor-to-stator air gap. Wheel structure design for a large PMSG is more about managing deformation than
about limiting stresses. The slanted spoke and rim architecture of the wheel structures promises to provide adequate rotor-to-stator air gap management without all the extra steel. The publication reports on the static structural Finite Element (FE) analysis of a full-scale LC DD-PMSG stator wheel that verifies structural performance. Next, it describes FE modal analyses and an Experimental Modal Analysis (EMA) for a ¼-scale model that are used to examine dynamic behaviors.

R Scott Semken was the principal author and investigator for this publication and is responsible for the analyses. Professors Aki Mikkola and Jussi Sopanen supervised the work. R Scott Semken designed the actual and prototype wheel structures and the prototype EMA. FE modeling, building of the prototype, and setting up the EMA were all carried out by R Scott Semken and by Charles Nutakor, who also took the EMA measurements and helped interpret the results.

Publication 6 examines the steady-state thermal behaviors of the proposed 8 MW LC DD-PMSG concept using three different analytical methods: Finite Element (FE), Computational Fluid Dynamics (CFD), and Lumped Parameter Thermal Network (LPTN). Predictions are made using each of the thermals models and the results are compared. The influence of passive air cooling of the rotor surface magnets can be seen from the CFD thermal analysis results.

Dr. Maria Polikarpova was the principal author and investigator for this publication and is responsible for its analyses. Professor Juha Pyrhönen supervised the work. R Scott Semken developed the overall LC DD-PMSG architecture and designed, set up, and carried out the test procedures for a small-scale prototype stator-cooling loop and data acquisition system used to validate analytical predictions.

Publication 7 continues the examination of thermal behaviors for the proposed LC DD-PMSG design. A more exact LPTN model is developed that includes details of the stator slot and coolant flow configuration to account for the uneven distribution of heating in those areas. LC DD-PMSG temperatures predicted by the model are compared to those seen in existing liquid-cooled generators and against two-dimensional FE analysis results. Next, a transient thermal analytical model is prepared to predict time-dependent temperature distributions. Transient calculations are carried out to predict LC DD-PMSG changing windings temperatures for overcurrent and loss-of-coolant event scenarios and for a real-world duty cycle. Both analytical thermal models are validated with measurements taken from an instrumented prototype comprising two LC DD-PMSG duplex-helical tooth-coil windings embedded in a lamination stack and integrated within a liquid coolant loop.
1.6 Methodology, Thesis, and Scientific Contributions

Dr. Yulia Alexandrova was the principal author and investigator for this publication and is responsible for theoretical analysis, the implementation of the proposed methods in MATLAB®, and theoretical analysis of the experimental results. Professor Juha Pyrhönen was responsible for the supervision of the project and refinement of the publication. R Scott Semken developed the overall LC DD-PMSG architecture and designed, set up, and carried out the test procedures for the prototype and data acquisition system used to validate analytical predictions.

1.6 Methodology, Thesis, and Scientific Contributions

Research Methodology: An important objective of this research work was to enable the achievement of remarkably greater electromagnetic forces in wind turbine generators by developing and applying new holistic methodologies that integrate commercial, electromagnetic, thermal, structural, and manufacturing aspects. To this end, researchers from various laboratories came together to combine research efforts and develop common analytical modeling tools capable of virtually and quickly prototyping any number of novel generator architectures and comparing their relative merits.

Elements of the investigative research included state-of-the-market and state-of-the-technology analyses, as well as a thorough analysis of costing and wind turbine economics. Common wind turbine generator architectures were researched in terms of electromagnetics, thermal management, mechanical design, materials, manufacturing approaches, and material cost structures. Equally important to the approach, there were continual face-to-face discussions with prominent players from the wind energy industry.

As understanding of existing wind energy technologies grew and the direction in which the industry was heading became more clear, a few critical hurdles became evident causing the team to focus their creative energies on finding directed solutions. Once solutions had been hypothesized, efforts shifted to their embodiment, analytical and numerical prediction of the resulting behaviors, experimental evaluation of key functionalities, and finally a demonstration of the solutions to validate the hypotheses.

Electromagnetic, thermal, static structural, and structural dynamics modeling was primarily accomplished using commercial three-dimensional computer-aided design, finite element, and computation fluid dynamics software programs. However, fast analytical modeling tools were also developed using MATLAB® and Mathcad®. An important optimization algorithm was developed to streamline the process of establishing design requirements. Prototypes were designed and produced, and experimental measurements were made to demonstrate the more important functional behaviors and to verify the analytical and numerical models.
Introduction

Thesis: A liquid-cooled, direct-drive, permanent-magnet, synchronous generator with helical, double-layer, non-overlapping windings formed from a copper conductor with a coaxial internal coolant conduit offers an excellent combination of attributes to reliably provide economic wind power for the coming generation of wind turbines with power ratings between 5 and 20 MW. A generator based on the liquid-cooled architecture proposed here will be reliable and cost effective. Its smaller size and mass will reduce build, transport, and installation costs.

Scientific Contributions: A holistic approach to wind turbine generator design that considers the needs of the wind energy markets and the costs of manufacturing, logistics, assembly, and long-term operation has concluded that a more compact DD-PMSG architecture is advantageous for low-speed direct-drive wind energy applications. An architecture has been developed and is proposed here. The LC DD-PMSG architecture can be the basis for future wind generator technology development.

The LC DD-PMSG architecture makes use of a newly patented liquid cooling technology that comprises helical, double-layer, non-overlapping windings formed from a copper conductor with a coaxial internal coolant conduit integrated within a primary liquid coolant loop. The effectiveness of the stator cooling approach enables effective passive air cooling of the rotor permanent magnets.

This is the first conceptual embodiment of the new architecture; an 8 MW generator referred to as an LC DD-PMSG. The new architecture enables a machine that is approximately half the size and mass of currently available direct-drive wind turbine generators of equivalent power by increasing the maximum possible linear current density in the windings and the tangential forces produced. See Publications 1 through 7.

This is the first conceptual embodiment of an LC DD-PMSG that makes use of a newly invented lightweight slanted spoke stator and rotor wheel architecture comprising thin sheet-steel elements that are layered and bound to form the spokes and rim. The wheel architecture produces sufficient static structural strength to maintain generator air gap, and exhibits excellent dynamic performance (vibration), because of energy dissipated by friction between sheet-steel element layers. Another benefit to the stacked sheet-steel construction is that substantial spoke-and-rim wheel structures can be built up without welding together overly thick steel elements. See Publication 5.

This is the first comprehensive examination of the economic, electromagnetic, mechanical, and thermal performance of the LC DD-PMSG conceptual architecture. The results reveal excellent performance and value for wind turbine applications and suggest that a wind turbine drivetrain based on the LC DD-PMSG concept should be given careful consideration by the wind power industry.
This doctoral dissertation is based on research and development work carried out by a multidisciplinary team of researchers at the Lappeenranta University of Technology. The team worked through a series of projects aimed at improving the costs, efficiencies, and overall usability of large electrical generators intended for use in next-generation high-power wind turbines.

The specific goal was to define a reliable generator solution to enable lower installed wind turbine cost and lower cost of electricity production. The first step towards achieving the goal was to establish basic requirements to identify the target and guide further conceptualization.

### 2.1 Basic Requirements

The economics of wind power is driving continual upsizing. From 2000 to 2010, the average power rating for new wind turbine installations was approximately 1.7 MW [16, 61]. Today, 3 MW wind turbines are being installed on land, and 5 MW wind turbines are being installed offshore [4, 53]. Industry projections suggest that future wind turbine power ratings will reach 10 MW on land and 20 MW offshore [9]. At present, there are wind turbine prototype development projects that are targeting power ratings of 10 and 15 MW. Refer back to the list of development projects previously presented in Table 1.1 [4, 5]. The research and development team set 8 MW as the appropriate target to begin analytical design and conceptualization of a new generator solution.
A preliminary analysis of electrical machine architectures available for wind turbine application suggested that DD-PMSGs offer an attractive combination of higher overall electricity production, lower cost of operation and maintenance, long-term reliability, and long life. In a trend supporting this conclusion, recent product introductions from major wind turbine manufacturers have been based on direct-drive generator architectures. For example; GE Wind, Goldwind, and Siemens have all introduced large systems intended for offshore use that are based on the DD-PMSG. This architecture was selected by the team as the basis for development of a new generator solution.

Reviewing the details behind the ongoing development projects also reveals the current state of the art for rotor blade diameter and turning speed with respect to rated power. According to their product literature, the Enercon E-126 achieves its rated 7.6 MW with a 127 m main rotor that turns at 11.7 rpm, and the Vestas V164-8.0 MW uses a 164 m rotor turning at 12.1 rpm to produce 8 MW. For the new generator development, a turning speed target of 11 rpm was set to achieve the desired 8 MW power rating.

Most currently operating power-generating wind turbines were designed for a 20-year life [55]. Typically, these wind turbines are land based. A move to offshore is the most recent development in wind energy, and any new generator paradigm must be compatible with offshore applications. While land-based wind turbines are relatively accessible for maintenance and repair, offshore wind turbines are not. Because offshore maintenance and repair is much more expensive; the reliability, serviceability, and repairability requirements for offshore turbines are significantly more stringent [66]. Furthermore, the economics of offshore wind farm investment demands increased design life. For the new generator solution, the team decided on a 30-year design life.

Cooling

Building an 8 MW generator for wind turbine use based on currently available DD-PMSG architectures is neither practical nor economical, because the resulting machine becomes too large, too heavy, and too expensive.

For a rotating electrical machine, power $P$ is the scalar product of the produced torque $\tau$ and the mechanical angular velocity $\Omega$ of the rotor ($P = \tau \cdot \Omega$). In equilibrium, the torque being applied to the input shaft of a generator is opposed by an equal and opposite torque being developed by the generator. For the DD-PMSG, this opposing torque comes from tangential electromotive forces that develop between the rotor and stator according to Maxwell stress theory.
These tangential forces can be characterized as a tangential stress acting on an imaginary cylindrical surface between the rotor and stator. The diameter of the cylindrical surface can be set midway in the air gap and can be considered the generator’s active diameter. The length of the surface is the length of the rotor poles (and stator). The total active surface area is the product of \( \pi \), the diameter, and the length. So, total force, the sum of the tangential forces, is the product of tangential stress and this active surface area. Finally, the product of the total force and the radius of the cylindrical surface is the magnitude of the opposing torque produced by the generator.

The rotor of a DD-PMSG spins at the same speed as the wind turbine’s main rotor blades, and the angular velocity of the blades is limited by how fast they can rotate without exceeding structural strength limits and by how much noise the blade tips make speeding through the air. Therefore, for a direct-drive generator, maximum rotor speed is limited. The noise limitation is less of a concern for offshore installations. Since power is the product of torque and rotor speed, and since maximum rotor speed is fixed, more torque must be developed by the DD-PMSG to produce higher power.

Accordingly, to increase the magnitude of opposing torque within a DD-PMSG, either the active cylindrical surface area acted upon by the tangential stress must be increased or the magnitude of the tangential stress itself must be increased.

However, in a traditional DD-PMSG, tangential force is also limited. According to Heinrich Lenz’s interpretation of Faraday’s law of induction, the electromotive force induced produces electrical current in its windings. The primary output of any generator, this electrical current is proportional to the magnitude of the developed tangential stress. To sustain higher levels of tangential stress, the stator windings of the DD-PMSG must run more electrical current, which means higher linear current densities in the winding conductors.

Increasing linear current density also increases internal resistive heating, which is referred to as Joule heating. Wind turbine DD-PMSGs in use today are air cooled. To remove heat, air cooling relies on the convection coefficient of air, which is a function of air’s thermal conductivity and velocity. The thermal conductivity of air is relatively poor, and in practical applications, the velocity of cooling air is limited by the increasing difficulty and expense of pumping higher and higher volumes through the electrical machinery [53]. Therefore, beyond a certain linear current density limit, an air-cooled generator begins to run too hot. So, tangential force production in a traditional air-cooled generator is ultimately limited by the ineffectiveness of forced air cooling.
Since rotor speed is fixed and tangential force is limited, the only practical means of significantly raising the power rating of an air-cooled direct-drive wind turbine generator has been to increase the active cylindrical surface area acted upon by its tangential stress. In practice, this has meant increasing diameter \( d \). Because of the very high magnetic and electromotive forces acting both radially and tangentially upon the stator and rotor wheel structures, extending generator length \( l \) presents serious structural challenges. Furthermore, developed torque increases with diameter squared and only linearly with length. The equation for generator torque as a function of tangential stress is as follows.

\[
\tau = \sigma \tan \pi d^2 l / 2
\]  

(2.1)

Considering these points, a more appropriate way to achieve a high target power rating in a compact DD-PMSG architecture is to produce higher tangential stresses by running higher linear current densities in the stator windings, which is only possible with improved cooling. Examining heat transfer first principles and the relevant heat transfer mechanisms suggests that direct liquid cooling of the stator windings, using an appropriate cooling fluid, is the best way to remove the extra heat that comes with higher levels of tangential stress [53]. Two appropriate cooling fluid candidates are demineralized and deionized water and the synthetic dielectric fluid polyalphaolefin (PAO).

Electromagnetic analysis shows that Joule losses in the stator windings account for 85% of all losses [53]. Furthermore, heat transfer analysis reveals that primary coolant fluid flowing in direct contact with the conductor minimizes stator temperatures, and consequently, maintains low rotor temperatures [5, 47]. As a result, rotor cooling can be effected passively. Therefore, liquid cooling of the stator-windings conductors was selected as the best approach to generator cooling for this development. Direct liquid cooling is not unprecedented. It is a common cooling approach for larger modern turbogenerators. One example has been reported by Gray et al. [28]. The particular combination of direct liquid cooling with the direct-drive, permanent-magnet, synchronous generator architecture proposed here is referred to as LC DD-PMSG.

**Efficiency**

Preliminary electromagnetic analyses revealed that full load electrical efficiencies for the LC DD-PMSG are not particularly high. For an 8 MW machine, the rated efficiency is approximately 92%. However, the analyses also predict excellent partial load efficiencies. At lower rotor blade speeds, predicted electrical efficiencies can reach 96% and beyond [4]. Because of the variation in wind speed at most wind farm sites, partial load efficiency is more relevant for electricity production than rated efficiency, and an LC DD-PMSG promises higher Annual Energy Output (AEO) than do other machine architectures that come with higher rated efficiencies [4]. The efficiency requirement for the introduced
2.1 Basic Requirements

LC DD-PMSG conceptual design was specified in terms of AEO. A minimum AEO target of 20 GWh was set for an annual average effective wind speed of 9.6 m/s, which is typical of the wind speed distribution for North Sea coastal waters [42].

Size and Mass

The overall size and mass of the generator influences the installed cost of a wind turbine. A larger, more massive generator costs more to build, more to ship, and more to install. Extra generator mass calls for a more robust and heavier nacelle, a sturdier and more massive tower, and a deeper more massive foundation. Certainly, an important objective for any new wind turbine generator solution is reduced size and mass. Liebherr Construction claims their LTM 11200-9.1 to be the strongest telescopic mobile crane on the market with the longest telescopic boom in the world. According to their literature, its maximum lifting capacity for a height of 80 m is 100 tonne and for 90 m is 75 tonne. As such, 90 tonne seems a reasonable maximum mass limit and the requirement set for the new generator solution.

Setting a size limit is more complicated. As discussed previously in the argument for liquid cooling, the active cylindrical surface area acted upon by the developed tangential stress is a key parameter in determining the power rating of a DD-PMSG. And, increasing the diameter of the area has a greater effect than increasing its length. Moreover, there are structural limitations to increasing active length in a permanent magnet machine.

High radial magnetic forces act upon the bridging structures between the concentric rotor and stator wheels as illustrated by Figure 2.1. A large diameter, narrow structure is more suited geometrically to supporting these radial forces than is a small diameter, long structure. It is a trade-off, but in general, for a given power rating, it is possible to achieve a lighter overall generator structure with a larger diameter-to-length aspect ratio. Considering these arguments and based on the various practical aspects of logistics and installation, a 7.5 m diameter overall size target was set for the proposed LC DD-PMSG.

Electrical Power

A six-phase electrical power configuration was selected as most appropriate for the proposed electromagnetic topology. Increasing the number of phases from the traditional three to six reduces the current per phase, smoothens torque pulsations, reduces harmonic content in the air gap for lower rotor losses, and improves reliability [34]. A line-to-line voltage (voltage between phases) of 3.3 kV was specified, which is classified as medium voltage and is commonly used for high power electrical machinery. Compared to low
2 Configuration Basics and the Market Challenge

Figure 2.1 Radial magnetic forces action upon representative stator and rotor structure segments - Because the wheel rims hold the end faces relatively immobile, maximum radial deformation occurs at the midplane between end faces.

Voltage 690 V systems, a 3.3 kV system features lower currents, less cabling, and reduced system losses [1].

Stator Windings

For the proposed LC DD-PMSG, the windings conductor material must be low in electrical resistivity to meet the efficiency requirement and high in thermal conductivity to meet heat transfer requirements. Oxygen-free copper per ASTM C10200 (American Society for Testing and Materials) delivered in a soft annealed initial temper to improve coil formability was selected for the conductor material.

A helical, double-layer (slots shared by adjacent coils), non-overlapping tooth-coil winding geometry is most compatible with the direct liquid cooling approach. This geometry is compact, and an internal coaxial coolant passage within the coil conductor can serve as the liquid coolant conduit. Moreover, beginning and ending the coil on the same end makes for simplified cooling loop connections. Finally, coupling this type of liquid-cooled tooth-coil approach with an internal stator and external rotor generator architecture yields a minimally complex cooling system arrangement. Figure 2.2 illustrates how helical, double-layer, non-overlapping tooth-coil windings can be arranged in a simple internal stator generator configuration.

In addition to being compatible with the planned cooling approach, this winding geometry offers other advantages. Compared to the more common distributed-windings, the end
windings are shorter and less complex [39], and they offer a higher copper space factor (more copper). Manufacturing and assembly is simplified, and with open slots as shown in the figure, the coils can be replaced in the field [2, 33]. Because they offer a higher degree of magnetic isolation between phases, tooth-coil windings make possible a more fault-tolerant design [22, 27, 44]. Finally, the back-electromotive force waveform produced by non-overlapping tooth-coil windings is nearly sinusoidal and results in lower torque cogging [29, 70, 71].

The first step in effectively applying a tooth-coil winding approach for an electrical machine is to set the base ratio of stator slots to magnetic poles. This ratio determines the machine’s fundamental behaviors and performance [48]. A 12/10 slot/pole combination was established here as the base machine configuration. The number of slots per pole and phase $q$ determines how the winding layout is arranged, which affects winding factor and harmonics. The 12/10 base configuration for a six-phase ($m = 6$) supply results in $q = 12/(10 \cdot 6) = 0.2$ slots per pole and phase and a winding factor of 0.966 [4, 35].
The symmetries resulting from the layout of the 12/10 slot-to-pole combination effectively eliminate unbalanced magnetic forces [41]. Moreover, with this ratio, only the desirable fifth stator space harmonic interacts with the magnetic pole fields to produce continuous torque, which minimizes rotor losses [15]. In addition, the 12/10 combination offers a high Least Common Multiple (LCM) of slot and pole numbers. The LCM determines the number of cogging periods per rotor revolution, and a higher LCM corresponds to lower torque cogging [41]. The LCM for 12 and 10 is 60.

The IEC (International Electrotechnical Commission) has adopted NEMA (USA National Electrical Manufacturers Association) standards for electrical insulation that define safe maximum operating temperatures for motors based on an average 20,000-hour lifetime. The four classes commonly used in motors and generators are IEC class 105, 130, 155 and 180; which correspond to temperatures of 105, 130, 155, and 180°C, respectively [65]. Insulation life as a function of temperature is expressed by the Arrhenius equation, which relates reaction rate to temperature [31].

A general rule of thumb based on the Arrhenius equation is that electrical insulation life is cut in half for each rise of 10°C in average insulation temperature. The inverse should also hold true, and by IEC definition, Class 155 insulation will hold up for 20,000 hours at a winding temperature of 155°C. Therefore, if stator winding temperatures are required to remain below 115°C by design, the 20,000-hour insulation life will be doubled four times to as many as 37 years of continuous operation \((155 - 115 = 40 = 4 \cdot 10)\). Class 155 insulation was specified for the stator windings to meet the design life of 30 years.

**Rotor Poles**

The availability of high-energy rare earth magnet materials has made it increasingly popular to use permanent magnet rotors in electric machines. Using permanent magnets in rotors rather than the conventional electrical windings or induction makes it possible to achieve greater energy densities with improved efficiencies and reduced complexity [57]. There are two common permanent magnet rotor configurations: 1) the magnets are magnetized radially and mounted to the outer diameter surface of the rotor, or 2) the magnets are magnetized circumferentially and embedded in slots below the outer diameter surface. In general, surface magnet rotor configurations are less complex and can produce, in a low speed machine, higher torque densities with less magnet material [52]. A rotor-surface permanent-magnet rotor was specified for the proposed LC DD-PMSG.

Since this LC DD-PMSG is an external rotor and internal stator design, the rotor magnets will attach to its inner diameter surface. Attached to the inner surface, the centrifugal
forces of rotation work to keep the magnets in place, so it is easier to develop a secure and long lasting magnet attachment method [19]. Because air gap flux density fluctuates severely in an open-slotted tooth-coil machine, a laminated-steel rotor is required to avoid rotor eddy current losses [49]. An advantage of the laminated rotor is that magnet attachment features are easily included in the geometry.

Setting the maximum allowable rotor temperature to a conservative $80^\circ C$ means an economical high-flux-density grade of neodymium magnet material (NdFeB) can be used. Based on available NdFeB product literature, all available grades of NdFeB are stable at this temperature. Since NdFeB is susceptible to corrosion, an exterior coating using an appropriate corrosion protection material is also needed.

Protection

The International Protection (IP) marking code, according to IEC 60529, defines the degrees of protection for an enclosed system. The team determined that IP54 would be an appropriate IP classification for the LC DD-PMSG. An IP54 enclosure will prevent ingress of dust or splashed water above levels that might affect machine operation [64].

Summary of Base Requirements

Accordingly, the requirements listed in Table 2.1 were established as the most suitable basis for further development towards a proposed LC DD-PMSG concept and for investigation of the architecture’s electromagnetic, thermal, and mechanical behaviors.
Table 2.1 Base Requirements for development of an LC DD-PMSG

<table>
<thead>
<tr>
<th>Requirement</th>
<th>Specification</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated Power</td>
<td>8 MW</td>
</tr>
<tr>
<td>Machine Type</td>
<td>DD-PMSG</td>
</tr>
<tr>
<td>Rated Speed</td>
<td>11 rpm</td>
</tr>
<tr>
<td>Design Life</td>
<td>30 years</td>
</tr>
<tr>
<td>Cooling Type</td>
<td>forced direct liquid cooling</td>
</tr>
<tr>
<td>Cooling Method, Stator</td>
<td>axial fluid flow through stator conductor</td>
</tr>
<tr>
<td>Cooling Method, Rotor</td>
<td>passive, contact with ambient air</td>
</tr>
<tr>
<td>Minimum AEO (North Sea Coastal)</td>
<td>20 GWh</td>
</tr>
<tr>
<td>Maximum Generator Mass</td>
<td>901 (90,000 kg)</td>
</tr>
<tr>
<td>Maximum Diameter</td>
<td>7.5 m</td>
</tr>
<tr>
<td>Number of Electrical Power Phases</td>
<td>6 (two 3-phase systems in 30° phase shift)</td>
</tr>
<tr>
<td>Nominal Line-to-Line Voltage</td>
<td>3.3 kV</td>
</tr>
<tr>
<td>Rotor/Stator Configuration</td>
<td>external rotor, internal stator</td>
</tr>
<tr>
<td>Winding Conductor Material</td>
<td>ASTM C10200 oxygen-free copper, soft annealed</td>
</tr>
<tr>
<td>Winding Geometry</td>
<td>tooth coil (duplex-helical, double-layer, non-overlapping)</td>
</tr>
<tr>
<td>Winding Layout</td>
<td>$q \leq 0.5$ fractional slot (12/10 base winding)</td>
</tr>
<tr>
<td>Maximum Stator Temperature</td>
<td>115°C</td>
</tr>
<tr>
<td>Windings Insulation Class</td>
<td>F (155°C)</td>
</tr>
<tr>
<td>Rotor Magnet Configuration</td>
<td>rotor surface permanent magnets</td>
</tr>
<tr>
<td>Rotor Wheel Rim Composition</td>
<td>laminated electrical steel</td>
</tr>
<tr>
<td>Magnet Material for Rotor</td>
<td>coated neodymium (NdFeB)</td>
</tr>
<tr>
<td>Maximum Rotor Temperature</td>
<td>80°C</td>
</tr>
<tr>
<td>Ingress Protection Class</td>
<td>IP54</td>
</tr>
</tbody>
</table>

2.2 Market Challenge

A wind turbine designed around a drivetrain based on the above requirements will be unique and perhaps even disruptive to the wind energy marketplace. Manufacturers will be reluctant to accept and adopt the new technology, and the marketplace will only invest if the economic advantages are compelling.

Wind Turbine System Complexity

From the outside, wind turbines appear to be simple. However, the engineering disciplines involved in the successful implementation of a utility grade wind turbine comprise meteorology, aerodynamics, mechanical engineering, electrical engineering, electronics engineering, instrumentation and controls, software engineering, plant and maintenance engineering, and construction engineering.
Modern utility grade wind turbines are complex systems of over 8,000 interdependent components. When any of the major subsystems change, the entire wind turbine system is affected. A drivetrain change influences the design of the rotor blades and hub, the nacelle, the tower, and the auxiliary systems. A change to the generator architecture is a change to the entire drivetrain, which ultimately results in a unique new wind turbine product offering.

Testing and Certification

Before it can be introduced to the marketplace, any new wind turbine design and the manufacturing processes for its production must be evaluated and tested to ensure conformance to a standard. Currently, the preeminent standard is the IEC 61400-22, which covers both land-based and offshore wind turbines and wind farms. Independent testing, inspection, and certification services institutions; such as Bureau Veritas, Intertek, and SGS (Société Générale de Surveillance); carry out the evaluations and perform the testing. Once conformity has been established, a final evaluation report is prepared and a type certificate is issued.

Each institution has its own specific certification process; however, their processes typically include:

• design basis evaluation,
• design evaluation,
• type testing, and,
• manufacturing evaluation.

*Design basis evaluation* ensures the design basis requirements laid out for the engineering and design teams are properly documented and sufficient to achieve a safe product with the intended performance characteristics. The design requirements identify relevant principles and methodologies, codes and standards, and statutory requirements. They detail the requirements for manufacturing, transportation, installation, and commissioning and for operation and maintenance. Finally, the design basis requirements define appropriate grid connection and operation specifications.

*Design evaluation* determines if the completed design conforms to the design basis requirements. All aspects of safety and control are assessed, as are load assumptions and load calculations. All components are examined based on the approved loads and the relevant standards and guidelines. The dynamic behavior predictions for the system are also evaluated.
Type testing provides data needed to verify safety, focusing on aspects that cannot be reliably evaluated by analysis. Typically, type testing comprises safety and functionality testing, dynamic behavior testing, load measurement, and rotor blade testing. Finally, manufacturing evaluation determines if production processes are in accordance with design specifications. It includes an examination of the quality system and an assessment of implementation with regard to the design documentation.

Cost of Development

A major development effort would be required to move the proposed LC DD-PMSG from concept to a marketable product offering. The development project would involve detailed analyses and full detail design followed by the procurement, assembly, and testing of at least two full-scale prototypes. Because the design power rating is 8 MW, the only way to thoroughly test the generator is to drive it with an equally powerful motor. Therefore, one prototype would be required to operate as the motor to drive the second prototype operating as a generator.

This type of development program would span two to three years and cost on the order of €10 million. Facilities capable of handling a generator with a diameter of 7.0 m and a mass of 85 tonne would be required. Upon successful conclusion of the development program, type certification would be required before the product offering could be introduced to the marketplace. Type certification could cost another €1-2 million.

Wind Turbine Economics

Currently, wind turbine manufacturers are in a race to reduce the capital costs of their product offerings. To do so, they must reduce development costs and move towards product standardization. In this type of cost cutting environment, engineering changes are resisted and wholesale technology shifts are avoided. Engineering changes add significantly to unit costs and product delivery schedules. Lower costs are best achieved by producing higher unit volumes of a standard product offering.

Historically, disruptive new technologies are introduced to the marketplace by startups that do not have their engineering, manufacturing, and marketing organizations already heavily invested in the technologies that make up their existing offerings. Established manufacturing companies may act when a new technological discovery is made that promises to significantly reduce manufacturing cost or when a new technology promises a competitive advantage big enough to significantly improve market share. However, even when prompted to act, existing manufacturing companies move slowly and deliberately to avoid disruptions to their existing operations and to protect existing market share.
New technologies can also be introduced to the marketplace when existing technologies are no longer capable of satisfying marketplace demands as is the case for direct-drive wind turbine generators. As mentioned previously, for example, 6 MW seems to be the maximum power rating in which air cooling is a viable wind turbine generator thermal management approach. Beyond 6 MW, a new approach is called for.

If and when the LC DD-PMSG solution is introduced to the marketplace, it will probably be by a newer manufacturer striving to increase its share of the large offshore wind farm market space.

Table 2.2 compares the current approximate costs of manufacture, logistics, and installation for 8 MW versions of a traditional air-cooled DD-PMSG and the proposed LC DD-PMSG. Both on-land and offshore installations are addressed. The research team developed generator designs for each of the two architectures and used them to predict costs. Capital cost estimates were based on predicted material and production costs. Logistics and installation costs were based on the size and mass of the two designs and the typical costs reported for wind turbine components of comparable size and mass. As the table shows, the cost advantages of the LC DD-PMSG architecture are compelling.

Table 2.2 Projected cost of air-cooled and LC DD-PMSGs with 8 MW rating

<table>
<thead>
<tr>
<th></th>
<th>On Land</th>
<th>Offshore</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>DD-PMSG</td>
<td>LC DD-PMSG</td>
</tr>
<tr>
<td>Capital Cost</td>
<td>8,170 k€</td>
<td>6,960 k€</td>
</tr>
<tr>
<td>Logistics</td>
<td>1,140 k€</td>
<td>900 k€</td>
</tr>
<tr>
<td>Installation</td>
<td>2,340 k€</td>
<td>2,290 k€</td>
</tr>
<tr>
<td>Total</td>
<td>11,650 k€</td>
<td>10,150 k€</td>
</tr>
</tbody>
</table>
This next chapter begins with a discussion of the current state of wind energy technology, pointing out the benefits that could result from a new approach to wind turbine generator design and the establishment a new lighter weight generator architecture. The discussion is followed up with a detailed presentation of the new architecture in the form of a proposed conceptual embodiment of an 8 MW LC DD-PMSG.

3.1 Benefits to Wind Power Generation (Pub 1)

Currently, the European Union is producing over 115 GW of electricity from wind power, which is more than 10% of total European electricity production [25]. The USA is producing over 60 GW of wind power electricity; 4.25% of their total [63]. And, at the end of 2012, China’s wind power electricity production was over 75 GW [14]. Clearly, in each of these large energy markets, wind energy plays a vital role in energy planning, and in each case, there are plans for continued aggressive wind energy development.

One of the essential but most costly components of a wind turbine is the generator, which can be based on a number of electromagnetic architectures. Common types include the PMSG, the DFIG, and the EESG. Recently, PMSGs are showing up in more of the newer wind turbine product offerings. They offer advantages such as lower active material weight, improved thermal performance, lower losses, and higher energy yield [7, 8, 20, 37]. Vestas, GE Wind, Goldwind, Siemens, and Gamesa have all recently introduced large systems featuring PMSGs.
The majority of operational wind turbines are land based, but because offshore sites have more wind, an increasing percentage of new installations are moving offshore. Projections show the offshore percentage continuing to increase and wind turbine technologies, in general, are being driven by new requirements in response to the demands of offshore wind turbine operation.

The overriding focus of these new requirements is improving the reliability of the wind turbine and reducing both the frequency and duration of maintenance and repair operations, that is, extending uptime and reducing downtime [10, 21, 51, 56]. These more stringent requirements are encouraging continuing development of wind turbines based on direct-drive generator technologies.

Moving to a direct-drive, low-speed, high-torque generator eliminates the complex and heavy gearboxes used in medium- or high-speed generators. And, with the nearly one failure per year per ten wind turbines reported in one study, gearboxes have been significant contributors to wind turbine failure rate [59]. As such, the direct-drive approach promises reduced maintenance and improved reliability and longevity [21, 45, 59]. Furthermore, the direct-drive PMSG offers excellent overall efficiency [7, 37].

Over the past 15 years, hundreds of direct-drive units have gone into operation in Germany, Denmark, and Spain. In the USA and China, many of the newer installations have also been based on direct-drive generator architectures. Avantis, Clipper Wind, Darwind, Harakosan, Leitwind, Siemens, Vensys, and Mitsubishi are all introducing units based on DD-PMSGs [56].

Why Wind Turbines Are Getting Bigger

However, DD-PMSG drivetrains have one major shortcoming for wind turbine applications. The generators must become very large to produce higher powers. Because the rotor of a direct-drive generator spins at the relatively low speed of the wind turbine’s rotor blades, the torque developed by the generator must be very large to produce higher power ratings.

A couple real world examples will serve to illustrate. Typical parameter values for a 1500 rpm (gear-driven) and a 15 rpm (direct-drive) synchronous generator each rated at 3 MW have been published [50]. The tangential stress $\sigma_{\tan}$ developed by the 1500 rpm generator is 43 kPa. In the 15 rpm generator, the developed tangential stress is 60 kPa. The number of pole pairs in the configuration of each machine is 3 and 60, respectively.
A typical length-to-diameter aspect ratio \( l/d \) for the rotor of a synchronous generator can be estimated as a function of pole pair number \( p \) using the following equation [49].

\[
l/d \approx \frac{\pi}{4p} \sqrt{p}
\]  

(3.1)

For the 1500 and 15 rpm machines with pole pair values of 3 and 60, the \( l/d \) aspect ratios are 0.45 and 0.1.

The torque \( \tau \) developed for each configuration can be expressed as a function of power \( P \) and angular velocity \( \Omega \) as given by Equation (3.2).

\[
\tau = \frac{P}{\Omega}
\]  

(3.2)

Previously, in Equation 2.1 torque was also expressed as a function of developed tangential stress \( \sigma_{\text{tan}} \). If length \( l \) is written as a function of the aspect ratio \( l/d \) as follows.

\[
l = d(l/d)
\]  

(3.3)

Then, the expression of torque based on tangential stress can be rewritten as illustrated in this next Equation (3.4).

\[
\tau = \frac{1}{2} \sigma_{\text{tan}} \pi d^2 l = \frac{1}{2} \sigma_{\text{tan}} \pi d^2 d(l/d) = \frac{1}{2} \sigma_{\text{tan}} \pi d^3 (l/d)
\]  

(3.4)

Setting the expression for torque from Equation 3.2 equal to the one from Equation 3.4 and solving for the diameter \( d \) produces an equation for rotor diameter.

\[
d = \sqrt[3]{\frac{2P}{\Omega \sigma_{\text{tan}} l (l/d)}}
\]  

(3.5)

The diameters calculated by Equation (3.5) for the actual parameter values of the 1500 and 15 rpm synchronous generators are 0.85 m and 5.85 m. In other words, the rotor diameter of a low-speed, direct-drive synchronous generator designed for 15 rpm will be nearly 7 times the diameter of a conventional 1500 rpm generator.

The Problem with Bigger Diameter Generators

Bigger is not better for wind turbine generators. Because the manufacturing cost of an electrical machine is directly related to the mass of its constituent materials, a bigger diameter translates to higher manufacturing cost. All other parameters being equal, if the diameter of a generator increases by a factor of seven, generator mass will increase by seven cubed \( (7^3) \), which dramatically increases the cost of materials and overall costs of manufacturing.
The increase in diameter and this tremendous increase in mass also translate into increased costs for logistics. In general, heavier and larger is more expensive to transport. Beyond certain limits, if standard shipping methods cannot be used for example, these costs can also escalate dramatically.

A larger and more massive generator also means the wind turbine itself must be designed more robustly. The nacelle and tower must be significantly larger, and the foundation must support and manage the dynamic movement of a great deal more mass. These structural changes contribute to substantially higher wind turbine capital costs. Increased generator mass is a problem for floating offshore wind turbines in particular. Because greater mass amplifies nacelle movement in response to wave motion, it is especially important to minimize the mass of elevated components in a floating system [54].

At the wind farm, more expensive site preparations are required to accommodate the larger equipment needed to handle more massive wind turbine components, and the equipment is more expensive. Lifting the more massive components to assemble the wind turbine is a longer and substantially more costly endeavor. Already today, wind turbine construction is straining the limits of mobile construction technologies.

Figures 3.1 and 3.2 illustrate the scale of the size problem. Figure 3.1 is a photo showing the nacelle of an Enercon E-126 being prepared for lifting during wind turbine assembly on site. Figure 3.2 shows the mobile lifting crane, one of the largest available in the world, alongside the E-126 tower. The photo on the right shows the generator being lifted towards the nacelle.

**Electromagnetic Limitations of the DD-PMSG**

As previously mentioned, the tangential stress $\sigma_{\text{tan}}$ developed by a rotating electrical machine determines its torque production. According to Maxwell’s stress tensor theory, this tangential stress is a function of the existing magnetic field strength, which is a function of linear current density. Tangential stress and, therefore, torque production is directly proportional to linear current density.

In practice, linear current density is produced by supplying current to the windings that pass through slots in the stator electrical steel. The magnitude of linear current density depends on the level of current passing through each conductor and the number of conductor lengths passing through each slot.
3.1 Benefits to Wind Power Generation (Pub 1)

Figure 3.1  Nacelle of the 7.6 MW Enercon E-126 wind turbine being prepared for lift and installation onto the wind turbine tower (Photo by Herman Walraet) - The two workers below the 12 m plus diameter nacelle offer a size reference.

Figure 3.2  (Left) Mobile lifting crane for the 7.6 MW Enercon E-126 wind turbine standing alongside the tower and nacelle (photo by Steenki) - (Right) The 220 tonne E-126 generator being lifted upwards toward the nacelle (Installation of a stator © Enercon GmbH)
Geometric constraints limit the number of conductor lengths that can be fitted circumferentially in a stator. Electromagnetic constraints limit how many conductors can be stacked radially. Increasing the number of conductors in the radial direction increases the depth of the slot, which leads to excessively large slot leakage inductance.

A physical constraint also limits the level of current that can pass through each conductor. As current level increases, Joule losses increase in proportion to the electrical resistivity of the conductor material. The temperature of the stator windings and electrical steel is set by the balance that develops between the resistive heat being produced and the heat being removed by the cooling system.

Improving the cooling method is the most appropriate way to increase linear current density. Table 3.1 illustrates how cooling method affects the maximum linear current density achievable in synchronous machines. The table gives linear current density ranges for air cooling, cooling with hydrogen, and cooling with water applied directly onto the windings, which is the LC DD-PMSG approach.

<table>
<thead>
<tr>
<th>Linear Current Density (kA/m)</th>
<th>Air Cooling</th>
<th>H₂ Cooling</th>
<th>H₂O Cooling</th>
</tr>
</thead>
<tbody>
<tr>
<td>30 ... 80</td>
<td>90 ... 110</td>
<td>150 ... 200</td>
<td></td>
</tr>
</tbody>
</table>

There is no theoretical upper limit to the linear current density produced in a synchronous electrical machine. It increases indefinitely with increasing electrical current in the windings. However, more current corresponds to higher Joule losses, which represents a drop in overall machine efficiency. Furthermore, higher linear current density results in deeper reactive voltage drop, which limits the peak torque at any given terminal voltage. Since terminal voltage is limited by both the capabilities of the voltage supply and the system of insulation, at some point further increases in linear current density are no longer possible.

Finally, magnetizing inductance, a function of several of the electromagnetic design parameters that must be changed to increase torque production, grows with and inhibits torque production imposing a hard upper limit that cannot be exceeded. Linear current density can be increased to develop higher peak torque production by improving cooling method, but there is a cost to efficiency and a hard upper limit.
Thermal Limitations of the DD-PMSG

The stator winding temperatures in a wind turbine DD-PMSG should be kept below 120°C, and the temperatures of the rotor permanent magnets should be kept below 100°C [53]. These temperatures must be maintained regardless of ambient temperature, which could be as high as 40°C.

To maintain constant safe temperatures, total available cooling power must be equal to or greater than the heat produced by the machine due to Joule and frictional heating, in other words, due to the machine inefficiencies. Most wind turbine DD-PMSGs today are air cooled. As coolant air passes through the internals of the stator and over the rotor, convection is the dominant heat transfer mechanism. This convection is supported by continual conductive heat flow through the generator materials.

The temperature of the coolant used to manage machine temperatures is determined by ambient temperature levels, so total available cooling power cannot be increased by simply specifying a lower incoming coolant temperature. It must be increased by either raising the mass velocity of the coolant flow or by improving heat transfer coefficients.

For an air-cooled system, increasing mass flow velocity means increasing fan power. Air cooling power is directly proportional to fan power. This imposes a practical limitation on taking a higher flow rate approach. Beyond a certain mass flow velocity limit, it rapidly becomes prohibitively expensive to provide the necessary additional fan power. Therefore, the best approach to increasing cooling power is to improve the heat transfer coefficients.

Increasing the torque production of a wind turbine DD-PMSG is best accomplished using stator windings that develop higher linear current densities, which are subject to higher levels of internal heat production. And, there is really only one practical way to manage the higher temperatures that result, which is to improve the heat transfer coefficients that drive cooling power by selecting a coolant with exceptional heat removal properties.

The obvious solution is a move to liquid cooling using deionized water or other dielectric heat transfer fluid. Combining direct liquid cooling of the stator coils with indirect air cooling of the rotor is a generator cooling approach that enables significant increases in linear current density for higher torque production to achieve high-power DD-PMSGs of reasonable size.
3.2 Proposed Embodiment of an 8 MW LC DD-PMSG

The following paragraphs describe one possible conceptual embodiment for an 8 MW LC DD-PMSG derived from the base requirements presented previously in Table 2.1.

Sectioning, Number of Coils, and Number of Poles

Even with an increase in tangential forces and the resultant reduction in air gap diameter, an 8 MW direct-drive generator is not going to be small. Practical manufacturing considerations call for sectional construction, and both the generator rotor and stator can be divided into a number of equal sectors. The tooth coils for each stator sector can be electrically connected in a number of series/parallel combinations to get a variety of voltage levels. For the proposed 8 MW LC DD-PMSG, both rotor and stator are divided into 12 equal 30° sectors.

To maintain the desired $q = 0.2$ fractional number of slots per pole and phase for a 12/10 base winding with $m = 6$, the number of rotor poles and the number of tooth coils in the stator must be equal multiples of 10 and 12, respectively. The number of rotor pole pairs $p$ and rotor rotation speed $n$ determine the rated frequency $f$ of the machine ($f = p \cdot n / 60$), which must be compatible with the cyclic loading requirements for the insulated-gate bipolar transistors of the generator’s frequency converter [4]. For 120 magnetic poles, which is $p = 60$ pole pairs, a rotor speed of 11 rpm yields a frequency of 11 Hz. To satisfy the 12/10 base winding ratio, 144 tooth coils are required for 120 magnetic poles. So, the specified number of coils and poles for the proposed LC DD-PMSG was 144 and 120, respectively.

The line-to-line root mean square voltage specified for the six-phase machine is 3300 V. The corresponding phase voltage is $3300/\sqrt{3}$ or 1905 V. Each phase comprises 24 tooth coils ($144/6 = 24$), and these 24 coils are wired in series. Therefore, the voltage across each tooth coil is 1905/24 or 79 V.

Tooth Coil Configuration, Air Gap Diameter, and Active Length

A critical step towards embodiment of the LC DD-PMSG concept was to determine the specific details of the tooth-coil design for the stator windings. Electromagnetic performance was key; however, the coils also had to accommodate coolant conduits of appropriate size, and coil geometries had to conform to practical manufacturing limitations.
The coil conductor itself can be solid or stranded. Cross-sectional areas being equal, solid conductors are subject to more resistive heating than are stranded conductors due to the skin effect distribution of current. In this case, however, the low operating frequency of the generator results in minimal skin-effect losses, and it is easier to produce a rectangular cross section with high copper space factor using a solid copper extrusion. Furthermore, a solid rectangular conductor offers coil manufacturing advantages, and the resulting tooth coil offers stator assembly advantages. The tooth-coil configuration for the proposed generator solution was based on solid copper conductors with a rectangular cross section.

If the rectangular profile of the conductor is wider than it is tall, leakage inductance is lower, which results in better electromagnetic performance. However, coiling the conductor is much easier if the aspect ratio is reversed, that is, if the conductor is narrow and tall. To get best electromagnetic performance and still maintain adequate formability, a design rule was established setting the width-to-height aspect ratio for the conductor to 1.2.

To minimize corrosion and ensure long-term leak-proof operation for a forced (pressurized) liquid-cooling system, the coolant should be contained within a single-material corrosion-resistant jacket with a minimum of transitions and connections. Furthermore, the inner surface should be as smooth as possible. For the proposed generator solution, the tooth-coil design features corrosion-resistant stainless steel tubing that has been coaxially embedded within the conductor material to serve as the coolant conduit. Austenitic stainless steel tubing such as ASTM 316 offers suitable corrosion resistance. EN 1.4401 is the equivalent material according to the European standard. Figure 3.3 shows a length of conductor with its inner coaxial stainless steel conduit.

Fabricating long lengths of this hybrid liquid-cooling conductor material at production volumes can be accomplished using a continuous rotary extrusion process. The heart of this process is a continuous rotary extruder, such as a Metalmorph TTX 320 or a Meltech-CRE. See Figure 3.4. The extruder is serviced by various feed and take-up units. Copper feedstock comes from a pay-off reel, passes through a straightener and cleaner, and feeds into the extruder. Stainless steel tubing comes off its own pay-off reel, passes through its own straightener and cleaner, and then moves through a heater before it feeds into the extruder. Heating the stainless steel tubing ensures a metallurgical bond is formed between the copper and the stainless during the extrusion process. The coaxial hybrid rectangular conductor exits the extruder, passes through a cooling system, and then feeds onto a take-up reel.

This type of extrusion line must be set up and tuned to produce a single hybrid conductor size at a time. For each conductor size, it uses size-specific extrusion dies, unique
Figure 3.3 Segment of copper conductor (aspect ratio 1.2-to-1) showing stainless coolant conduit - Copper is extruded over heated stainless steel tubing forming metallurgical bond.

Figure 3.4 Meltech continuous rotary extrusion machine (image courtesy of Meltech)
process recipes, and unique pre- and post-processing units. Changing the line over to produce another size is time consuming and costly. So, it makes sense to limit the number of conductor sizes available for LC DD-PMSG use. Consequently, a catalog of sizes compatible with standard stainless steel tubing was developed to cover a 5 to 20 MW range of generator powers. These hybrid conductor sizes were considered for all subsequent LC DD-PMSG design and analysis work. Figure 3.5 shows the selected sizes.

![Catalog of conductor sizes](image)

**Figure 3.5** A catalog of acceptable conductor sizes was defined to simplify design and manufacture of a family of LC DD-PMSGs.

The number of phase turns, the number of conductors, and the total copper area needed for the tooth coils to achieve an 8 MW power rating can be approximated using simple analytical expressions [49]. Generator air gap diameter and the number of slots and poles are important parameters for these expressions, and the analytical process is iterative. However, after exploring how various preliminary combinations of these parameters affect the result, it became clear the proposed machine would require duplex coils. In other words, the continuous length of conductor would be wound with an inner and outer coil and each tooth-coil side would be a 2-column array of conductors. Figure 3.6 illustrates a simple two-level duplex-helical coil. The coil down-bend shown in the figure is necessary. It accommodates the step changes in elevation required by the helical winding of the conductor and makes it possible to keep the conductor active lengths level and parallel.

The width of any duplex-helical (two-column) tooth coil depends on the minimum bend radius of the inside coil. Experimentation determined that a minimum bend radius of twice the copper conductor width ($r_{\text{min}} = 2w_{\text{cu}}$) was workable and resulted in minimal distortion of the copper conductor without significant deformation of the coolant passage. Moreover, as implied by Figure 2.2 presented previously, stator diameter is determined by tooth-coil width for a generator architecture based on a fixed number of tooth coils.
Presentation of Proposed Generator Concept

Duplex winding has inner and outer conductor columns

Down bend enables winding level change

Terminal for Electrical Connection

316 Stainless Steel Coolant Conduit

Figure 3.6 Simple two-level duplex coil showing conductor wound into inner and outer coil - Down bend at near end of coil is to accommodate the level change for each wrap of the helical winding making it possible for the straight active lengths to remain horizontal and parallel.

Accordingly, and to facilitate cost effective production and simplify early design decisions, a catalog of acceptable generator air gap diameters was prepared. The diameters were based on circular arrays of 144 duplex coils for the six minimum practical coil widths corresponding to the previously established catalog of hybrid conductor sizes. In each case, spacing between coils was chosen to be the minimum practical, while still providing an approximately one-to-one tooth and slot width ratio. Figure 3.7 presents the details.

Considering the 7.5 m goal set in the initial requirements for maximum generator diameter and referring to Figure 3.7, two of the catalog tooth-coil widths yield air gap diameters that are appropriate for an 8 MW power rating. Tooth coils formed from the $18 \times 15$ mm hybrid conductor are compatible with an air gap diameter of 7 m. Tooth coils formed from the $16.8 \times 14$ mm conductor are compatible with an air gap diameter of 6.5 m.

Both tooth-coil configurations were evaluated using an optimization program that estimates total generator cost and performance based on generator architecture [3]. Publication 4 describes this optimization tool in detail. The predicted performance behaviors for the 6.5 m and 7 m configurations were equally acceptable with little difference in total generator cost. Ultimately, the 6.5 m air gap diameter configuration using the
3.2 Proposed Embodiment of an 8 MW LC DD-PMSG

Figure 3.7 A catalog of acceptable air gap diameters was defined as a function of duplex tooth coil conductor size to simplify design and manufacture of a family of LC DD-PMSGs.

16.8 × 14 mm conductor size was selected, because the smaller diameter results in a smaller generator. The required active length determined by the optimization program for this configuration is 1.1 m.

Re-applying the previously mentioned analytical expression for number of conductors per slot revealed that 24 active lengths of 1.1 m are needed to achieve the 8 MW power rating at the catalog 6.5 m diameter.

Figure 3.8 illustrates the fully configured duplex tooth-coil with 24 conductors and an active length of 1.1 m. Each 16.8 × 14 mm conductor must be insulated to eliminate coil short-circuiting, either wrapped with a thin electrical insulating tape or coated, and each coil side is covered with a more substantial wall insulation material to seal and protect the coil conductors and to isolate the coil from the surrounding electrical steel.

Stator Cooling Lines

Substantial voltage develops between the input and output legs of the LC DD-PMSG tooth coil and between the coil and ground. Therefore, each coil must be electrically isolated from the supply and return lines of the liquid-cooling system. A cooling tube electrical isolation component was developed for this purpose. See Figure 3.9. The body of the unit can be any appropriate electrically resistive material. For the proposed concept, a polyoxymethylene acetal polymer such as Delrin® was assumed. The end caps shown in the figure are stainless steel. They are orbitally welded and integral to the incoming and outgoing stainless steel tubing fluid lines, which are not shown.
Main wall insulation isolates coil from lamination slot walls

Conductor is insulation wrapped or coated prior to winding

Terminals for Electrical Connection

316 Stainless Steel Conduit

Figure 3.8 Duplex tooth coil for proposed LC DD-PMSG based on 144 tooth coils on a 6.5 m air gap diameter - Total length of conductor is 34 m (27 m active length), and the total coil mass is 66 kg.

Stainless steel end caps are orbitally welded to stainless steel coolant lines

Concentric pair of Viton® double-seal o-rings provides four serial sealing surfaces at each end of the body

Delrin® body provides galvanic isolation

Figure 3.9 Cooling tube electrical isolation assembly - The only mechanical seals in the cooling lines near the generator are within these assemblies.
This cooling tube electrical isolation component represents two potential leak paths for the stator cooling system, so it has been carefully designed to minimize that possibility. A concentric pair of double-seal Viton® o-rings; also known as quad seals, quattro seals, or x-rings; is used at both ends of the unit to effect the seal using four serial concentric sealing surfaces.

Incoming and outgoing coolant flow for each stator sector of the proposed LC DD-PMSG passes through a single dual-path distribution manifold. Figure 3.10 shows the manifold and offers a cutaway view to reveal its inner configuration. The bottom of the manifold interior is divided front and back into two radial volumes that span its entire length. One of these volumes connects to a single large feed line, and the other connects to a single large return line. Above the two large radial volumes, the manifold is divided into 24 smaller chambers. Each chamber has a hole in its floor that connects it to one of the two larger radial volumes. Every second chamber connects to the feed volume, and every second chamber connects to the return volume. A length of tubing passes through the roof of each one of these chambers to serve as a connection port, and these 24 connection ports comprise 12 feed-and-return port pairs.

![Stator Segment Coolant manifold - Cutaway view on right side shows internal configuration with inlet and outlet chambers to feed 12 tooth coils.](image)

Cooled fluid coolant flows from the large inlet feed line and into the front radial volume. It moves up into the feed chambers (every other chamber) and then out the feed taps. Warmed fluid comes back through the return taps and into the return chambers. It flows down into the back radial volume and then exits via the large return line. Each of the tooth coils connects to one of the feed-and-return tap pairs.
3 Presentation of Proposed Generator Concept

Stator Section

Every stator sector comprises 12 coils surrounding 12 teeth formed by the electrical steel laminations. The conductors from one side of two adjacent coils sit side-by-side in each open slot (double-layer configuration) held in place with a slot wedge. Slot material can vary. A suitable slot wedge material has high flexural strength and high electrical resistance. SPIndustries offers slot wedges made from their own SPIndu楔edge material, which is an epoxy-glass-prepreg.

The electrical steel laminations make up the base structure for the stator section in each sector. The base structure consists of stacked laminations, two stainless steel endplates, and a series of high-tension binding (tie) rods that bind the stack tightly together. The tie rods are stainless steel, but hollow to minimize eddy current losses. Binding rods made from Röchling HIR Glasrod, an electrically non-conductive and non magnetic fiberglass-reinforced thermoset polyester, offer improved electromagnetic performance. HIR Glasrod binding rods were tested and with some development could possibly be used in this application.

To minimize mass, the laminated base structure also serves as an arc structural element for each stator sector. Its structural integrity relies on the frictional bond between laminations that develops as the tie rods compress the laminations stack. Consequently, an electrical steel with an appropriate high-friction coating must be used. An example is Suralac® 7000 offered by Tata Surahammars Bruks AB. Suralac® 7000 has an inorganic phosphate coating.

There are two different lamination shapes. The first is radially shorter and designed primarily to optimize electromagnetic performance. The second is like the first, but it extends further inward. The extension includes an array of large holes through which cross tubes can be fed to fix the stator segment to the stator wheel structure. The shape of the endplates is similar to the second radially taller lamination shape. Figure 3.11 shows the bound stator section base structure. The bottom right image in the figure is a close-up view of a single end of one of the tie rods.

Figure 3.12 presents a complete stator section assembly, showing the attached coolant manifold. The only non-welded cooling line connections are between the end caps and insulating body of the cooling system electrical isolation assemblies. All other stainless steel tubing line connections should be orbital welded. Top right in the figure illustrates how the notch wedges are axially constrained using a retainer, which should be made of a low electrical conductivity material.
3.2 Proposed Embodiment of an 8 MW LC DD-PMSG

Figure 3.11  Stator Segment Laminations - The electrical steel laminations are bound between two end plates with 24 binding tie rods. Crossing tubes (not shown) pass through the large holes at the bottom to mount the segment to the stator wheel structure.

Figure 3.12  Stator section assembly showing coolant manifold and cooling line connections - Twelve of these sectors combine to make up the active circumference of the stator.
Rotor Surface Magnets

The magnetic flux fields of the rotor are produced by front-to-back rows of radially magnetized segmented NdFeB magnets positioned side-by-side on the rotor surface. An economical grade of NdFeB is acceptable, but since NdFeB is susceptible to corrosion, an effective corrosion resistant coating is required. An epoxy resin layer impregnated with zinc powder has proven to offer excellent performance [13].

Each magnet consists of smaller segments of sintered NdFeB that have been adhesively bonded together. See Figure 3.13. The segmentation of the magnet inhibits the development of eddy currents, which produce heat and reduce efficiency. Each segmented magnet is 136 mm wide by 26 mm tall and 54.5 mm front-to-back. The magnet design results in the relative magnet width (pole arc / pitch) of 0.75 that is recommended to minimize torque ripple for a fractional open-slot machine with $q = 0.2$ and $m = 6$ [52].

![Figure 3.13 Radially magnetized segmented NdFeB magnet](image)

Rotor Section

Also comprising 12 sectors, the circumference of the outer rotor wheel consists of 12 inside-facing rotor sections. Each rotor section comprises 10 magnetic poles, that is, 10 rows of magnets running front-to-back. The magnets are surface mounted to the inside surface of the rotor section.

The base structure of each rotor section is made up of electrical steel laminations. Following the design philosophy introduced for the stator section, the rotor base structure also consists of stacked laminations, two stainless steel endplates, and a series of high-tension tie rods to bind the stack tightly together. Again, the laminated base structure serves as a structural element for the rotor wheel rim. The laminations come in two shapes: one regular and one extended radially. For this outer rotor configuration, the radial
3.2 Proposed Embodiment of an 8 MW LC DD-PMSG

extension is outward. The extension includes an array of large holes to accommodate the cross tubes that mount the rotor section to the rotor wheel structure. Figure 3.14 shows the bound rotor section base structure.

Figure 3.14 Rotor laminations bound together with end plates and tensioned rods

Figure 3.15 illustrates the magnet attachment method developed for the outer rotor of the proposed LC DD-PMSG concept. Each row (pole) of segmented magnets is bonded with adhesive into shallow flat-bottomed channels formed by the bound stack of electrical steel laminations. Two rows of retainer clips slide into a single retainer groove that sits between each row of magnets. The retainer clips are short lengths cut from an extruded aluminum profile sized to clamp down on the magnets. Hard anodizing of the clips offers electrical isolation. These rows of retainer clips are intended as a backup magnet retention system should the primary adhesive bonding system fail.

A complete rotor section assembly is shown in Figure 3.16. The assembly includes the 10 large cross tubes that fix each rotor section to the rotor wheel structure. Because of the considerable magnetic attraction forces that develop between the stator and rotor facing surfaces, putting the rotor and stator together is a significant assembly challenge for any large permanent magnet machine. A nice feature of the outer rotor and rotor wheel structure design for the proposed LC DD-PMSG is that the rotor magnet sections can be installed after the rotor wheel structure is assembled around the stator. With the large
cross tubes already installed, the 12 rotor section assemblies can be eased into position on the rotor wheel structure one at a time, temporarily being held in place by the magnetic attraction forces.

Figure 3.15 Rows of magnets adhesively bonded to laminations and also held in place by rows of tensioned extruded aluminum retainer clips

Figure 3.16 Rotor section assembly showing 10 magnet poles and mounting cross tubes
Active Element Positioning

Figure 3.17 is a cross-sectional view of the stator and rotor facing surfaces for the 8 MW LC DD-PMSG showing the relative positioning of the active elements. The air gap between the rotor inner surface and the stator outer surface is 8 mm. This is the minimum possible gap that can be produced using standard manufacturing methods. The angular spacing between the tooth coils is 2.5°, and the spacing between rows of magnets is 3°. The rotor cross tubes shown at the top of the figure have a longer span than the stator cross tubes shown at the bottom, and there are fewer of them (120 versus 144). Consequently, the rotor cross tubes are bigger in diameter.

Axle and Bearings

There are multiple ways an LC DD-PMSG with an inner stator and outer rotor could be integrated with the drivetrain of a large wind turbine. The proposed conceptual embodiment assumes the stator is mounted to two flanges on a large hollow axle that will be fixed within the nacelle structure. Two large roller bearings mount to the axle on either
side of the stator mounting flanges. Surrounding and secured snugly to the outer races of these bearings are two rotor flange assemblies. The rotor, which envelops the stator, mounts to these two flange assemblies. Figure 3.18 offers a cutaway view of the axle with bearings installed showing the stator and rotor mounting flanges.

![Diagram of generator axle with stator wheel mounting flanges and rotor bearing flange assemblies](image)

**Figure 3.18** Cutaway view of fixed generator axle with stator wheel mounting flanges and rotor bearing flange assemblies

Each of the two rotor flange assemblies comprises a facing pair of flange halves and a rigid insulation component. The insulation isolates the flange pairs from the races to block any flow of electrical current through the bearings, which can result in deterioration and early bearing failure.

The bearings selected for this conceptual embodiment were recommended by SKF (Svenska Kullagerfabriken AB) to meet the predicted design loads and the 30-year design life requirement. The axially fixed rotor bearing is an SKF 710 mm, tapered-bore, double-row, Spherical Roller Bearing (SRB 240/7107 ECAK30/W33). The axially free bearing is an SKF 710 mm, tapered-bore, Compact Aligning Roller Bearing (CARB C 40/710 K30M). The SKF CARB uses toroidal rollers so it can self-align to accommodate limited axial displacement between the inner and outer races. The tapered inner race for each bearing is secured with a large bearing nut.

Cooling system feed and return lines for each stator sector pass through a circular array of 12 slot-shaped axle wall penetrations so connections to the primary coolant loop can be made within.
Layered Sheet Steel Stator and Rotor Wheel Structures

Once tangential stress has been maximized and overall dimensions minimized, the structural design of the LC DD-PMSG stator and rotor wheel structures has the next biggest influence on generator mass. The wheel structures must work together to resist the large permanent magnetic attraction forces that pull together the facing surfaces of the rotor and stator. Since the air gap must be kept small to optimize efficiency, the structures must hold overall radial deformation to an absolute minimum. The structures must also resist the large tangential electromotive forces produced by the generator. In addition, dynamic performance is important, and the harmonic responses of both the stator and rotor wheel structures must be compatible with expected excitations. For large generators in particular, harmonic response becomes more of an issue.

In this conceptual embodiment, a novel lightweight wheel structure is being introduced for both the stator and rotor. It is a spoked-wheel architecture; however, the spokes are not radial. Instead of radiating normal to the hub, they are slanted to come out tangentially. This orientation enables the wheel structures to provide maximum static structural performance with minimum mass when the wheel rim is subjected to both tangential and radial forces.

The spokes of the inner stator slant in the direction of the tangential forces acting on the outer diameter surfaces. When these tangential forces are applied, they bend the slanted spokes inward, pulling the outer diameter surfaces inward. When outward radial magnetic forces are applied, they bend the spokes outward. So, when both forces are applied simultaneously, the inward movement is opposed by the outward movement resulting in less overall radial deformation. In the same way, slanting the spokes of the outer rotor away from the tangential forces pulls its outer diameter outward in opposition to the inward pull of the magnetic forces, which again results in less overall radial deformation.

Another unique and important attribute of the new lightweight rotor and stator wheel architecture is its use of layered sheet-steel elements to form the spokes and rim of the wheel faces. When appropriately bound, friction between the layered sheet-steel elements establishes structural integrity with a substantial increase in structural damping in the direction normal to the stack. In addition, because of the stacked sheet metal approach, substantial spoke-and-rim wheel structures can be built up without having to weld together overly thick steel elements. Eliminating deep structural welds reduces manufacturing cost and eliminates problems associated with fatigue cracking and failure of welded connections [32]. Fatigue cracking is a serious problem for a large structure with a 30-year design life that is subjected to continual flexing and extreme temperatures.
Figure 3.19 depicts one of the sheet-steel elements used to make up the LC DD-PMSG stator wheel. For the proposed 8 MW generator, this sheet steel element is 5 mm thick. It can be cut from a rectangular sheet that is 2.8 m long and 1.6 m wide.

Figure 3.19 Punched or laser cut sheet steel element for stator wheel structure is 5 mm thick.

Figure 3.20 illustrates one of the wheel faces used in the stator wheel structure. Each wheel face comprises five layers of 12 elements laid out in a circular array. There are 60 total sheet-steel elements in each wheel face stacked to produce an overall thickness of 25 mm. Each new layer of sheet-steel elements is oriented 15° from the previous layer, so the seams between elements are staggered.
Figure 3.20 Full circular array of sheet-steel elements for one face of stator wheel structure
Figure 3.21 gives close-ups of the hub and rim regions to show how the element layers work together to build a strong planar structure.

One side of the wheel structure is assembled from two wheel faces bolted to either side of a single hub plate. Spacers sit between the wheel faces in line with the spokes to maintain the spacing established by the hub. For this embodiment, a polymer material is envisioned for the spacers, High Density Polyethylene (HDPE) for example, to introduce structural damping in the axial direction. Figure 3.22 illustrates one fully assembled wheel side for the stator wheel structure.

To form the wheel structure, a pair of wheel sides is joined together by a circular array of cross tubes. Figure 3.23 shows a cross tube. In addition to establishing the wheel structure, the 144 cross tubes of the stator wheel structure position and secure the 12 stator sections that make up the complete stator. The cross tubes are sized to withstand the predicted radial and tangential forces. The stator wheel structure also includes internal cross bracing to establish axial rigidity. Figure 3.24 shows a pair of cross braces, illustrating how they attach to the spoke spacers. The cross braces are bands of 5 mm thick sheet steel. Not shown in the figure, an elastomeric damper separates and pretensions the cross-brace bands.
Figure 3.22 One wheel side of the stator wheel structure showing the hub and HDPE spoke spacers - The close-up view in the upper corner shows the sheet steel element seams.
3 Presentation of Proposed Generator Concept

Figure 3.23 Stator cross tube assembly - The inner spacers fix the axial position of the laminations for each stator section.

Figure 3.24 Cross braces for the stator wheel structure, shown fastened to polymer spoke spacers - Not shown, an elastomeric damper, where the braces cross, separates and pretensions the bands.
Figure 3.25 illustrates the complete 8 MW LC DD-PMSG stator wheel structure with cross tubes and cross braces in place.

The outer rotor wheel structure architecture is similar to that of the stator wheel. Because fewer tubes must bridge a longer distance, the 120 cross tubes of the rotor wheel structure are bigger in diameter than the cross tubes of the stator. These cross tubes both position and secure the 12 rotor magnet sections needed for the rotor. Figure 3.26 shows the complete 8 MW LC DD-PMSG rotor wheel structure.
Figure 3.26  Complete wheel structure for the outer rotor of the proposed 8 MW LC DD-PMSG

**Stator and Rotor for 8 MW LC DD-PMSG**

The complete inner stator for the 8 MW LC DD-PMSG is shown in Figure 3.27. The figure illustrates how the liquid cooling system interfaces to the windings. The cooling connections for each stator sector are made inside the main axle.

Figure 3.28 shows the complete outer rotor assembly of the 8 MW LC DD-PMSG, and Figure 3.29 offers a close-up of the magnet sections. Because the cross tube slots of the rotor wheel structure are open, each rotor section can be eased into place after the rotor wheel has been assembled around the fixed stator. Because the magnetic attraction force between each rotor section and the stator wheel is so high, being able to install the rotor sections one at a time greatly simplifies the assembly process.
Figure 3.27 Complete stator for the proposed 8 MW LC DD-PMSG mounted on fixed axle
Figure 3.28 Complete rotor for the proposed 8 MW LC DD-PMSG with bearing insulation flange assemblies mounted onto the wheel hubs
3.2 Proposed Embodiment of an 8 MW LC DD-PMSG

Rotor Segments can be lowered onto Wheel Structure

Figure 3.29 Close-up view of magnet sections retained by rotor wheel structure - Because the cross tube slots are open, the rotor sections can be eased into place after the rotor wheel has been assembled around the fixed stator.

8 MW LC DD-PMSG: The Full Conceptual Embodiment

Figure 3.30 reveals the complete conceptual embodiment of the proposed 8 MW LC DD-PMSG. The man shown in the bottom left of the figure is 182 cm tall. The outer diameter of the machine as shown in the figure is 7 m. Not considering the axle, the generator is just under 2 m from front-to-back. The total dry mass, calculated by SolidWorks® for the model with axle and bearings, is approximately 85 tonne. Table 3.2 gives a summary of the basic dimensions and resulting masses for the conceptual embodiment.

<table>
<thead>
<tr>
<th>Physical summary of 8 MW LC DD-PMSG</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total Generator Mass</td>
</tr>
<tr>
<td>Outer Diameter</td>
</tr>
<tr>
<td>Air Gap Diameter</td>
</tr>
<tr>
<td>Front-To-Back Length</td>
</tr>
<tr>
<td>Mass of Copper</td>
</tr>
<tr>
<td>Mass of NdFeB</td>
</tr>
<tr>
<td>Stator Laminations Mass</td>
</tr>
<tr>
<td>Rotor Laminations Mass</td>
</tr>
</tbody>
</table>
Man is 182 cm tall.

Figure 3.30 Embodiment as proposed of complete 8 MW LC DD-PMSG
Chapter 4 describes the analytical and numerical predictions made and the experimentation carried out to examine the behaviors of the introduced LC DD-PMSG. This body of work has been published as Publications 2 through 7. The following Subsections 4.1 through 4.6 are summaries of these publications. Publication 1 was covered earlier in Subsection 3.1. Reference citations are not present in the following publication summaries, but can be found in the full texts included at the end of this dissertation.

4.1 Predicted Reliability of an LC DD-PMSG Design (Pub 2)

Publication 2 addresses the question of LC DD-PMSG reliability. It presents a reliability analysis for the 8 MW embodiment of the proposed generator architecture including primary and secondary liquid-cooling systems. For the publication, LC DD-PMSG reliability is determined analytically and assessed based on the number of failures per year, the MTTF, the MDT, the MTBF, availability, and unavailability.

Introduction

There is a wealth of ongoing development activity in wind turbine technologies seeking to improve the economics of wind energy by increasing reliability levels, producing better efficiencies, reducing size, and simplifying construction. For DD-PMSGs, one avenue of development that offers compelling cost and performance benefits is a switch from air to liquid cooling. However, introducing liquid cooling raises reliability questions. Will an LC DD-PMSG be as reliable as an air-cooled DD-PMSG over its design lifetime?
These reliability analyses considered a) the critical primary loop plumbing comprising tubing, manifolds, and connections and b) the constituent Original Equipment Manufacturer (OEM) components of the primary and secondary cooling loops. For the secondary side, both liquid-to-liquid and liquid-to-air cooling system approaches were evaluated. Published failure and repair data were used to quantify the reliability of plumbing elements and OEM components. The reliability analyses did not consider wind turbine electrical power and control systems.

A MATLAB® algorithm executed the calculations. The MATLAB® code makes it possible to quickly compare predicted reliabilities for various approaches.

**LC DD-PMSG Properties**

The stator copper losses calculated for the subject LC DD-PMSG are approximately 415 kW. The losses due to stator steel heating are 14.5 kW. Rotor surface losses are 1.5 kW. Because most of the Joule heating takes place in the stator copper, the LC DD-PMSG cooling concept relies primarily on direct liquid cooling of the copper windings to manage overall generator temperatures. The cooler stator temperatures and passive air cooling keep the rotor magnets cool.

As proposed, the stator for the LC DD-PMSG is divided into 12 sectors, each with 12 copper tooth-coil windings. Each tooth coil has a galvanically isolated coolant inlet and outlet, so there are 288 separate connections, which are subject to corrosion or sealing issues. These connections are a major concern from the standpoint of reliability.

Appropriate cooling fluids include demineralized and deionized water, a water/glycol mixture, or a synthetic dielectric cooling fluid, such as PAO. Water has superior heat transfer performance, but it cannot be used where ambient temperatures drop below freezing.

A primary coolant average temperature of 60°C and a flow rate of 1 m/s were assumed for this reliability study. The expected ambient temperature was 30°C. The assumed maximum stator and rotor operating temperatures were 85°C and 50°C, respectively.

Primary coolant flow velocity and temperature both influence loop reliability. Keeping the temperature of the coolant below 90°C prevents crevice or pitting corrosion on the inner stainless steel surfaces. Maintaining the appropriate coolant chemistries is also important. Often, liquid cooling conduit failures are the result of material cracking that precipitates when stresses act on internal surfaces that have become sensitized by an inappropriate coolant chemistry.
4.1 Predicted Reliability of an LC DD-PMSG Design (Pub 2)

Liquid-to-Liquid or Liquid-to-Air Systems

Both liquid-to-air and liquid-to-liquid cooling system approaches were considered in the analyses. In each approach, the OEM components on the liquid primary side comprised a deionizer, a centrifugal pump, a water reservoir, liquid filters, and an accumulator to accommodate fluid expansion.

For the liquid-to-air approach, the secondary-side auxiliary OEM components included five parallel units consisting of an air filter, a heat exchanger, and a blower. For the liquid-to-liquid approach, the secondary-side components were liquid filters, a single heat exchanger, and a centrifugal pump. With the liquid-to-liquid approach, only one heat exchanger unit is needed on the secondary side, because liquid-to-liquid heat exchange is more effective.

Reliability Equations

To facilitate the reliability analyses, the cooling system architectures were represented as constituent elements laid out in the appropriate arrangement of series and parallel circuits. A number of reliability equations for series and parallel circuits based on component repair and failure intensities were published by Villemeur and are applied here.

For a series of \( n \) elements, system unavailability \( UA_S \), system repair intensity \( \mu_S \), and system failure intensity \( \omega_S \) can be calculated using the following equations.

\[
UA_S(\infty) \approx \sum_{i=1}^{n} \frac{\omega_i}{\mu_i} \quad (4.1)
\]

\[
\mu_S(\infty) \approx \sum_{i=1}^{n} \frac{\omega_i}{\mu_i} \sum_{i=1}^{n} \frac{1}{\mu_i} \quad (4.2)
\]

\[
\omega_S(\infty) \approx \sum_{i=1}^{n} \omega_i \quad (4.3)
\]

In the equations, \( \mu_i \) is the repair intensity and \( \omega_i \) is the failure intensity of each element \( i \).

For parallel branches of \( n \) total elements where \( a \) are functional elements and \( b \) are non-functional elements and for a binomial distribution, numbers of event occurrences can be expressed in terms of a binomial coefficient. The coefficients for the functioning and nonfunctioning elements of the system are:

\[
C_n^a = \frac{n!}{a! (n-a)!} \quad \text{and} \quad C_n^b = \frac{n!}{b! (n-b)!} \quad (4.4)
\]
System unavailability $UA_S$, system repair intensity $\mu_S$, and system failure intensity $\omega_S$ can be expressed in terms of these coefficients from the next three equations.

\begin{align*}
UA_S(\infty) &\approx \sum_{b=1}^{a-1} C_n^b \left( \frac{\omega_i}{\mu_i} \right)^{n-b} \\
\mu_S(\infty) &\approx \frac{(n-a+1) \mu_i C_n^{a-1} \mu_i^{n-a} \omega_i^{n-a+1} \sum_{i=a}^{n} C_n^b \mu_i^{n-b} \omega_i^{n-b}}{\sum_{i=a}^{n} C_n^b \mu_i^{n-b} \omega_i^{n-b}} \\
\lambda_S(\infty) &\approx \frac{a \omega_i C_n^a \mu_i^{n-a} \omega_i^{n-a} \sum_{b=a}^{n} C_n^b \mu_i^{n-b} \omega_i^{n-b}}{\sum_{b=a}^{n} C_n^b \mu_i^{n-b} \omega_i^{n-b}}
\end{align*}

MTTF, MDT, and MTBF for the system can be calculated using:

\begin{align*}
MTTF_S &= \frac{1}{\omega_S} \\
MDT_S &= \frac{1}{\mu_S} \\
MTBF_S &= MTTF_S + MDT_S
\end{align*}

Reliability and availability are the two primary measurable reliability properties of a repairable system. Reliability $R$, defined as a function of time $t$, and system availability $A_S$ can be determined as follows.

\begin{align*}
R(t) &= e^{-\omega t} \\
A_S &= 1 - UA_S
\end{align*}

**Reliability Analysis of the Primary Cooling Loop Plumbing**

In addition to its constituent auxiliary OEM components, the primary cooling loop included various plumbing elements such as tubing, manifolds, and connections. Figure 4.1 illustrates. The 12 sectors of the stator represented 12 identical parallel circuits. Each circuit comprised a) an inlet tube leading to b) a coolant manifold with c) 12 connections to d) 12 tooth-coil cooling conduits, which e) return through 12 connections to f) the coolant manifold and g) an outlet tube.

Applying failure intensities and MDT values taken from the literature for the tubing, connectors, and manifolds; the reliability Equations (4.1) through (4.12) were applied to predict the fundamental reliability parameters for the primary cooling loop plumbing. The values determined for the key parameters are presented in Table 4.1.

As a function of time over a 30-year period, the reliability of the primary loop plumbing drops linearly from its beginning maximum of 1 to 0.933.
4.1 Predicted Reliability of an LC DD-PMSG Design (Pub 2)

Figure 4.1 Schematic representation of a single primary coolant loop circuit

Table 4.1 Key Reliability Parameters for Primary Loop Plumbing

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failures per Year ($\mu$)</td>
<td>$3.3 \times 10^{-3}$</td>
</tr>
<tr>
<td>MTTF$_S$</td>
<td>303 years</td>
</tr>
<tr>
<td>MDT$_S$</td>
<td>14 hours</td>
</tr>
<tr>
<td>MTBF$_S$</td>
<td>303 years</td>
</tr>
<tr>
<td>Availability ($A_S$)</td>
<td>$\approx 1$</td>
</tr>
<tr>
<td>Unavailability ($UA_S$)</td>
<td>$5.24 \times 10^{-6}$</td>
</tr>
</tbody>
</table>

Reliability Analysis of Loop OEM Components

For the loop component reliability analysis, auxiliary OEM components were situated in series or parallel as appropriate to the subject cooling loop configuration. Again, the failure intensities and MDTs for the components used in both the primary and secondary cooling loops were taken from the literature.

Deionizers, filters, blowers, and pumps have relatively short design lifetimes, so their failure intensities are high. These components must be continually serviced and finally changed out at end-of-life. The deionizers and water filters must be changed out once a year. The pumps and blowers must be replaced every 10 years.

Based on the reliability Equations (4.1) through (4.12), fundamental system reliability parameters were determined for the auxiliary OEM components of the primary and secondary loops for both the liquid-to-liquid and liquid-to-air cooling system approaches. The values for the key parameter are presented in Table 4.2.
Table 4.2 Key Reliability Parameters for Auxiliary OEM Components

<table>
<thead>
<tr>
<th></th>
<th>Liquid-to-Liquid</th>
<th>Liquid-to-Air</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failures per Year ($\mu S$)</td>
<td>1.8</td>
<td>1.9</td>
</tr>
<tr>
<td>MTTF$_S$</td>
<td>0.55 years</td>
<td>0.54 years</td>
</tr>
<tr>
<td>MDT$_S$</td>
<td>4.3 hours</td>
<td>4.3 hours</td>
</tr>
<tr>
<td>MTBF$_S$</td>
<td>0.55 years</td>
<td>0.54 years</td>
</tr>
<tr>
<td>Availability ($A_S$)</td>
<td>$\approx 0.991$</td>
<td>$\approx 0.991$</td>
</tr>
<tr>
<td>Unavailability ($U_A$)</td>
<td>$8.8 \times 10^{-4}$</td>
<td>$1 \times 10^{-3}$</td>
</tr>
</tbody>
</table>

Reliability as a function of time is plotted in Figure 4.2. As illustrated by the figure, liquid-to-air system reliability degrades rapidly over time. Since the secondary side of the liquid-to-air cooling system uses five parallel heat exchanger and blower units compared to the single exchanger and pump used by the liquid-to-liquid cooling system, the dramatic difference in predicted reliability for the two cooling approaches makes sense. Its reduced number of OEM components makes the liquid-to-liquid cooling approach inherently more reliable.

**Combined Reliability Analyses**

Combining the reliability analysis results for the primary coolant loop plumbing with the results from the analysis of the auxiliary OEM components makes it possible to predict key reliability parameter values for both the liquid-to-liquid and liquid-to-air cooling system approaches to LC DD-PMSG cooling. Table 4.3 lists these values.

Table 4.3 Key Reliability Parameters for Complete Cooling Systems

<table>
<thead>
<tr>
<th></th>
<th>Liquid-to-Liquid</th>
<th>Liquid-to-Air</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failures per Year ($\mu S$)</td>
<td>2.4</td>
<td>2.4</td>
</tr>
<tr>
<td>MTTF$_S$</td>
<td>0.43 years</td>
<td>0.43 years</td>
</tr>
<tr>
<td>MDT$_S$</td>
<td>4.3 hours</td>
<td>4.6 hours</td>
</tr>
<tr>
<td>MTBF$_S$</td>
<td>0.43 years</td>
<td>0.43 years</td>
</tr>
<tr>
<td>Availability ($A_S$)</td>
<td>0.999</td>
<td>0.999</td>
</tr>
<tr>
<td>Unavailability ($U_A$)</td>
<td>$1.1 \times 10^{-3}$</td>
<td>$1.2 \times 10^{-3}$</td>
</tr>
</tbody>
</table>
Publication 3 evaluates the proposed conceptual design solution for an 8 MW LC DD-PMSG and examines key aspects related to the design, including tangential stress, current density, linear current density, heating factor, and generator efficiency at full and partial load. The performance characteristics of a variable-speed wind turbine based on the proposed LC DD-PMSG drivetrain are determined in terms of annual energy production and load factor for a particular set of wind conditions.

Introduction

Wind turbine economics is a trade-off between expenses, the initial costs of putting a wind turbine into operation plus its long-term operating costs, and the income from electricity production over the life of the system. The ideal wind turbine generator should
be inexpensive to commission, operate at minimal cost for its design life, and maximize average electricity production. The capital cost of a generator is heavily dependent on its size, and particularly, on the amount of permanent magnet and copper materials called for by the design. To minimize operating cost, the generator must be simple and robust in design and construction and must function reliably. Average electricity production is maximized when uptime and average efficiency of energy conversion are maximized.

Electrical machine efficiency is determined by several independent factors such as type, size, operating speed, loading, materials, and operating regime; so there is no single efficiency for any particular generator. Hydroelectric generators have efficiencies of approximately 95%. Wind turbine DD-PMSG efficiencies usually range from 94 to 95%. Both generator types are typically low-speed machines featuring short, but relatively large diameter rotors.

The proposed LC DD-PMSG is also a low-speed machine; however, it develops higher tangential forces in the air gap making it possible to dramatically reduce rotor diameter and overall generator size. When optimized for minimal mass, the predicted peak load efficiency of an LC DD-PMSG is approximately 93%. While this efficiency may seem slightly lower than those offered by traditional DD-PMSGs, the LC DD-PMSG offers excellent partial load efficiencies, which results in better overall electricity production in wind energy applications. Since the LC DD-PMSG is significantly smaller and less massive, it offers lower capital, logistics, and installation costs, which makes it a more economic wind turbine drivetrain solution.

### Tangential Stress and Copper Mass in an LC DD-PMSG

If the radial flux and stator linear current densities in an electrical machine are sinusoidal and exactly in phase, then the average value of the tangential stress in the air gap $\sigma_{\tan}$ can be expressed as follows.

$$
\sigma_{\tan} = \frac{B_{\text{peak}} A}{\sqrt{2}}
$$

(4.13)

$B_{\text{peak}}$ is the peak air gap flux density of the working harmonic. It represents the magnetic load. $A$ is the root mean square value of linear current density fundamental. It represents the electrical load.

Writing the equation for machine torque $\tau$ in terms of tangential stress and rotor diameter $d$ shows that output torque is proportional to tangential stress, the cube of the rotor surface diameter, and the ratio of rotor active length to rotor diameter $l/d$.

$$
\tau \propto \sigma_{\tan} \frac{(l/d) d^3}{d^3}
$$

(4.14)
Total generator mass $m_{\text{gen}}$ can be approximated in terms of $l/d$, the average density of the active materials $\rho_{\text{am}}$, and the coefficients $k_{\text{str}}$ and $k_{\text{dia}}$. See Equation (4.15).

$$m_{\text{gen}} \propto (l/d) k_{\text{str}}(k_{\text{dia}}d)^3 \rho_{\text{am}} \quad (4.15)$$

The structural coefficient $k_{\text{str}}$ is the total mass of the generator structure divided by the mass of the active materials, and the coefficient $k_{\text{dia}}$ is the ratio of the outermost active materials diameter to the innermost active materials diameter; a measure of total active materials thickness. For the proposed outer rotor design, $k_{\text{dia}}$ is the outer diameter of the rotor’s active materials divided by the inner diameter of the stator’s active materials. The $k_{\text{dia}}$ needed is a function of the number of rotor pole pairs. A machine with fewer pole pairs requires a larger $k_{\text{dia}}$. Increasing the number of pole pairs permits using a smaller $k_{\text{dia}}$. As the number of pole pairs approaches infinity, $k_{\text{dia}}$ approaches one.

The ratio of machine torque to total generator mass is referred to as torque density and represented as $\lambda_\tau$. Torque density can be written in terms of tangential stress $\sigma_{\text{tan}}$, the average density of the active materials $\rho_{\text{am}}$, and the introduced coefficients $k_{\text{str}}$ and $k_{\text{dia}}$, as follows.

$$\lambda_\tau = \frac{T}{m_{\text{gen}}} \propto \frac{\sigma_{\text{tan}}}{k_{\text{str}}k_{\text{dia}}^3 \rho_{\text{am}}} \quad (4.16)$$

Equation (4.16) indicates that machine torque density is proportional to tangential stress and inversely proportional to both the average mass density of the active materials and the coefficients $k_{\text{str}}$ and $k_{\text{dia}}$. The equation also reveals that machine torque density is fully independent of rotor size, but grows rapidly as $k_{\text{dia}}$ values drop. Low speed electrical generators typically have significantly more pole-pairs than do high-speed generators, so they can be designed with substantially smaller $k_{\text{dia}}$ values. The use of high pole-pair numbers in high-speed machines is limited by iron losses that increase with operating frequency. The advantage of the low-speed, high-tangential-stress PMSG architecture in terms of torque density is reasonably clear.

The amount of copper in the stator of a DD-PMSG influences its efficiency. In the subject low-speed high-torque machine, Joule heating in the copper accounts for more than 85% of total losses. The effect of copper mass on efficiency can be quantified in terms of the product of copper linear current density $A$ and current density $J$ for the stator $s$. Referred to as the heating factor, the expression for $AJ_s$ can be written as follows.

$$AJ_s = 2K\nu_{\text{r}}\sigma_{\text{tan}} \frac{1 - \eta}{\eta} \quad (4.17)$$

In the equation, $\eta$ is efficiency, $\nu_{\text{r}}$ is the linear surface speed of the rotor, and $K$ is a grouping of coefficients and parameters including the winding factor of the working harmonic $k_{\text{wh}}$, the end winding factor $k_{\text{we}}$, the alternating current resistance coefficient...
$k_R$, the electromotive force coefficient $k_E$, the electrical resistivity of the copper $\rho_{cu}$, and the load angle $\delta$. Equation (4.18) is the expression of $K$.

$$K = \frac{k_{wh} \cos \delta}{\rho_{cu} k_R k_{we} k_E}$$  \hspace{1cm} (4.18)

Equation (4.17) reveals an interesting and important characteristic of the LC DD-PMSG. Efficiency $\eta$ and heating factor $A_J$ are to some extent inversely proportional. That is, a drop in the value of the heating factor represents an increase in efficiency. The units of $A$ and $J$ are $A/m$ and $A/m^2$, respectively. Therefore, the heating factor units are $A^2/m^3$, where $m^3$ represents the electrical machine’s copper and tooth volume in cubic meters. So, increasing active copper volume works to reduce the value of the heating factor, which means that heating factor value drops with increasing copper mass. Or in other words, machine efficiency increases with increasing copper mass.

**Design Concept**

The following paragraphs introduce one possible electromagnetic design for an LC DD-PMSG. Because of the generator’s large physical size, the design is based on segmented construction. That is, the rotor and stator are each divided into 12 identical sectors. Each stator sector includes 12 slots for windings. Each rotor sector includes 10 magnet poles. The combined sectors of the generator comprise 144 slots and 120 poles, i.e., 60 pole pairs.

The 12/10 slot/pole combination represents a $q = 0.2$ and $m = 6$ fractional slot winding. The 12/10 winding configuration works at the fifth harmonic of the stator current linkage. The design uses a double-layer (two adjacent coil sides per slot), duplex-helical, concentrated (tooth-coil) winding geometry. The double-layer winding configuration results in lower sub-harmonics in the current linkage distribution and shorter end windings. The electrical machine is designed for a 6-phase electric power system. In a 6-phase system, the 12/10 slot/pole combination offers a 0.966 operating harmonic winding factor. Alternatively, a 3-phase ($m = 3$) version of the same arrangement should result in a winding factor of 0.933. The 6-phase system utilizes copper more efficiently than the 3-phase version.

The tooth-coil conductor is extruded oxygen-free copper of rectangular cross section with an internal coaxial stainless steel conduit to accommodate liquid coolant flow. The rectangular shape of the cross section maximizes the amount of copper in the windings. Each coil is formed to a minimum recommended bend radius, which establishes overall coil width. Coil width, the number of coils, and their minimum practical spacing determine the diameter of the stator. Correspondingly, stator diameter sets the air gap diameter of the generator.
Figure 4.3 is a photo of a pair of duplex-helical tooth-coils that were fabricated to demonstrate the manufacturability of the windings concept. The coil up-bend near the top in the photo is necessary. It makes it possible for the helical conductor winding to step up in level, while still keeping all the coil active lengths parallel.

Stainless steel tubing is coaxial to copper conductor

An 18 × 15 mm hybrid liquid-cooling conductor was used for this proposed 8 MW design. The outer and inner diameters of the internal coaxial 316 stainless steel tubing are 7 and 5.5 mm, respectively. Each tooth coil is 72 mm wide. With practical spacing, the array of 144 coils results in a minimum possible generator air gap diameter of 7 m.

An optimization algorithm using the direct search method and targeting minimization of copper, permanent magnet, and iron masses was applied to define initial target values for generator diameter, active length, and number of conductors per slot. Electromagnetic performance was predicted using a methodology proposed by Pyrhönen. Thermal performance evaluation was carried out via the procedure described by Holman.
Table 4.4 lists the key specification values established for some of the most important generator design parameters.

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated Power</td>
<td>8 MW</td>
</tr>
<tr>
<td>Conductor per Slot</td>
<td>20</td>
</tr>
<tr>
<td>Rated Speed</td>
<td>11 rpm</td>
</tr>
<tr>
<td>Copper Mass</td>
<td>8.1 t</td>
</tr>
<tr>
<td>Line-to-Line Voltage</td>
<td>3.3 kV</td>
</tr>
<tr>
<td>Magnet Mass</td>
<td>4.5 t</td>
</tr>
<tr>
<td>Number of Phases</td>
<td>6</td>
</tr>
<tr>
<td>Generator Mass</td>
<td>92 t</td>
</tr>
<tr>
<td>Tangential Stress</td>
<td>80 kPa</td>
</tr>
<tr>
<td>Copper Losses</td>
<td>550 kW</td>
</tr>
<tr>
<td>Stator Length</td>
<td>1.15 m</td>
</tr>
<tr>
<td>Total Losses</td>
<td>651 kW</td>
</tr>
<tr>
<td>Air Gap Diameter</td>
<td>6.93 m</td>
</tr>
<tr>
<td>Electrical Efficiency</td>
<td>92.6%</td>
</tr>
</tbody>
</table>

**Tooth Coils Prototype**

To demonstrate the proposed LC DD-PMSG liquid-cooling technology, a pair of helical, double-layer, non-overlapping, tooth-coil windings were fabricated from a solid copper conductor surrounding an internal coaxial coolant conduit of type 316 stainless steel. See the previous Figure 4.3. The hybrid liquid-cooling tooth coils were fitted into a section of bound laminations and plumbed into the primary side of an instrumented closed-loop dry cooling system. Fabricating the hybrid tooth coils verified their manufacturing feasibility. Running the loop demonstrated the effectiveness of the internal coaxial coolant conduit liquid-cooling approach. ECOCUT HS PAO heat transfer fluid was the coolant used for the primary loop. Figure 4.4 is a photograph of the prototype cooling loop.

Electrically, the pair of tooth coils was connected in series. A variable frequency 550 Hz synchronous generator supplied 100 A current to the tooth coils to induce Joule heating. The high frequency was used to induce more losses in the coils, because a 1000 - 2000 A low-frequency source was not available. Coolant liquid fed into and out of each tooth coil via a parallel connection to a coolant manifold. Coolant temperature, pressure, and fluid mass flow sensors were positioned to monitor inlet and outlet coolant conditions. Three 100 Ω platinum resistance temperature detectors for each coil measured copper temperature at the inlets, the outlets, and at the coil midpoints.

To evaluate the cooling performance of the hybrid liquid-cooling tooth coils, electrical power was applied to the prototype with coolant flowing through each coil at 2 l/min. The system was allowed to heat up for several hours until temperatures reached a steady state. Once at steady state, data recording was triggered. Figure 4.5 shows the temperatures recorded over a 5-hour time period. All the temperature measurements read approximately 40°C for the first 4 hours. From Figure 4.4, the left and right coils are designated Coil A and Coil B, respectively.
4.2 Electromagnetic Characteristics of an LC DD-PMSG

Figure 4.4 Prototype primary coolant loop with duplex tooth coils - (1) tooth coil, (2) galvanically isolated coolant manifold, (3) coolant reservoir, (4) pump, (5) heat exchanger, (6) filter, (7) flow transducer, (8) pressure transducer, (9) thermocouple, (10) resistance thermometers

Figure 4.5 Temperature measurement results over 5-hour period
At 4 hours, the pump was turned off to stop coolant flow. As expected, copper temperatures began to rise rapidly, and coolant temperature began to drop over the next 45 minutes. The pump was then turned back on. As the figure shows, copper and coolant temperatures almost immediately returned to the area of 40°C.

**Power, Partial Load Efficiencies, and AEO for 8 MW LC DD-PMSG Design**

To demonstrate the performance of this particular 8 MW LC DD-PMSG design in a wind turbine application, its AEO and load factor were calculated using published North Sea wind data.

Turbine generator power $P_{\text{gen}}$ depends on how much wind is driving through the main rotor blades. This power can be expressed in terms of wind speed $\nu_{\text{air}}$ and rotor blade radius $r_{\text{rb}}$ as follows.

$$P_{\text{gen}} = \frac{1}{2} \rho_{\text{air}} \pi r_{\text{rb}}^2 \nu_{\text{air}}^3 C_P(\lambda_{\text{rb}}, \beta_{\text{rb}})$$  \hspace{1cm} (4.19)

In the equation, $\rho_{\text{air}}$ is the air density, and $C_P$ is the dimensionless rotor power coefficient, which is a function of the tip speed ratio $\lambda_{\text{rb}}$ and blade angle $\beta_{\text{rb}}$ of the rotor blades.

By employing a control strategy reported by Hansen, wherein the frequency converter directly controls generator speed, several operating regions for a direct-drive wind turbine can be defined based on incoming wind speed, rotor speed, and wind turbine mechanical power. The family of curves presented in Figure 4.6 illustrates how generator power varies as a function of rotor speed for a number of wind speeds and corresponding blade angles.

Between points A and B, main rotor speed increases linearly. At point B, the rotor blades reach their maximum allowable speed of rotation, which corresponds to a tip speed of 90 m/s. From that point on, rotor blade speed is held constant.

The electrical power produced by a wind turbine is the product of the mechanical power extracted from the wind and the efficiency of the generator. Generator efficiency varies with load, so partial load efficiencies must be used to determine total wind turbine energy production over time with respect to varying wind conditions. Figure 4.7 is a map of generator efficiency as a function of main rotor torque and speed. The torque-speed characteristics of the proposed LC DD-PMSG design are shown by the bold black curve. As the curve indicates, the efficiencies for a direct-drive machine are good at lower rates of rotation: above 95% below 9 rpm.
4.2 Electromagnetic Characteristics of an LC DD-PMSG

Figure 4.6 Wind turbine power curve and corresponding power coefficient curves

Figure 4.7 Efficiency map for proposed 8 MW LC DD-PMSG concept
The average wind speed for North Sea coastal waters at heights greater than 30 m above sea level is approximately 9 m/s.

Wind speed distributions are commonly modelled using Weibull and Rayleigh probability density functions. In this case, a Weibull shape parameter value of 2.17 and a Rayleigh scale parameter value of 10.6 were selected to represent North Sea winds. The average power available from the wind turbine was estimated by integrating the product of generator power output at each wind speed by the probability of occurrence of that wind speed.

Figure 4.8 shows the resulting distribution of wind energy content superimposed on the Weibull wind speed distribution. The figure also shows power output as a function of wind speed for the 8 MW LC DD-PMSG.

To calculate AEO, total average generator output power was multiplied by the number of hours per year that the wind turbine operates assuming 97% uptime. For this proposed 8 MW LC DD-PMSG design, the estimated AEO is 33.8 GWh. Because the LC DD-PMSG design offers higher partial load efficiencies, wind turbine drivetrains based on the architecture offer higher annual energy output.
4.3 Establishing Optimal EM Geometries for an LC DD-PMSG

Publication 4 describes the development of a simple MATLAB®-based algorithm that employs a direct search method with variable step size to define, based on performance requirements, the basic parameters needed to begin the design of an LC DD-PMSG optimized for minimum material cost and mass. The output, combined with an existing set of LC DD-PMSG design guidelines, makes it possible to converge quickly on an appropriate initial electromagnetic geometry.

Introduction

Establishing the optimal initial geometries for an electrical machine is complicated by a large number of unknowns, by the nonlinear relationships between electrical machine parameters, and by the practical need to use readily available and cost effective materials and material sizes. To minimize capital costs, the primary optimization objective is to minimize the cumulative masses of the permanent magnet materials, the windings copper, and the electrical steel. The objective function for this optimization is nonlinear. Ultimately, therefore, deciding on optimal initial geometries depends on the experience and judgment of the lead engineer.

Seeking to simplify initial design specification steps, a simple MATLAB®-based algorithm was developed to set, within specific electromagnetic, thermal, and manufacturability constraints, the most appropriate air gap diameter, active length, and number of conductors per slot from which detailed LC DD-PMSG design development work can begin for a particular solution. The algorithm is based on a direct search method that allows variable step size.

Basic LC DD-PMSG Design

The proposed LC DD-PMSG architecture uses concentrated non-overlapping tooth-coil windings. The tooth-coil conductor has a rectangular cross section and includes an internal coaxial stainless steel conduit to accommodate liquid coolant flow. The electromagnetic architecture is based on a 12/10 windings configuration. Figure 4.9 illustrates one configuration of a duplex-helical LC DD-PMSG tooth-coil winding.

This winding type imposes practical geometry limitations. The width of the coil is determined by the acceptable minimum bending radius of the copper conductor, which is itself determined by the width of the copper cross section. The minimum diameter of the LC DD-PMSG stator is then a function of coil width and the total number of coils needed.
Some initial requirements must be set to begin the process of determining an optimal electromagnetic geometry for an LC DD-PMSG architecture. For the example here, the requirements were as follows.

### Table 4.5 Initial Requirements for LC DD-PMSG Design

<table>
<thead>
<tr>
<th>Requirement</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power</td>
<td>8 MW</td>
</tr>
<tr>
<td>Speed</td>
<td>11 rpm</td>
</tr>
<tr>
<td>Line-to-Line Voltage</td>
<td>3.3 kV</td>
</tr>
<tr>
<td>Number of Poles</td>
<td>120</td>
</tr>
<tr>
<td>Rated Frequency</td>
<td>11 Hz</td>
</tr>
<tr>
<td>Number of Slots</td>
<td>144</td>
</tr>
</tbody>
</table>

### Constraints and Objective Function

A set of constraints was established for the optimization algorithm to bound the number of possible solutions. A maximum temperature of 80°C and a maximum total pressure drop of 2 bar (200 kPa) were set for the liquid coolant in the primary loop. Staying below these maximum values improves reliability. Because generator efficiency drops continuously with increasing tangential stress, a lower limit of 92% was set for generator efficiency. Also for reliability assurance, a minimum torque overload capability of 130% of rated torque was established as a constraint. Finally, a minimum power factor of 0.7 was set to ensure reasonable converter overhead.

The objective function for the optimization algorithm calculates the total masses of the active copper $m_{cu}$, the permanent magnet material $m_{pm}$, and the active and inactive
steel $m_{fe}$. The steel calculation assumes the mass of inactive structural steel is 35% of the sum of the active steel and active copper masses. This percentage was estimated by reviewing the inactive-to-active steel ratios for actual full size generator design models.

Algorithm Description

The optimization goal for the algorithm is to minimize active masses. Mass values are determined numerically from LC DD-PMSG models. Generator electromagnetic design is guided by the methodology proposed by Pyrhönen. Thermal performance is estimated using the procedure presented by Incropera. Figure 4.10 is a flowchart detailing the decision steps for the optimization algorithm.

In box (1) of the flowchart, the two design variables are electromotive force factor $k_E$ and stator length $l_s$. For each iteration $j$, only one of the two design variables is changed. The expression $n = [1, 2]$ indicates the order number of the design variable that changes. The expression $n(1) = 1$ indicates that $k_E$ is changing, and $l_s$ remains fixed; and $n(2) = 2$ indicates that $l_s$ is changing, while $k_E$ remains fixed. The starting design variable values are $\gamma_{ini} = [k_{E,ini}, l_{s,ini}]$. The expression $\Delta = [\Delta k_E, \Delta l_s]$, defines the discretization step size for each design variable. The variable $\alpha \in [\alpha_{min}, 1]$ is a decreasing scaling factor (for $k_E$ or $l_s$), with $\alpha_{min}$ being the minimum value of $\alpha$. Once $\alpha = \alpha_{min}$, the algorithm stops iterating. The variable $m^*$ represents the objective function for each iteration, which is the total active mass of magnet material, copper, and steel.

In (2), the value of $\Delta(n)$ is scaled for each iteration, that is, $\Delta(n)_{new} = \Delta(n)_{old} \times \alpha$. To begin, $\alpha = 1$. For each iteration $j$, the design variable is calculated at three different points $\xi(i = 1, 2, 3)$. The second point $\xi(2)$ refers to the current value $\xi(2) = \gamma(n)$ of the design variable. The first $\xi(1)$ and third points $\xi(3)$ are equal to the $\xi(3) = \gamma(n) - \Delta(n)$ and $\xi(3) = \gamma(n) + \Delta(n)$ of the changing design variable.

In (3), calculations are carried out to determine geometric dimensions, electromagnetic parameters, liquid-cooling parameters, and the mass objective function for the three values of the subject design variable $\xi$. Conductor cross section, slot wedge height, the basic windings configuration, inlet coolant temperature, allowable flux densities in the electrical steels, insulation thickness, air gap diameter, air gap, effective permanent magnet width, and the number of conductor per slot width are all held constant.

Initial air gap diameter $d$ is set arbitrarily as a multiple of the base width $w_{cu}$ of the copper conductor, that is, $d_{ini} = 385 \times w_{cu}$. Air gap is kept at its minimum practical value; 0.125% of air gap diameter. Effective permanent magnet width $w_{pm}$ is held constant at
4 Analytical and Experimental Evaluation of LC DD-PMSG Concept

\[ n = [1, 2] \text{ - order number of design variable;} \]
\[ n(1) \text{ is the initial value for the } 1^{st} \text{ iteration; } \Delta_{ini} = [\Delta_{kE}, \Delta_{l}] \; ; \]
\[ \Delta = \Delta_{ini} \text{ initial discretization step sizes;} \]
\[ \gamma_{ini} = [\gamma_{kE, ini}, \gamma_{l, ini}] \text{ starting values of design variables;} \]
\[ \alpha_{ini} \in [\alpha_{ini}, 1]; \alpha_{ini} = 0.01; \alpha = 1 \text{ initial value;} \]
\[ m^*_1 = 0 \text{ is the first value of total active mass;} \]
\[ j \text{ represents an iteration cycle} \]

\[ \Delta(n)_{new} \rightarrow \Delta(n)_{old} \times \alpha \]

Calculation points \( i = 1, 2, 3 \) at
\[ \xi = [\gamma(n) - \Delta(n), \gamma(n), \gamma(n) + \Delta(n)] \]

Analytical calculation for PMSG: geometric dimensions, Electromagnetic parameters, water-cooling parameters, and the objective function
\[ m(\xi(i)) = m_{pm}(\xi(i)) + m_{lc}(\xi(i)) + m_{cu}(\xi(i)) \]

\[ T_{out}(\xi(i)) \leq 80 ^{\circ}C \]
\[ \Delta P(\xi(i)) \leq 2 \text{ bar} \]
\[ \eta(\xi(i)) \geq 92\% \]
\[ \tau_{max}(\xi(i))/\tau_{gen} \geq 1.3 \]
\[ \cos(\xi(i)) > 0.7 \]

Yes

\[ m^*_j = \min(m(\xi(i)) \neq 0) \]
Delete \( i \) iteration

Yes

\[ m^*_j \geq m^*_{j-1} \]
\[ n = n(2) \]

No

\[ m^*_j < m^*_{j-1} \]

\[ m^* = m^*_j \]
\[ \alpha = \alpha \times 0.99 \]
\[ n = n(1) \]
\[ \gamma = \gamma_{j-1} \]

\[ m^* = m^*_j \]
\[ \alpha = 1 \; ; \; n = n + 1 \]
\[ \Delta = \Delta_{ini} \]
\[ \gamma = \gamma_{ini} \]

\[ m^* = m^*_{j-1} \]
\[ \alpha = \alpha \]
\[ n = n(2) \]
\[ \gamma = \gamma_{j-1} \]

Yes

\[ n = n(2) \]

No

\[ n = n(1) \]

Yes

\[ \alpha \geq \alpha_{min} \]

No

End and print

Figure 4.10 Iteration logic flow for the MATLAB® tool optimization algorithm
4.3 Optimal EM Geometries for an LC DD-PMSG (Pub 4)

a fraction of pole pitch: \( b_{\text{pm}} = 0.8 \times \tau_p \). There are four conductors across the width of each slot, representing double-layer, duplex-helical coils.

In (4), the analytical results are checked with respect to the following given set of constraints. Maximum coolant outlet temperature \( T_{\text{out}} \) must not exceed 80°C. Maximum total pressure drop \( \Delta P \) must not exceed 2 bar. Efficiency \( \eta \) must not drop below 92%. Maximum generator torque at constant speed must at least 130% of generator rated torque \( (\tau_{\text{max}}(\xi(i))/\tau_{\text{gen}} \geq 1.3) \). And, minimum power factor \( \cos \zeta \) must remain above 0.7. Failure of any of the conditions in (4) removes the iteration from further consideration (11).

In (5), the design variable value \( \xi(i) \) that yields the minimum resultant value for the active mass objective function \( m^* \) for iteration \( j \) under the given set of constraints is selected.

In (6), if the objective function value \( m^*_j \) is greater than the value for the previous iteration \( m^*_{j-1} \), and if the order number of the design variable is \( n = n(2) \), meaning there have been two successive iterations for both design variables, then the algorithm moves to step (7). Otherwise, the algorithm moves to step (12).

In (12), if the objective function has decreased, the algorithm moves to (13). Otherwise, the algorithm moves to (15).

In (13), the scaling factor \( \alpha \) is set to 1, the order-number-counter \( n \) is incremented, and the discretization step size is set to its initial value \( \Delta = \Delta_{\text{ini}} \). The starting values for the two design variables set for the next iteration are set to the final values from the current iteration \( \gamma = \gamma_j \).

In (14), if the order number is \( n = n(2) \), meaning there have been two successive iterations for both design variables, then it is reset to \( n = n(1) \). Otherwise, the order number is set to \( n = n(2) \). Then, the algorithm increments the iteration-counter \( j \) and starts the next iteration (10).

Step (15) takes place when the objective function did not change for two successive iterations. The scaling factor \( \alpha \) is left unchanged, and the order-number-counter is toggled to \( n = n(2) \). The starting values for the two design variables are set equal to the values from the previous iteration \( \gamma = \gamma_{j-1} \). Then, the algorithm increments the iteration-counter \( j \) and starts the next iteration (10).
In (7), the scaling factor is tweaked slightly ($\alpha = \alpha \times 0.99$). This decision to emphasize "search in depth" rather than "search in breadth" is because it is not possible to specify exactly which minimum value of $\Delta(n)$ must be set for a specific design variable. The order number is reset to $n = n(1)$, and the starting values for the design variables are set equal to the values from the previous iteration $\gamma = \gamma_{j-1}$.

In (8), if the scaling factor value remains greater than its preset minimum ($\alpha \geq \alpha_{\text{min}}$), the algorithm increments the iteration-counter $j$ and starts the next iteration (10). Otherwise, the algorithm moves to final step (9), which indicates that optimum values for design variables $k_E$ and $l_s$ have been determined.

**Case Study**

The optimization algorithm was used to determine the initial design parameters for an example LC DD-PMSG configuration based on the initial requirements of previously presented Table 4.5. Additionally, the example configuration included 12 equal machine sectors with 6-phase stator windings. The air gap diameter was set to 6.93 m and hybrid liquid-cooling conductor cross-sectional dimensions of $18 \times 15$ mm with a 5.5 mm inner conduit diameter were chosen as appropriate for an 8 MW machine.

The algorithm was initialized with discretization step sizes for the electromotive force factor and stator length of $\Delta k_E = 0.1$ and $\Delta l_s = 0.1$, respectively.

It took 9132 iterations and a total computation time of approximately seven minutes for the algorithm to arrive at a solution. The resultant values for load factor and stator length were 0.8 and 1.1 m, respectively. Resultant copper, magnet, and steel masses were 9.2, 3.6, and 31 tonne. Predicted efficiency was 92%, and copper losses were 580 kW.

Figure 4.11 shows how the calculated total mass and tangential stress values evolved during the iteration process.

To verify the efficacy of the algorithm results, a numerical simulation was carried out using the commercial finite element software CEDRAT Flux 2D. The copper losses calculated by Flux 2D were 620 kW at the rated point compared to 580 kW predicted by the algorithm. The root mean square value of induced voltage at no-load was 1500 V resulting in an electromotive force factor of 0.79, which compares to the algorithm predicted value of 0.8. Flux 2D predicted internal conductor temperatures did not exceed 80°C.
4.4 Mechanical Behaviors for the Wheel Structures (Pub 5)

In general, there was good agreement between the algorithm and Flux 2D results, and the slight differences can be attributed to some analytical simplifications made to the algorithm calculations to speed up computation. The algorithm seems to be effective in quickly establishing optimal initial geometries.

4.4 Mechanical Behaviors for the Wheel Structures (Pub 5)

Publication 5 examines the mechanical performance aspects of the unique wheel structures designed into the proposed LC DD-PMSG concept. The dominant forces are the magnetic attraction forces that act radially and the torque forces that act tangentially between rotor and stator. The stator and rotor wheel structures must withstand these large forces and maintain a constant and uniform rotor-to-stator air gap. Wheel structure design for a large PMSG is more about managing deformation than about limiting stresses. The slanted spoke and rim architecture of the wheel structures promises to provide adequate rotor-to-stator air gap management without all the extra steel.
Introduction

A key limitation of the DD-PMSG architecture for wind turbine applications is excessive size, weight, and material costs. At and above the 5 MW level, DD-PMSGs based on traditional concepts become too large to be economically viable. The direct-drive annular generator used in the 7.6 MW Enercon E-126 wind turbine, for example, is 12 m in diameter and 220 tonne in mass. To continue the current trend towards direct-drive wind turbine drivetrains, generator diameters should be kept below 8 m, and generator masses should be kept below 100 tonne.

Structurally, a generator is a pair of concentric wheels. In a DD-PMSG, the stator and rotor wheel structures must work together to resist large permanent magnetic attraction forces and large tangential electromotive forces. At the same time, the wheels must be dynamically stable. Particularly, as generator diameter becomes larger, dynamic response becomes more of an issue. For the structural designer, managing static structural deformation and dynamic performance while keeping internal stresses within safe limits are primary objectives.

This publication presents a novel concept for a lightweight stator wheel structure based on a slanted spoked-wheel architecture composed of layered sheet-steel elements. The geometry of the structure helps to manage both static and dynamic performance. The layered sheet-steel construction improves manufacturability and dynamic behavior.

A static structural analysis was carried out to demonstrate how the lightweight wheel structure responds to predicted tangential and radial forces. A numerical modal analysis predicted vibration characteristics. Finally, an EMA using a laser vibrometer validated the results of the numeric modal analysis. The actual size of the subject stator structure, which is 6.5 m OD, prohibited building a full-scale prototype, so a quarter-scale prototype was designed and built for the EMA.

Once the model had been validated, a second numerical modal analysis was carried out to predict the dynamic behaviors of a full-scale stator wheel structure.

Conceptual Design

A detailed description of the LC DD-PMSG conceptual design was previously presented in Subsection 3.2 of this dissertation. One can also be found in the full text of Publication 5.
4.4 Mechanical Behaviors for the Wheel Structures (Pub 5)

Radial and Tangential Forces

A prediction of the radial and tangential forces acting upon the stator wheel was needed to carry out the static structural analysis. An electromagnetic FE analysis was carried out using CEDRAT Flux 2D to determine the electromagnetic forces for a 30° segment of an 8 MW LC DD-PMSG. The outputs of the FE analysis were the resultant radial and tangential components of flux density under rated load in the air gap as a function of rotor position.

The radial and tangential electromagnetic forces calculated from these equations are presented in Figure 4.12. The values predicted at rated load using the Maxwell stress method for the radial and tangential forces averaged over a single 30° segment were $F_{rad} = 492.5 \text{ kN}$ and $F_{tan} = 169.3 \text{ kN}$, respectively.

The rotor of the proposed LC DD-PMSG has 120 magnetic poles and the rated rotor speed is 11 rpm, so the fundamental electrical frequency is 11 Hz.

The two frequencies of excitation relevant to wheel structure vibration response are the radial excitation frequency of 22 Hz, which is the result of the 120 magnetic poles of the rotor pulling radially on the stator teeth, and the torque cogging torsional excitation of 66 Hz, produced by the sixth harmonic of the fundamental machine frequency. For optimum dynamic performance, the natural radial vibration frequencies of the full-scale generator wheel structures should be at least 25% above 22 Hz or greater than 27.5 Hz.
and the natural torsional vibration frequencies should be at least 25% above 66 Hz or greater than 82.5 Hz.

**Static Structural Analysis**

A static structural analysis was carried out on a simplified FE model of the full-scale stator structure using ANSYS® Workbench® v15.0. The model was constructed in SolidWorks®, and then imported into ANSYS®. The axle was fixed, and the predicted radial and tangential forces were applied. Figure 4.13 shows the meshed model.

![Meshed model](image)

**Figure 4.13 Meshing of lightweight stator structure simplified for static structural analysis**

The FE-model of the wheel structure was meshed with solid tetrahedral elements using program-controlled course meshing. Convergence was ensured by repeating the analysis with finer mesh sizes. The specific solid elements included SOLID186 and SOLID187. Contact between elements was defined using CONTA174 and TARGE170. There were 494,039 elements with 3,024,795 degrees of freedom.
4.4 Mechanical Behaviors for the Wheel Structures (Pub 5)

Figure 4.14 illustrates the radial and circumferential deformations predicted for the lightweight stator structure when subjected to the outward magnetic forces and the clockwise tangential forces.

As the figure illustrates, the stator wheel structure performed as expected. In the left image, the 0.2 mm radial deformation (orange) of the spoke end region near the rim indicates that the spokes are moving inward. The maximum outward radial deformation, which occurs on the outer diameter of the stator segment body, is shown to be a negligible 0.04 mm. The image on the right in the figure shows the stator segment bodies moving 2.6 mm circumferentially in the clockwise direction. A peak stress of 99.6 MPa was predicted. It occurs in the fillet where the axle and wheel flanges meet. Elsewhere in the structure, the stresses are relatively low.

Vibration Characteristics of Quarter-Scale Wheel Structure

To understand the dynamic performance of the proposed wheel structure concepts, the normal modes and natural frequencies that define the structure’s vibration characteristics must be evaluated. A simplified numerical model is needed that can predict these characteristics, and the numerical model must be verified.
A quarter-scale layered-sheet-steel stator wheel structure was designed, fabricated, and assembled. Its radial vibration characteristics were measured using a laser vibrometer. Next, a simplified numerical model of the same quarter-scale structure was developed, and a modal analysis was carried out using ANSYS®.

Figure 4.15 is a composite photograph showing the assembled stator wheel structure and the zinc-iron coated, sheet-steel elements used to assemble it. The wheel comprises two pairs of spoked wheel faces connected by a circular array of threaded rods. The wheel faces are stacked circular arrays of the sheet-steel elements. Each new layer of arrayed sheet-steel elements is oriented 15° from the previous, so the seams between elements are staggered as shown on the right in the photo. HDPE polymer spacers sit between the spoke layers of each wheel-face pair. Interior braces cross diagonally from the near to far sides of the structure for axial rigidity.

Figure 4.16 shows the EMA setup used to take radial vibration measurements. The setup comprises a Bruel & Kjaer 4814 modal exciter, a Polytec PSV-500 scanning vibrometer, and the analyzer, a Polytec OFV-5000 vibrometer controller.

The EMA system was configured to measure vibrational velocity and displacement. Both the exciter and the wheel structure were hung from above to simulate a free-free constraint.
Figure 4.16  EMA radial measurement setup used to develop modal model for prototype wheel structure

The modal exciter pulsed the wheel structure with a pseudo-random signal within the excitation frequency range of 0-to-800 Hz. Ninety-five scanning points were measured using a complex average of three, and the sampling rate for the measurement was 2 kHz with 1600 fast Fourier transform lines.

The Scanning Laser Doppler Vibrometer (SLDV) measurement procedure and subsequent data processing produced a frequency response function plot relating velocity magnitude to frequency. Figure 4.17 shows the relevant range of frequencies of the plot for the radial SLDV measurements. The first five radial vibration mode peaks have been tagged. They occur at 270, 291, 304, 318, and 332 Hz, respectively.

Vibration measurement signals usually include noise. Superimposed onto the measurement signal, this noise results in a level of uncertainty in the extracted modal parameter data. Taking the average of several measurement samples is one common method used to minimize the uncertainty. This method was employed here. The Polytec software offers averaging in the time domain or in the frequency domain. In the frequency domain, a preset number of serial time traces are gathered. Using FFT, a spectrum is calculated for each time trace. Then, averaging all the values derived at each frequency produces an averaged spectrum. For the time domain, an averaged spectrum is produced in the same way.
Uncertainties may also arise in SLDV measurements if the laser beam is not properly aligned to the tested structure. To minimize these uncertainties, scan points are defined on the video image of the measurement plane to enable video triangulation, also known as two-dimensional and three-dimensional alignment. Laser beam positioning precision increases as a function of the number of scan points. Furthermore, precision increases if the video image occupies the entire measurement area of interest. Both the number of scan points and the video image were optimized to minimize uncertainty.

A simplified numerical model was constructed to carry out the modal analysis to predict the vibration characteristics of the prototype wheel structure. Nonlinear effects resulting from, for example, fastening methods were ignored. Linear elastic behavior was assumed for the wheel structure materials.

The two layered-wheel-face pairs, comprising the slanted spokes and wheel rims, were meshed as SHELL181 elements. All other structural components were meshed with SOLID186 and SOLID187 elements. The model mesh consists of 211,181 total elements with 2,336,571 degrees of freedom. To facilitate the connection of these element types and varying degrees of freedom, a bonded contact with a multi-point constraint formulation is used. The formulation helps to couple translational DOF from the solid surfaces to the rotational DOF of the shell edges. A free-free boundary constraint was applied. Surface-to-surface contact, CONTA174 elements and TARGE170 segment elements, were implemented on the solid element interfaces.

The modal analysis solution revealed four distinct radial vibration modes at 301, 310, 320, and 333 Hz, respectively. Their shapes correspond to radial modes 2, 3, 4, and 5.
There was good agreement between the measured and predicted results indicating that the numerical model represents a good tool for predicting basic vibration behavior. See Figure 4.18.

![Graph showing correlation of predicted versus measured frequencies for radial vibration](image)

**Figure 4.18 Correlation of predicted versus measured frequencies for radial vibration**

**Vibrations of Full-Scale Wheel Structure**

The numerical model was adapted to carry out a modal analysis of the full-scale version of the stator wheel structure. The analysis solution did not predict any torsional vibration modes; however, it did identify radial vibration modes 2, 3, 4, and 5 at 111, 129, 150, and 177 Hz, respectively. Figure 4.19 illustrates the radial modes. The critical radial excitation frequency for this wheel structure is 22 Hz, so the minimum radial modal frequency should be greater than 27.5 Hz. There seems to be a substantial margin of safety.

The modal analyses revealed that most of the low-frequency vibration modes are axial. The layered sheet-steel element construction of the proposed wheel structure concept increases damping in the axial direction. Although there is theoretically no axial excitation, further work should be carried out to postulate any possible deleterious effects of the axial vibration frequencies and identify possible solutions.
Radial Mode 2
Freq: 110.91 Hz

Radial Mode 3
Freq: 128.60 Hz

Radial Mode 4
Freq: 150.14 Hz

Radial Mode 5
Freq: 176.87 Hz

Figure 4.19 Radial vibration modes 2 through 5 and their respective frequencies (111, 129, 150, and 177 Hz) predicted for the full-scale stator wheel

4.5 Thermal Design and Analysis of the Concept (Pub 6)

Publication 6 examines the steady-state thermal behaviors of the proposed 8 MW LC DD-PMSG concept using three different analytical methods: finite element, computational fluid dynamics, and lumped parameter thermal network. Predictions are made using each of the thermals models and the results are compared. The influence of passive air cooling of the rotor surface magnets can be seen from the CFD thermal analysis results.

Introduction

For very high Maxwell stress permanent-magnet generator architectures, traditional air-cooling methods are not adequate, because forced-air cooling cannot cope with the higher levels of internal heating that develop at the higher stresses due to inherent generator inefficiencies. Without a more effective cooling method, rotor magnet temperatures can quickly exceed safe operating levels. One very effective thermal management method is
to liquid cool the stator windings. The much cooler windings make active cooling of the rotor’s surface magnets unnecessary.

This publication examines the thermal behaviors of an 8 MW example of the proposed LC DD-PMSG architecture via both analytical and finite element method simulations. Thermal models are developed and analyses are carried out based on FE, CFD, and LPTN methods.

**Generator Cooling System**

The subject LC DD-PMSG is an 8 MW low-speed three-phase synchronous generator with an internal rotor and external stator comprising double-layer, duplex-helical, concentrated tooth-coil windings and laminated electrical steel. The rotor has permanent magnets on the outer diameter surface. Rated speed, torque, and line-to-line voltage are 11.8 rpm, 6.5 MNm, and 787 V, respectively. Based on a 12/10 slot/pole winding configuration, the generator has 144 slots and 60 magnetic pole pairs. It uses 144 tooth coils.

In operation, the main internal heat sources are the copper and electrical steel losses. Copper losses dominate, because of the high electrical winding currents used to develop the required tangential stresses.

Table 4.6 summarizes the key geometries and the applied heat sources for the analyzed LC DD-PMSG configuration.

<table>
<thead>
<tr>
<th>Geometry (mm)</th>
<th>Heat Sources (kW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator OD 7600</td>
<td>Stator Windings 425</td>
</tr>
<tr>
<td>Air Gap Diameter 5900</td>
<td>Stator Steel 14.5</td>
</tr>
<tr>
<td>Air Gap 7</td>
<td>Permanent Magnets 25</td>
</tr>
<tr>
<td>Stator Length 1700</td>
<td>Rotor Steel 1.5</td>
</tr>
<tr>
<td>Magnet Size 133 × 30</td>
<td>Additional 40</td>
</tr>
<tr>
<td>Slot Size 63 × 122</td>
<td></td>
</tr>
</tbody>
</table>

The tooth-coil design features a solid copper conductor with a 15 × 15 mm cross section that surrounds an internal coaxial liquid cooling conduit with a 5.2 mm flow diameter. The 144 individual tooth coils that comprise the generator windings are integrated into the primary liquid cooling loop as 144 parallel cooling circuits of 45 m total length. The
coolant assumed for the analyses is deionized water with an cold temperature of 40°C and a 1 m/s volumetric flowrate. Cooling of the rotor magnets is passive.

**FE Thermal Analysis**

A steady-state finite element thermal analysis was carried out using CEDRAT Flux 3D. Because the LC DD-PMSG generator geometry is cyclically symmetric, only a 2.5° slice (1/144) of the generator was modeled. The FE model slice includes the stator frame, stator yoke, stator teeth, slot wedge, active windings, end windings, insulation, rotor yoke, rotor magnets, and air gap. The mesh used to determine the numerical solution consisted of 57,300 nodes, 122,000 surface elements, and 303,000 volume elements.

Both uniform and non-uniform thermal conductivities were used to model the solid materials. The laminated yoke, for example, was modeled using non-uniform conductivity. The stator slot comprised 24 insulated conductors with internal coaxial stainless steel conduits filled with deionized water. The thermal conductivity of the winding conductors was estimated using Maxwell’s formula for a two-phase solid-to-solid mixture. The air gap was defined as solid air with exaggerated thermal conductivity, a technique based on Ball’s experimental results to account for added heat flow due to convection.

Because the rotor spins, accounting for convection at the rotor and stator ends was more complicated. Empirical formulations proposed by Gerlando and by Kylander were used to define forced convection at the end surfaces. Natural convection to ambient was assumed outside the motor frame.

The stator copper temperature of 80°C and the convection coefficients on the rotor and stator end surfaces were defined as boundary conditions. Figure 4.20 illustrates the FE analysis predicted temperature field for the LC DD-PMSG.

**CFD Thermal Analysis**

A computational fluid dynamics analysis of the LC DD-PMSG design was carried out using COMSOL Multiphysics®. A CFD analysis can determine internal fluid flows. This makes it possible to predict temperature distributions without empirical equations to define the convection heat transfer coefficients. The CFD model of the LC DD-PMSG represented a 20° slice (1/18) comprising the stator yoke, stator windings, slot wedge, air gap, rotor electrical steel, rotor magnets, support structure, and rotor shaft. All generator components were modeled as hollow cylinders. The mesh consisted of 61,899 boundary elements and 203,745 tetrahedral elements.
4.5 Thermal Design and Analysis of the Concept (Pub 6)

Heating was introduced in the stator yoke, stator windings, air gap, permanent magnets, and rotor electrical steel. The laminated electrical steels of the stator and rotor were modeled using non-uniform thermal conductivities. The thermal conductivities of the insulated stator-winding conductors and embedded stainless steel conduits were also non-uniform. Convection at the axial ends of the rotor and stator was handled as in the previous FE analysis. Natural convection to ambient was assumed outside the motor frame.

Figure 4.21 illustrates the CFD predicted temperature field for the subject LC DD-PMSG. The stator copper temperature was set to 80°C, and the rotor speed was set to 11.8 rpm. The COMSOL conduction and convection modes were used to carry out the analysis.

LPTN Analysis

A lumped-parameter thermal network analysis of the LC DD-PMSG design was carried out using a PTC Mathcad® engine. LPTN analysis is based on dividing the system into discrete (lumped) components represented by nodes arranged in a series/parallel network of conduction and convection resistances.
For this analysis, the stator frame, stator yoke, stator teeth, stator windings, air gap, rotor yoke, rotor magnets, shaft, support structure, structural air cavities, and end-cap air volumes were modeled as lumped parameters. The hybrid tooth-coil windings were broken down into active windings, end windings, and coolant.

All nodes were assumed isothermal and thermally symmetric in the radial direction. Heat propagation was assumed to be in the radial and axial directions. Heating was defined with a vector combining all losses. Cooling was defined with a cooling matrix of coolant-to-node thermal resistances.

Figure 4.22 shows the equivalent network model developed for the subject architecture. Table 4.7 lists and describes each of the included LPTN resistances.

Comparison of Analysis Results

Table 4.8 summarizes the maximum predicted temperatures of the major components of the subject LC DD-PMSG system. These results demonstrate the efficiency of the LC DD-PMSG cooling method. Predicted magnet temperatures are below 70°C, and stator windings temperatures are approximately 80°C. With direct liquid cooling of the stator windings, peak torque production is no longer thermally limited.
4.5 Thermal Design and Analysis of the Concept (Pub 6)

Table 4.7 LPTN Thermal resistances

<table>
<thead>
<tr>
<th>Resistance</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R_1$</td>
<td>from rotor yoke to ambient (radial)</td>
</tr>
<tr>
<td>$R_2$</td>
<td>rotor yoke interconnections</td>
</tr>
<tr>
<td>$R_3$, $R_4$</td>
<td>between rotor magnets and rotor yoke (radial)</td>
</tr>
<tr>
<td>$R_5$</td>
<td>rotor magnet interconnections</td>
</tr>
<tr>
<td>$R_6$, $R_7$</td>
<td>between rotor yoke and air gap (radial)</td>
</tr>
<tr>
<td>$R_8$, $R_9$</td>
<td>between rotor magnets and air gap (radial)</td>
</tr>
<tr>
<td>$R_{10}$, $R_{11}$</td>
<td>between tooth and air gap (radial)</td>
</tr>
<tr>
<td>$R_{12}$, $R_{13}$</td>
<td>between coils and air gap (radial)</td>
</tr>
<tr>
<td>$R_{14}$, $R_{15}$</td>
<td>between tooth and coils (radial)</td>
</tr>
<tr>
<td>$R_{16}$</td>
<td>stator tooth interconnections</td>
</tr>
<tr>
<td>$R_{17}$, $R_{18}$</td>
<td>between coils and coolant</td>
</tr>
<tr>
<td>$R_{19}$, $R_{20}$</td>
<td>between coils and stator yoke (radial)</td>
</tr>
<tr>
<td>$R_{21}$, $R_{22}$</td>
<td>between tooth and yoke (radial)</td>
</tr>
<tr>
<td>$R_{23}$</td>
<td>stator yoke interconnections</td>
</tr>
<tr>
<td>$R_{24}$, $R_{25}$</td>
<td>between stator yoke and air in structure (radial)</td>
</tr>
<tr>
<td>$R_{26}$, $R_{27}$</td>
<td>between shaft and air in structure (radial)</td>
</tr>
<tr>
<td>$R_{1a}$, $R_{2a}$</td>
<td>between rotor yoke and end-windings air (axial)</td>
</tr>
<tr>
<td>$R_{3a}$, $R_{4a}$</td>
<td>between tooth and end-windings air (axial)</td>
</tr>
<tr>
<td>$R_{5a}$</td>
<td>between coils and end windings air (axial)</td>
</tr>
<tr>
<td>$R_{7a}$, $R_{8a}$</td>
<td>between end windings air and coolant (axial)</td>
</tr>
<tr>
<td>$R_{9a}$, $R_{10a}$, $R_{11a}$</td>
<td>between end-windings air and end windings (axial)</td>
</tr>
<tr>
<td>$R_{12a}$, $R_{13a}$</td>
<td>between stator yoke and end-windings air (axial)</td>
</tr>
<tr>
<td>$R_{14a}$, $R_{15a}$</td>
<td>between stator yoke air and shaft (axial)</td>
</tr>
<tr>
<td>$R_{16a}$, $R_{17a}$</td>
<td>between rotor yoke air and shaft (axial)</td>
</tr>
</tbody>
</table>

For Cooling Matrix (not shown in figure)

- $R_{\text{flow}}$: fluid flow
- $R_{\text{gap}}$: air flow in air gap
- $R_{\text{str}}$: air flow in structure

Table 4.8 Maximum temperatures predicted via thermal models

<table>
<thead>
<tr>
<th>Component</th>
<th>FE (°C)</th>
<th>CFD (°C)</th>
<th>LPTN (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator Steel</td>
<td>70</td>
<td>65</td>
<td>60</td>
</tr>
<tr>
<td>Stator Teeth</td>
<td>75</td>
<td>-</td>
<td>68</td>
</tr>
<tr>
<td>Stator Windings</td>
<td>82</td>
<td>80</td>
<td>78</td>
</tr>
<tr>
<td>Air Gap</td>
<td>-</td>
<td>-</td>
<td>77</td>
</tr>
<tr>
<td>Rotor Magnets</td>
<td>68</td>
<td>60</td>
<td>66</td>
</tr>
<tr>
<td>Rotor Steel</td>
<td>62</td>
<td>55</td>
<td>63</td>
</tr>
<tr>
<td>Support Structure</td>
<td>57</td>
<td>50</td>
<td>60</td>
</tr>
<tr>
<td>Liquid Coolant</td>
<td>-</td>
<td>40</td>
<td>41</td>
</tr>
</tbody>
</table>
Figure 4.22 Lumped-parameter thermal network model for subject LC DD-PMSG

The LPTN analysis method is excellent for making preliminary determinations of temperature distribution in the LC DD-PMSG architecture. The FE and CFD thermal analysis approaches should be considered to examine critical areas in more detail. Moreover, CFD thermal analysis makes it possible to define cooling system parameters.
Publication 7 continues the examination of thermal behaviors for the proposed LC DD-PMSG design. A more detailed LPTN model is developed that includes details of the stator slot and coolant flow configuration to account for the uneven heating distribution in those areas. LC DD-PMSG temperatures predicted by the model are compared to those seen in existing liquid-cooled generators and against two-dimensional FE analysis results. Next, a transient thermal analytical model is prepared to predict time-dependent temperature distributions. Transient calculations are carried out to predict the changing windings temperatures for overcurrent and loss-of-coolant event scenarios and for a real-world duty cycle. Both of the analytical thermal models are validated with measurements taken from an instrumented prototype comprising two LC DD-PMSG duplex-helical tooth-coil windings embedded in a lamination stack and integrated within a liquid coolant loop.

Introduction

Cooling behaviors of the proposed LC DD-PMSG design are examined using analytical models to predict both steady state and transient temperature distributions. The three approaches typically applied to predict the thermal performance of electrical machinery are 1) measuring the relevant temperatures of prototype machine analogs, 2) applying numerical methods, and 3) making calculations using a simplified mathematical representation.

However, prototyping large electrical machinery is prohibitive in terms of both cost and time. So determining temperatures experimentally is becoming a less attractive option. The CFD numerical approach is useful, and a number of CFD models of electrical machines have been developed. However, CFD analysis is computationally intensive, and the approach becomes less feasible when modeling more complex electrical machinery.

Another useful mathematical approach is LPTN modeling. The LPTN modeling approach can be executed efficiently and cost effectively, and an LPTN model can predict approximate thermal behaviors of even the most complex systems. Publication 6 presented a basic LPTN model of the LC DD-PMSG architecture that used equivalent thermal conductivity to simplify the stator windings representation.

Here, a more exact LPTN model is developed and implemented in MATLAB®. The new LPTN model adds stator slot detail and a better definition of liquid coolant flow geometries. It rapidly predicts steady-state temperatures, enabling the rapid preliminary
assessment of temperature behaviors needed to quickly establish basic geometries early on in an LC DD-PMSG design process. The LPTN model is suitable for integration with the optimization algorithm presented previously in Publication 4.

Another purely mathematical approach can be used to evaluate time-dependent temperature distributions. For example, safe and reliable generator design requires an understanding of how stator temperatures change in response to changes in wind speed and wind turbine power output. Based on the theoretical temperature transient calculation introduced by Kazovskij, a transient thermal analytical model is developed and implemented in MATLAB® to address this need.

Analytical results obtained from both the LPTN and transient thermal models are compared to the thermal behaviors of real-world systems and existing code requirements to better understand how LC DD-PMSG thermal behaviors compare. Both analytical models are validated with measurements taken from an instrumented prototype comprising two LC DD-PMSG duplex-helical tooth-coil windings embedded in a lamination stack and integrated within a liquid coolant loop.

Coolant Temperature Limits

The most generous recommended temperature range for the incoming coolant of a water-cooled electrical machine is 33-50°C. Temperatures below 33°C can induce thermal shock, which can shorten insulation life. Temperatures above 50°C can result in higher stator temperatures, which can also shorten insulation life. According to the international standard for rotating electrical machinery, outgoing water temperature cannot exceed 90°C. It is usually held below 80°C. The maximum acceptable temperature rise of the primary water coolant as it flows through the thermal system is 30°C assuming an average ambient air temperature of 35°C.

LC DD-PMSG Design and Cooling

Stainless steel liquid coolant conduits embedded coaxially within the extruded copper conductors of helical tooth-coil stator windings is the basis for thermal management of the LC DD-PMSG. Copper windings temperatures are maintained via forced liquid cooling. The surface permanent magnets of the rotor are cooled passively via convection with ambient air. The generator modeled here comprises 144 tooth coils and 120 magnetic poles. Table 4.9 summarizes the key characteristics of each coil.
Table 4.9 Tooth Coil Characteristics

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of turns in tooth-coil</td>
<td>10</td>
</tr>
<tr>
<td>Cu Conductor length</td>
<td>1.4 m</td>
</tr>
<tr>
<td>Cu Conductor size (width \times height)</td>
<td>18 \times 15 mm</td>
</tr>
<tr>
<td>Cooling hole diameter</td>
<td>5.5 mm</td>
</tr>
<tr>
<td>Weight per tooth coil</td>
<td>58 kg</td>
</tr>
<tr>
<td>Number of cooling circuit per tooth-coil</td>
<td>1</td>
</tr>
</tbody>
</table>

Lumped-Parameter Thermal Model

In the LC DD-PMSG, stator conductor temperature is a function of coolant temperature. Therefore, coolant temperature must be known to predict temperature distribution within the conductor. Coolant outlet temperature can be calculated based on a pair of straightforward formulations describing heat transfer in single-phase steady-state flow. Combining the coolant temperature rise formulations leads to an expression balancing the heat transfers of coolant and conductor. This expression was used to build the LPTN model.

Some heat flows from the stator teeth, through the insulation materials, through the conductor copper, and into the coolant. Stator tooth heat also flows into the air gap and into the stator yoke. A portion of the heat generated in the permanent magnets flows into the rotor steel, and a portion flows across the air gap and into the stator. Both stator and rotor laminations are bound tightly together and exposed on both the inner and outer diameter surfaces. This unique construction improves convective cooling.

The new LPTN model ignores axial temperature variation. The heat transfer paths between LC DD-PMSG elements were represented using thermal resistances at constant temperature. Figure 4.23 illustrates the resulting equivalent steady-state LPTN model of the proposed LC DD-PMSG design architecture.

In LPTN analysis, typically, the complex heterogeneous structure of the stator windings is represented with a single homogeneous material. To ensure accurate prediction of temperatures for the liquid-cooled windings, this new LPTN model assigns an equivalent convective thermal branch for each conductor. In the model, the inlet coolant temperature for each downstream conductor pass is set equal to the outlet coolant temperature of the upstream conductor pass. Heat transfer at the end windings is not modeled. Instead, coolant conduit length is exaggerated to account for end winding thermal effects.
A steady-state LPTN analysis was carried out to predict the temperature distributions for an example 8 MW LC DD-PMSG design. Heat losses predicted for the various regions were input to the model as heat sources. Because the losses in the electrical steels are negligible for the LC DD-PMSG architecture, steady-state temperature rise in the stator windings depends mainly on copper losses. Table 4.10 lists the electromagnetic and mechanical losses.

Water was selected as the coolant, and conductor dimensions appropriate for the 8 MW LC DD-PMSG example were set. Table 4.11 summarizes the results of the analytical calculation.
4.6 Thermal Behavior of the LC DD-PMSG Concept (Pub 7)

Table 4.10 Electromagnetic and mechanical losses for the 8 MW LC DD-PMSG

<table>
<thead>
<tr>
<th>Power loss component</th>
<th>Value (kW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Copper winding loss, $P_{cu}$</td>
<td>550</td>
</tr>
<tr>
<td>Stator tooth iron loss, $P_{s,t}$</td>
<td>5</td>
</tr>
<tr>
<td>Stator yoke iron loss, $P_{s,y}$</td>
<td>3.7</td>
</tr>
<tr>
<td>Eddy losses magnets, $P_{pm}$</td>
<td>30</td>
</tr>
<tr>
<td>Rotor yoke iron, $P_{r,y}$</td>
<td>1</td>
</tr>
<tr>
<td>Air gap friction loss, $P_{gap}$</td>
<td>1.5</td>
</tr>
</tbody>
</table>

Table 4.11 Steady-state temperatures at rated load for 8 MW LC DD-PMSG

<table>
<thead>
<tr>
<th>Conductor (Cu losses)</th>
<th>Copper (Coolant) Temperature (°C)</th>
<th>Conductor (Cu losses)</th>
<th>Copper (Coolant) Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>01 (150 W)</td>
<td>42.6 (42.0)</td>
<td>11 (250 W)</td>
<td>63.7 (63.1)</td>
</tr>
<tr>
<td>02 (150 W)</td>
<td>44.5 (43.9)</td>
<td>12 (250 W)</td>
<td>66.3 (65.7)</td>
</tr>
<tr>
<td>03 (160 W)</td>
<td>46.3 (45.8)</td>
<td>13 (215 W)</td>
<td>66.3 (65.7)</td>
</tr>
<tr>
<td>04 (160 W)</td>
<td>48.1 (47.6)</td>
<td>14 (182 W)</td>
<td>68.4 (68.0)</td>
</tr>
<tr>
<td>05 (184 W)</td>
<td>50.1 (49.6)</td>
<td>15 (182 W)</td>
<td>70.7 (70.3)</td>
</tr>
<tr>
<td>06 (184 W)</td>
<td>51.9 (51.4)</td>
<td>16 (160 W)</td>
<td>72.6 (72.2)</td>
</tr>
<tr>
<td>07 (222 W)</td>
<td>54.1 (53.5)</td>
<td>17 (160 W)</td>
<td>73.2 (72.9)</td>
</tr>
<tr>
<td>08 (222 W)</td>
<td>56.1 (55.6)</td>
<td>18 (160 W)</td>
<td>74.5 (74.1)</td>
</tr>
<tr>
<td>09 (283 W)</td>
<td>58.7 (58.1)</td>
<td>19 (147 W)</td>
<td>79.5 (79.2)</td>
</tr>
<tr>
<td>10 (283 W)</td>
<td>61.1 (60.5)</td>
<td>20 (147 W)</td>
<td>81.1 (80.8)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Region</th>
<th>Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator Teeth</td>
<td>52.7</td>
</tr>
<tr>
<td>Stator Yoke</td>
<td>51.5</td>
</tr>
<tr>
<td>Magnets</td>
<td>67.7</td>
</tr>
<tr>
<td>Air Gap</td>
<td>67.3</td>
</tr>
<tr>
<td>Rotor Yoke</td>
<td>57.0</td>
</tr>
</tbody>
</table>

According to the table, for the proposed 8 MW LC DD-PMSG design, the coolant average temperature at the tooth-coil conduit outlet is 81°C. With a flow velocity of 1 m/s, the mean temperature rise is approximately 40°C, which is an average 0.9 W/cm² heat removal.

Transient Thermal Analytical Model

Short term generator current overloading may increase the temperature of the stator windings to dangerous levels, so it is important to understand how temperatures respond to changes in power level. Temperature does not change immediately. Its time variation follows an exponential law, where the steepness of the curve decreases with time.
According to existing industry requirements, hydropower generators with liquid-cooled stator windings must continue to run safely through short periods of abnormal operation. For example, they must continue to operate undamaged for set durations in the event of a stator overcurrent scenario. Liquid-cooled generators must also be able to shut down safely following a loss of coolant to the stator windings because of pump failure or a break in the coolant lines.

To better understand the time-dependent temperature behaviors of the proposed LC DD-PMSG, a transient thermal analytical model was prepared and implemented in MATLAB® based on theoretical temperature transient calculations introduced by Kazovskij.

**Transient Thermal Analyses Results**

Analyses were carried out using the transient thermal model to predict the responses of the 8 MW LC DD-PMSG design to both the overcurrent and loss of coolant scenarios. In addition, an analysis was carried out to determine how windings temperature responds to the changing power levels associated with a typical wind turbine duty cycle.

The first analysis set determined the time evolution of stator winding temperature for two overcurrent situations. Current levels were controlled for the calculations via a current density increase coefficient $k_J$. A maximum limit of 90°C was set for stator temperature. The thermal time constant was 236 s. Initial total copper loss was 550 kW, and the coolant velocity was set to 1 m/s. Figure 4.24 shows the predicted conductor temperature distributions with coolant velocity 1 m/s for current density increase factors $k_J = 1.1$ and $k_J = 2$.

In the first case, the generator never reaches the 90°C temperature limit, meaning it can operate continuously with 10% overcurrent. With a 100% increase in windings current ($k_J = 2$), stator conductor temperatures reach the limit within 20 s.

LC DD-PMSG cooling is quite effective. The generator can operate at substantially higher than rated power levels without overheating. The power rating of the architecture is defined by its economic optimum rather than by thermal limits.

The second analysis targeted a loss of coolant scenario. The results show that at 50% power with no coolant flow, both windings and coolant temperatures increase linearly at a rate of approximately 0.05°C/s. At that rate, the generator is capable of operating for 60 s at half power before reaching the 90°C temperature limit.
The third analysis looked at windings temperature in response to the changing power levels associated with actual wind turbine operation. To represent actual operation, an example duty cycle was approximated by applying constant power levels of varying duration over an extended period. As expected, stator windings temperatures exhibited a delayed response to the changes in power level. Figure 4.25 is a graph of the duty cycle representation overlaid with the calculated temperature response of the windings.

Figure 4.25  Predicted windings temperature response during representative duty cycle for an 8 MW LC DD-PMSG
Tooth Coils Prototype and Validation

To validate the simulation results and to demonstrate the liquid-cooling technology with its applications in a stator implementation, a small prototype comprising a pair of helical, double-layer, non-overlapping, hybrid liquid-cooling tooth-coil windings was integrated with an instrumented closed-loop dry cooling system. The instrumented loop provided thermal performance data to clarify the dependence and distribution of temperatures within the coils as well as to make a comparison between theoretical and practical results. This prototype was introduced and described previously in Subsection 4.2, and Figure 4.4 in that subsection is a photograph of the experimental setup. PAO heat transfer fluid was used for the primary loop coolant.

Figure 4.5, also presented previously in Subsection 4.2, showed the thermal behaviors of the system over a period of 5 hours. Coolant flow rate through each coil was constant at 2.0 l/min for the first 4 hours shown on the graph. Inlet pressure was 2.5 Pa. Then, the pump was switched off resulting in a rapid increase in copper temperatures and a drop in coolant temperature. After 45 minutes, the pump was turned back on and temperatures quickly returned to the steady-state normal.

Table 4.12 lists the steady state temperatures recorded during the experiment and those calculated using the analytical model.

<table>
<thead>
<tr>
<th>Measurement Location</th>
<th>Average Measured Temperatures (°C)</th>
<th>Average Predicted Temperatures (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet oil temperature</td>
<td>41.3</td>
<td>40.0</td>
</tr>
<tr>
<td>Outlet oil temperature</td>
<td>43.0</td>
<td>43.0</td>
</tr>
<tr>
<td>Copper temperature (coil 1 &amp; 2 inlet)</td>
<td>40.3 39.3</td>
<td>41.2</td>
</tr>
<tr>
<td>Copper temperature (mid-coil 1 &amp; 2)</td>
<td>42.5 41.9</td>
<td>43.7</td>
</tr>
<tr>
<td>Copper temperature (coil 1 &amp; 2 outlet)</td>
<td>41.9 41.8</td>
<td>43.7</td>
</tr>
</tbody>
</table>

As the table reveals, there are no significant differences between the measured and calculated values. The predictions of the analytical thermal model are suitably precise. Moreover, the test results clearly demonstrate the effectiveness of the LC DD-PMSG cooling approach. When the pump was switched off and coolant flow stopped, copper temperatures began rising rapidly at a rate of 0.015°C/s. The temperature ramp predicted by the transient thermal model was 0.02°C, which agrees well with the measured value.
Concluding Remarks and Recommendations

Projects and Funding

The research and development work reported by this dissertation began in 2010 as a pure research project funded by the Academy of Finland targeting efficiency improvements in wind energy. A multidisciplinary team of post-graduate students, post-doctoral researchers, and professor mentors was assembled to address the Academy project objectives. Research into the state-of-the-art in wind power technologies and wind energy industry planning combined with several targeted analytical investigations led the project team to the LC DD-PMSG idea.

A follow-on project funded by Tekes, the Finnish funding agency for innovation, continued the work into 2013. For the Tekes project, the core team of researchers from the Academy project continued researching the relevant electromagnetic, thermal, and mechanical behaviors and began to develop the LC DD-PMSG architecture. The proposed conceptual designs were realized during this period, and the manufacturability of the architecture’s keystone element; the helical, double-layer, tooth-coil winding, was demonstrated.

Most recently, research and development work continued with financial assistance from the European Regional Development Fund of the European Commission. The researchers that were active in the Tekes project remained active throughout the European Regional Development Fund project, which concluded at the end of 2014. This third project continued exploration of the relevant electromagnetic, thermal, and mechanical behaviors associated with the direct liquid cooling of generator windings. Coolant conduit embedded Litz-wire conductors were evaluated for use in a liquid-cooled electrical machine and an
Achievements and Outcomes

The three aforementioned projects yielded several significant analytical accomplishments including three master’s degree theses, four doctoral dissertations, and thirteen publications. Moreover, there have been a number of concrete outcomes associated with low-speed, high-torque, liquid-cooled, wind turbine generators including several IP claims. Beginning with only a basic understanding of wind turbine drivetrains, enough in-depth understanding of the LC DD-PMSG technologies and existing wind energy challenges and opportunities was developed to effectively address and advise both industry and academia on the relevance of the LC DD-PMSG architecture for future applications.

Major concrete outcomes included:

✓ Intellectual Property (IP) for direct liquid cooling of stator windings via an embedded coaxial coolant conduit, IP for a lightweight wheel structure for low-speed, high-torque electrical machinery, and IP for numerous other details of LC DD-PMSG design

✓ Analytical demonstrations of the equivalent reliability of the LC DD-PMSG; validated electromagnetic, thermal, structural, and dynamic prediction models; and an analytical demonstration of the superior partial load efficiency and annual energy output of an LC DD-PMSG design

✓ A set of LC DD-PMSG design guidelines and an analytical tool to quickly and early establish optimal geometries

✓ Proposed 8 MW LC DD-PMSG concepts for both inner and outer rotor configurations

Future Research

In the future, building a full-scale generator prototype to further validate and refine the developed analytical models would be ideal. Prototype verification and validation would bring fruition to the ideas, help to complete and further validate the analytical and numerical toolsets, and demonstrate the practical realities of the LC DD-PMSG architecture.
Future electromagnetic research and development could target further improvements to direct-liquid-cooling electrical machine efficiencies. In particular, it would be interesting to evaluate the tradeoffs between using solid copper or stranded copper Litz wire in combination with the internal coolant conduits for the tooth coils. Further, more could be learned about the influence of tooth-coil configuration on electromagnetic performance and machine efficiency.

The work carried out thus far has focused on large low-speed wind turbine generators. The liquid-cooling technology is not limited, however, to low-speed applications. It also promises benefits in higher speed electromotive applications. Electric motors used to propel vehicles are notoriously difficult to cool. One industry solution is to immerse the rotor and stator in coolant oil; however, friction between the rotor and oil can degrade efficiency at higher rotor speeds. It would be interesting to explore the application of the liquid-cooling technologies to higher speed drive systems.

Future research and development into the thermal management of liquid-cooled electrical machinery could target development and refinement of a full CFD model. The results of predictions made by the CFD model would assist in further refinement of the LPTN models used for design development.

Future research and development into the structural and dynamic behaviors of large LC DD-PMSG stators and rotors could target further optimization of the structure for improved load-bearing and dynamic response. It would be quite helpful to develop an optimization tool to establish basic wheel structure geometries as a function of electrical machine customer requirements.

The layered sheet-steel element construction technology used for the LC DD-PMSG stator and rotor wheel structures has potential for a wide range of dynamic structure applications. Further research and development of the technology would be interesting and could have a substantial impact in a number of industrial market areas.

Economic realities are fundamental in all electrical machine applications, and manufacturing and operating costs are key. The cost optimization methodology developed as part of this work could be further improved or a more sophisticated optimization methodology could be developed encompassing a wider range of applications that would further improve the economics of hybrid liquid-cooling-based solutions.


Publication 1

Copyright © 2012, Reprinted with permission from the Institution of Engineering and Technology (IET).

This paper is a postprint of a paper submitted to and accepted for publication in IET Renewable Power Generation and is subject to Institution of Engineering and Technology Copyright. The copy of record is available at IET Digital Library.

Direct-drive permanent magnet generators for high-power wind turbines: benefits and limiting factors
R. Scott Semken1 M. Polikarpova2 P. Röyttä2 J. Alexandrova2 J. Pyrhönen2 J. Nerg2 A. Mikkola1 J. Backman2
1Department of Mechanical Engineering, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 58551, Finland
2LUT-Energy, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 58551, Finland
E-mail: janne.nerg@lut.fi

Abstract: Wind turbines are getting larger. Their rated power capacities are moving from the 3 MW range to 6 MW and beyond. As a result, their size and mass, which grow rapidly with power capacity, is becoming a problem in terms of capital cost, logistics and assembly. Moreover, there is a move to offshore installations. Offshore wind turbines demand higher reliability, encouraging wind turbine manufacturers to integrate into their new designs inherently more reliable direct-drive permanent magnet synchronous generators. However, today’s high-power direct-drive generators are massive units that will need to become smaller to minimise costs. Here, the authors review the technological and economic benefits and limitations of direct-drive permanent magnet synchronous generators (DD-PMSGs). The authors examine the benefits and the physical and economic limitations of DD-PMSGs and consider their appropriateness as a key piece in the overall wind turbine system design. The authors look at why these generators are so big and propose a change that can lead to a more compact, more economical wind turbine nacelle.

1 Introduction

The rapid growth of wind power technology and its increasingly important role in energy planning for Europe, the United States and Asia is remarkable. In 2007, the EU endorsed the European Strategic Energy Technology Plan to accelerate the development of renewable energy technologies. Included in the plan are initiatives to increase wind energy’s share of overall EU energy production to 20% by the year 2020 [1]. In 2009, more wind power was installed in the EU than any other electricity generating technology. Of a total 26 GW new capacity, 39% was wind [2]. To reach the year 2020 goal an additional 100–200 GW will be needed [3].

The United States has embarked on a strategy aimed at providing 20% of the nation’s electricity via wind power by the year 2030. Reaching this goal will require installing about 300 GW of new wind generating capacity [4]. In China, starting from nearly zero total capacity in 2004, wind power accounted for nearly 23 GW by 2008. By 2020, their projected capacity will reach about 100 GW [5]. Japan and South Korea also are engaged in aggressive developments of additional wind power capacity. Elsewhere, significant potential markets include Latin America, the former Soviet Union and Africa. These markets are experiencing rapid growth in the demand for electricity, and their demand for wind power could surpass both Europe and the United States by the year 2020 [3].

Today, most wind turbines are land based. In the future, more wind farms will move offshore to utilise stronger winds and higher turbine speeds with less concern about noise production [6, 7]. To manage cost, there will be a strong demand for higher reliability with an absolute minimum requirement for onsite maintenance [8–11]. A typical large electrical power generating wind turbine uses a horizontal axis configuration with the generator mounted in a nacelle that sits at the top of a large tubular tower. Upwind of the generator a three-bladed turbine rotor that may be as large as 140 m in diameter spins at between 5 and 25 rpm. Variable-speed operation with pitch control seems to be the current standard [8]. However, there is still no clear consensus on transmission and electricity generation technologies. In the major wind turbine manufacturers’ catalogues, one will see systems using doubly fed or squirrel cage induction generators and electrically excited or permanent magnet synchronous generators. To couple the slow spinning turbine rotor to the generator, there are high-speed multiple stage gearboxes (1:100), medium-speed single-stage gearboxes (1:10) and direct-driven generators that do without the gearbox altogether.

The newest designs are based on the permanent magnet synchronous generator (PMSG). For example Vestas, GE Wind, Goldwind, Siemens and Gamesa have all introduced large systems intended for offshore use that feature PMSGs. Permanent magnet generators have some advantages including lower weight, improved thermal performance and higher efficiency and energy yield [12–14]. The more problematic choice is the transmission. Most turbines today use high-speed multiple stage gearboxes,
which have proven to be less reliable as they should be, needing replacement well before their design life of 20 years [15, 16]. A serious matter for land-based wind turbines, improved reliability and longevity becomes critical for offshore installations. More and more major manufacturers as well as many of the newer manufacturers of large wind turbines (such as Avantis, Clipper Wind, Leitwind, Vensys, Harakosan, Darwind and Mitsubishi [11]) are introducing offshore units based on direct-drive (DD) PMSGs.

Direct-drive promises lower maintenance cost and complexity, improving reliability and longevity by eliminating the bearings and gears that make up transmission gearboxes [8]. However, direct-drive generators also present some challenges that must be understood and resolved; their increased size and mass are prominent examples.

The rotor of a direct-drive generator spins at the speed of the turbine rotor (5–25 rpm), not at the much faster speed of gearbox generators (~1500 rpm). To obtain the same amount of power with this lower rotational speed, generator torque must increase. Pyrhönen et al. [17] offer a comparison of typical tangential stress values for 3 MW synchronous generators designed for 1500 and 14 rpm; \(\tau_{1500} = 43\) kPa and \(\tau_{14} = 60\) kPa, respectively. According to [18], typical length/diameter ratios \(\chi\) can be calculated by applying the equation \(\chi = (\pi D_p) p^{-1}\), where \(p\) is the number of pole pairs. Applying normal pole pair values (i.e. \(p = 3\) and \(p = 60\)), the typical ratios for the 1500 and 14 rpm cases are \(\chi_{1500} = 0.45\) and \(\chi_{14} = 0.1\). The equation for torque as a function of power and angular speed is \(T = P\Omega\), yielding necessary torque values for these two 3 MW generators of about \(T_{1500} = 19\) kNm and \(T_{14} = 2046\) kNm. Now, using the above values for tangential stress \(\tau_{ave}\), the length/diameter ratios \(\chi\) and these torques; values for generator gap diameter \(D_g\) can be calculated as \(D_{g,1500} = 0.85\) m and \(D_{g,14} = 5.98\) m. Therefore the rotor diameter of a low-speed, direct-drive synchronous generator will be about 7 times that of a conventional high-speed generator.

A review follows of the technological and economic benefits and limitations of DD-PMSGs. We examine the benefits and the physical and economic limitations of these DD-PMSGs and consider their appropriateness as a key piece in overall wind turbine system design. Further, we explain why these generators are so big and propose a change that should result in a more compact, more economical wind turbine nacelle.

2 Benefits of DD-PMSG generators for higher powers

Wind turbines should be constructed of the most reliable components. Downtime resulting from premature service or maintenance needs results in lost revenue and financial penalties. Economics also controls the logistics and installation of wind turbine components. Component size and weight must be kept compatible with readily available transportation and construction techniques. For example, wind turbine rotors are often configured for transport by truck. As wind turbine capacities continue to increase (>3 MW) and rotational speeds simultaneously become lower, gearboxes become more complex and more massive. DD-PMSGs, lacking the gearbox, promise to be more reliable, provide longer life and require lower maintenance. Furthermore, by eliminating gearbox losses, the DD-PMSG also produces an overall higher-energy yield [12].

2.1 Better reliability, longer life and improved performance

Multistage gearboxes are responsible for most of the losses in a high-speed generator. The rule of thumb is that 1% of the power applied at the input shaft is lost for each stage. Many of the larger wind turbines today use three-stage gearboxes, so only about 97% of the input power is transmitted through to the output shaft. According to Polinder et al. [19], the three-stage gearbox of a 3 MW wind turbine accounts for 65% of the total energy lost. Without the gearbox, the direct-drive generator offers improved efficiency. As a result, even although the direct-drive generator itself is not as efficient as high-speed generators, this deficiency is compensated for by the increased overall system efficiency, about 30% less losses overall [12]. Newer, larger power capacity wind turbines will begin using four-stage gearboxes, which will increase gearbox losses further.

Comprehensive data on the longevity of modern wind turbines are not readily available. Large wind turbines have been designed for 20 years of operation, but have not been in operation that long. Good end-of-life ‘wear out’ failure information is not available. Most failures have been a mixture of early ‘infant mortality’ and constant ‘random failures’ that occur in the beginning years of operation [8].

For example, the three-stage gearbox of a 3 MW wind turbine accounts for 65% of the total energy lost. Without the gearbox, the direct-drive generator offers improved efficiency. As a result, even although the direct-drive generator itself is not as efficient as high-speed generators, this deficiency is compensated for by the increased overall system efficiency, about 30% less losses overall [12].

From an investment point of view, efficiency, capital cost and profitability are the drivers. The DD-PMSG offers excellent overall efficiency [12, 13]. Capital cost for the PMSG is high. This increased cost is offset by eliminating the cost of the gearbox. If further design refinement results in significant size and weight reduction, capital cost can be reduced even further, making the DD-PMSG-based wind turbine very competitive.

2.2 Lower capital cost of the nacelle

Table 1 lists nacelle components and gives a percentage value for each to represent its share of total nacelle cost for a typical 6 MW wind turbine with and without gearbox. The table

<table>
<thead>
<tr>
<th>Component</th>
<th>Direct-drive, %</th>
<th>Gearbox, %</th>
</tr>
</thead>
<tbody>
<tr>
<td>nacelle</td>
<td>30–35</td>
<td>28–32</td>
</tr>
<tr>
<td>rotor</td>
<td>8–10</td>
<td>7–9</td>
</tr>
<tr>
<td>blades</td>
<td>5–6</td>
<td>5–6</td>
</tr>
<tr>
<td>pitch mechanisms and bearings</td>
<td>1.5</td>
<td>1.5</td>
</tr>
<tr>
<td>drivetrain</td>
<td>22–25</td>
<td>18–22</td>
</tr>
<tr>
<td>bearings</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>gearbox</td>
<td>–</td>
<td>4–5</td>
</tr>
<tr>
<td>generator</td>
<td>10–12</td>
<td>5–7</td>
</tr>
<tr>
<td>variable-speed electronics</td>
<td>4</td>
<td>4</td>
</tr>
<tr>
<td>yaw drive and bearings</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>main frame</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>electronic connections</td>
<td>2</td>
<td>2</td>
</tr>
</tbody>
</table>
values were calculated based on the model presented by Fingersh et al. [20] The nacelle accounts for about 28–35% of the total wind turbine cost. For this sample case, the gearbox-based wind turbine nacelle shows a slight cost advantage. At current pricing levels, a 6 MW direct-drive generator should cost about 1.1 million Euro. For the same power rating, a high-speed generator plus gearbox would be about 1.0 million.

2.3 Lower weight of the nacelle

For the nacelle, the primary component families are the turbine rotor and the drivetrain. Table 2 compares masses for the main components of a wind turbine nacelle with a 6 MW DD-PMSG to those for a gearbox-based higher-speed generator (calculated based on [20]). The nacelle comprises about 35–36% of the total wind turbine mass for both cases. The turbine rotor hub and blades account for 11%. The generator combined with gearbox results in the greatest mass percentage of the total, 15–16%. The high-power, low-speed DD-PMSG can be relatively less massive depending on power rating, an advantage that grows with power. For the 6 MW power rating case, both the direct-drive permanent magnet synchronous generator and the gearbox plus high-speed generator combination should mass a little over 100 t.

2.4 DD-PMSG wind turbine operation and maintenance

The operation and maintenance (O&M) costs for wind turbines are low when compared to other power generating systems as no fuel is needed, and no emissions are present. One main disadvantage is wind variability. The annual peak hours are normally about 2500–3000 in good sites. Another disadvantage; for its energy yield, the initial capital investment for a wind turbine is relatively large. In the future, the wind variability problem will be solved with improved electric energy storage technologies such as the magnesium–sodium sulphate-antimony battery, a solution that should significantly improve the applicability and acceptance of wind power [21].

Typical maintenance cost of a modern wind turbine is about 1.5 eurocents per kilowatt-hour. For an offshore station, O&M cost is 20% higher. This higher cost is offset by the absence of land rental expense, which can amount to 10–18% of the total O&M costs depending on the site [22].

<table>
<thead>
<tr>
<th>Component</th>
<th>% share of total mass</th>
</tr>
</thead>
<tbody>
<tr>
<td>nacelle</td>
<td>36</td>
</tr>
<tr>
<td>rotor</td>
<td>11</td>
</tr>
<tr>
<td>blades</td>
<td>8.0</td>
</tr>
<tr>
<td>hub</td>
<td>3.3</td>
</tr>
<tr>
<td>drivetrain</td>
<td>24.2</td>
</tr>
<tr>
<td>low-speed shaft</td>
<td>2.5</td>
</tr>
<tr>
<td>gearbox</td>
<td>2.5</td>
</tr>
<tr>
<td>generator</td>
<td>16.5</td>
</tr>
<tr>
<td>main frame</td>
<td>2.9</td>
</tr>
<tr>
<td>yaw drive and bearings</td>
<td>2.2</td>
</tr>
<tr>
<td>nacelle cover</td>
<td>1.1</td>
</tr>
</tbody>
</table>

Many current multistage gearboxes combine a planetary stage and two high-speed stages. For wind turbines designed for greater than 3 MW, a fourth stage is required [9]. The complexity, associated reduction in reliability, and increased maintenance and service needs for these gearboxes makes them increasingly costly and problematic. Furthermore, gearbox drivetrains require lubricants, making it more difficult to obtain necessary environmental permits. All this combines to make the DD-PMSG a more attractive economic choice.

2.5 Return on Investment: lifetime cost

The return on investment for a single wind turbine is about 8–12 years depending on type, location and the prevailing market price of electricity. About 25% of the total cost of a single wind turbine installation is the infrastructure cost. Spreading this cost out over multiple units means that return on investment is significantly shorter for a wind farm of 30–40 units.

The DD-PMSG can offer higher-energy yield [12], which positively affects income; and higher reliability, which reduces O&M costs. According to the Nordpool Spot market, electricity prices are about 50–55€/MWh [23]. This cost increase makes improving energy yield an increasingly important factor for wind turbine owner/operators. Future renunciation of nuclear power in countries such as Germany will inevitably lead to increasing prices for electricity in Europe.

3 Limiting Factors for DD-PMSG Generators for Higher Powers

3.1 Practical Logistics Limitations

Currently, the highest design capacity comes from a wind turbine with a direct-drive electrically excited synchronous generator having an external diameter of about 12 m (Enercon E-126 7.5 MW, 126 m blade diameter). However, because of logistics and construction technology limitations, currently it is not generally practical to market wind turbine generators having an external diameter larger than 8 m. Furthermore, currently available cranes used in wind turbine construction are limited to lifting a maximum of 100 t to a height of just over 100 m. For example a Liebherr 750-tonne crane can lift 100 t up to 120 m. Therefore reasonable design constraints for any new direct-drive generator would include a maximum total weight of less than 100 t and a maximum external diameter of 8 m.

3.2 Electromagnetic Limitations

According to Maxwell’s stress tensor theory, the magnetic field strength H between objects in air creates a stress σH on the object surfaces. Dividing H into its normal and tangential components, Hn and Htan, and introducing the magnetic permeability in a vacuum μ0, the stress σH can be expressed as follows:

\[ \sigma_n = \frac{1}{\mu_0} H^2, \quad \sigma_{\tan} = \frac{1}{\mu_0} (H_n^2 - H_{\tan}^2) \quad \text{and} \quad \sigma_{\tan} = \mu_0 H_{\tan} \]

In terms of torque production, the tangential component σHtan is of interest. The tangential magnetic field strength H_{\tan} must
In air-cooled industrial electrical machines, the tangential stresses typically vary between 10 and 50 T. This corresponds to linear current densities of 83–100 kA/m with an air gap fundamental flux density of 1.2 T. This high fundamental flux density value results from Fourier analysis of the air gap trapezoidal flux density. In practice, the maximum value of the trapezoid is in the range of 1 T.

Although this increase does enable some generator size (and weight) reduction, it is not enough to completely resolve the DD-PMG size problem for wind turbine applications. According to (5), a 4.4 MW direct-drive generator rotating at 14 rpm produces a torque of about 3 MNm. To achieve this torque with 60 kPa tangential stress would require, for example a 1.2 m-long rotor with a 5.2 m outer diameter. A generator of this size falls below the 100 t weight and 8 m diameter maximums. Other ways of increasing torque production capability must be found for generators rated at higher-power capacities.

Two factors limit increasing air gap flux density in a permanent magnet generator. First, the remanent flux densities of present-day magnets can achieve, in practice, average flux densities in the air gap in the range of 1 T. Assuming the trapezoidal flux density waveform, the fundamental flux density can reach about 1.2 T. Second, the saturation flux density of iron – limited to about 2 T – precludes increasing the air gap flux fundamental value much larger than 1.2 T.

Linear current density \( A \) does not have a theoretical upper limit. Its value will increase indefinitely with increasing electrical current levels. However, in practice, there are two factors working against simply increasing current levels to increase torque. One, increasing current level dramatically increases Joule heating losses, so there is a practical thermal limitation. Two, increasing the current, and correspondingly the linear current density, increases the armature reaction-induced reactive voltage drop of the machine. As a result, there is a voltage-based practical limit.

In principle, terminal voltage could also be increased indefinitely to increase linear current density and torque. But in practice, both the voltage supply and the insulation system limit substantial voltage increases. A machine’s reactive voltage drop limits the maximum torque at a given stator terminal phase voltage \( U \). Torque in a permanent magnet generator can be expressed as a function of load angle \( \delta \) as follows.

\[
T = \frac{m}{\Omega_s} \left( \frac{UE_{PM}}{\omega_s L_d} \sin \delta + \frac{U^2 L_q - L_d^2}{2\omega_s L_d L_q} \sin 2\delta \right)
\]

The variable \( m \) is the number of phases, \( \Omega_s \) is the mechanical synchronous angular velocity, \( E_{PM} \) is the permanent magnet-induced electromotive force, \( \omega_s \) is the electrical angular frequency and \( L_d \) and \( L_q \) are the direct and quadrature axes synchronous inductances of the generator.

---

**Table 3** Linear current densities of synchronous machines as a function of the cooling method

<table>
<thead>
<tr>
<th>Method</th>
<th>( A ) (kA/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Air cooling</td>
<td>30 – 80</td>
</tr>
<tr>
<td>( H_2 ) cooling</td>
<td>90 – 110</td>
</tr>
<tr>
<td>Direct water cooling</td>
<td>150 – 200</td>
</tr>
</tbody>
</table>

---

www.iintl.org

be present to produce any tangential stress. Linear current density \( A \) produces the necessary tangential field strength in an electrical machine. The tangential component of the magnetic field strength \( H_{tan} = \mu_0 A \), and the tangential component of air gap flux density \( B_{tan} = \mu_0 A \). According to (1), in terms of the normal component of gap flux density \( B_n \), the tangential stress becomes

\[
\sigma_{tan} = \mu_0 H_{tan} = \mu_0 H_n = B_n A \tag{2}
\]

Equation (2) results in a local time-dependent value for the tangential stress. If the flux density and linear current density vary sinusoidally in the air gap, and there is a phase shift caused by the machine power factor, the average tangential stress as a function of the power factor \( \cos \phi \) and the peak values for normal gap flux density \( B_n \) and linear current density \( A \) will be

\[
\bar{\sigma}_{tan} = 0.5 B_n A \cos \phi \tag{3}
\]

In terms of rotor diameter \( D \) and effective magnetic length \( l' \), the active rotor surface area \( S_t \) can be expressed

\[
S_t = \pi D l' \tag{4}
\]

Now we can write an expression for machine torque \( T \) as a function of the lever arm length \( r \) that can be written also as a function of the power factor.

\[
T = \pi D l' \bar{\sigma}_{tan} r = \pi r^2 \bar{B}_n A \cos \phi \tag{5}
\]

In practice, stator current is supplied to the windings mounted in the slots of a machine. If \( z_s \) is the number of conductors per slot, the slot current \( I_s \) can be replaced with sufficient accuracy by the slot’s local linear current density \( A_s \) over the width of the slot opening \( b \). Everywhere else, the linear current density is zero (i.e. \( A = 0 \)).

\[
A_s = \frac{z_s I_s}{b} \tag{6}
\]

---

Fig. 1 illustrates the behaviour of the linear current density in the air gap of a machine when the number of slots per pole and phase \( q \) is equal to 2.

Equation (6) indicates that we could increase the linear current density by increasing either the slot current or the number of conductors in a slot. Increasing the number of conductors, however, increases the height of the slot, which leads to an excessively large slot leakage inductance.

Another possible approach is to improve cooling. According to Table 3 [18], water cooling more than doubles the maximum allowable linear current density when compared with air cooling.
Introducing \( \phi_{pm} \), the peak value of permanent magnet flux linkage, PM-induced electromotive force can be expressed

\[
E_{pm} = \frac{1}{2} \psi_{pm} \phi_{pm}
\]

(8)

In a non-salient pole machine, \( L_s = L_g \). Therefore the torque becomes

\[
T = \frac{m p}{2 \omega_s} \frac{U \phi_{pm}}{L_s} \sin \delta
\]

(9)

Equation (9) indicates that it should be possible to increase the torque by increasing \( \phi_{pm} \). However in practice, a larger machine results from increasing the magnitude of flux linkage, because for an iron-based magnetic circuit there is a maximum flux density limit. Using \( \tau_p \) for pole pitch and \( l_p' \) for the stator effective length, \( \phi_{pm} \) can be expressed as follows

\[
\phi_{pm} = \frac{2}{\pi} B_s \tau_p l_p' = \frac{B_s D_s}{p} l_p' \]

(10)

Utilising this relation in the torque equation gives the following

\[
T = \frac{m U B_s D_s l_p'}{2 \omega_s L_s} \sin \delta
\]

(11)

Equation (11) infers that maximum machine torque can be increased by minimising synchronous inductance. Synchronous inductance \( L_d \) consists of magnetising inductance \( L_m \) and stator leakage inductance \( L_s \) so that

\[
L_d = L_m + L_s
\]

(12)

Introducing variables for effective air gap length \( \delta_{se} \), stator fundamental winding factor \( k_{w1} \), and the number of turns in series per stator winding \( N_s \), magnetising inductance can be expressed as follows.

\[
L_m = \frac{m D_s}{p \psi_{pm}} \mu_0 l_p' k_{w1} N_s^2 \]

(13)

The only variables from (13) that can be affected to minimise magnetising inductance are the effective air gap and the number of pole pairs so this approach is difficult.

However, examining total stator leakage inductance \( L_s \) may offer another approach. It can be broken up into four logical components. Introducing subscripts \( \delta, u, d \) and \( \omega \) to identify, respectively, the air gap, the slot, the tooth tip and the end windings, the four components of stator leakage inductance are \( L_{\delta u}, L_{\delta d}, L_{u d} \) and \( L_{u \omega} \). The air gap component \( L_{\delta u} \) can be written in terms of magnetising inductance, the stator harmonic \( v \), and the stator harmonic winding factor \( k_{w1} \), leading to an expression in terms of the leakage factor of the air gap inductance \( \alpha_1 \). \[ \text{(18)} \]

\[
L_{\delta u} = \sum_{v=1}^{\infty} \left( \frac{k_{w1}}{k_{w1}} \right)^2 L_{\delta u} = \alpha_1 L_m
\]

(14)

If \( Q_s \) is the number of stator slots, and \( \lambda_s \) and \( \lambda_d \) are the slot and tooth permeance coefficients, the following expressions can be written for stator leakage inductance in the slot and tooth tip (depending on machine geometries).

\[
L_{\delta u} = \frac{4m}{Q_s} \mu_0 l_p' N_s^2 \lambda_s
\]

(15)

\[
L_{u d} = \frac{4m}{Q_s} \mu_0 l_p' N_s^2 \lambda_d
\]

(16)

Stator leakage inductance in the end winding can be expressed in terms of the number of slots per pole phase, vacuum permeability, the end winding length \( l_u \) and the permeance coefficient for the end windings \( \lambda_u \) as follows

\[
L_{u \omega} = \frac{4m}{Q_s} \mu_0 l_u' N_s^2 \lambda_u \]

(17)

Air gap leakage \( L_{\delta u} \) is directly proportional to magnetising inductance; therefore it is inversely proportional to \( p^2 \) according to (13). Leakage inductances for the slot, tooth tip and end windings are proportional to the square of the number of winding turns in series per stator winding \( N_s^2 \). Total stator leakage inductance resolves as follows

\[
L_s = \sigma_1 L_m + \frac{4m}{Q_s} \mu_0 l_u' N_s^2 (\lambda_s + \lambda_d + q l_u \lambda_u)
\]

(18)

A common application of Faraday’s Law of induction leads to the following equation for the number of turns in series per phase winding needed for a specific induced electromotive force \( N_s \).

\[
N_s = \frac{E_{pm} \sqrt{2}}{\alpha_1 k_{w1} \phi_{pm}} = \frac{E_{pm} \sqrt{2}}{\alpha_1 k_{w1} \alpha_0 B_s \psi_{pm}} = \frac{E_{pm} \sqrt{2} \cdot \sqrt{2} l_p'}{\alpha_1 k_{w1} \alpha_0 B_s \psi_{pm}}
\]

(19)

where \( \alpha_i \) is a coefficient showing the arithmetical average of the flux density in the \( i \)-direction.

Recognising from (19) that \( N_s \) is proportional to \( p \), (15) and (16) for slot and tooth tip leakage inductance show them to be proportional to \( p^2 \). The proportionality of the end windings leakage inductance \( L_{u \omega} \) to \( (1/p) N_s^2 \) revealed in (17) shows that it is proportional to \( p \). As a result, despite the moderating effects of \( Q_s \) from (18) and \( D_s \) from (19), the leakage inductance tends to increase in high pole pair machines. According to experimental data, leakage inductance dominates in low-speed high torque wind generators, so \( L_s \approx L_u \). Therefore the torque equation can be expressed as follows.

\[
T = \frac{m U B_s D_s l_p'}{\sqrt{2} \omega_s l_u + L_u} \sin \delta = \frac{m U B_s \psi_{pm} \delta_{se}}{2 \sqrt{2} \omega_s m \mu_0 (k_{w1} N_s)^2} \sin \delta
\]

(20)

Equation (20) is valid for constant voltage and reveals that there is a machine inductance-based limiting factor for torque production. Previously, (5) defined torque independent of voltage showing that larger values of peak linear current density \( J \) result in larger values for torque. Together, the equations reveal that although torque grows with linear current density, the peak torque defined by (20) cannot be exceeded. Removing excess heat with direct liquid cooling (LC) should make it possible to achieve notably higher values of linear current density, increasing average tangential stress \( \sigma_{tan} \) until the machine-inductance-based limit set by (20) is reached.
www.ietdl.org

Copper losses will inevitably be high in situations where direct LC is most needed. Fig. 2 illustrates how the efficiency $\eta$ of a 3 MW machine with 95% rated efficiency behaves as machine power is increased to 6 MW. Joule heating losses increase from 150 to 600 kW, and most of these losses are in the copper stator coils. Total efficiency drops from 0.95 to 0.9.

Given equal cooling performance, rated torque is approximately proportional to rotor volume, which according to (5) is roughly proportional to the generator size. If generator cooling can be sufficiently improved to run at higher linear current densities, and if a slight drop in efficiency (e.g. 1–2% at the maximum power rating point) can be tolerated; a cost-effective, more compact, DD-PMSG (6 MW, <8 m OD, <100 t) could be developed.

3.3 Thermal limitations

Most generators used in wind turbines today are air cooled. As the air coolant passes through the rotor and stator internals, convection is the dominant heat transfer mechanism to remove heat. This convection is supported by continual conduction heat flow through the generator materials.

Further increases in power capacity are best realised by increasing linear current density in the stator coils. Higher linear current density inevitably translates into higher current density and higher losses, which equals increased heat generation. To remove heat in the range of temperatures typical for an electrical generator, convection and conduction are the relevant heat transfer modes. For convective heat flux, the general Newtonian law of convective cooling heat flux density $[\text{W/m}^2]$ $\Phi_{\text{conv}}$ states that

$$\Phi_{\text{conv}} = h(T_f - T_m)$$

where $h$ is the convective heat transfer coefficient $[\text{W/m}^2\text{K}]$, $T_f$ is the temperature of the surface being cooled and $T_m$ is the mean temperature of the fluid. For conduction, heat flux is defined by Fourier’s law.

$$\Phi_{\text{cond}} = -k\nabla T$$

The variable $k$ is the thermal conductivity $[\text{W/Km}]$ of the material and $\nabla T$ is the temperature gradient $[\text{Km}]$.

To maintain constant temperature in a generator system, total cooling power needs to exceed electrical losses. As rated power capacity of the generator increases, the electrical losses increase. Total cooling power $P_{\text{cool}}$ is controlled by (21) and (22), and the cooling surface area $S_{\text{cool}}$ which consists of all the surfaces of the machine that transmit heat flux. $\Phi$ is the total cooling heat flux density.

$$P_{\text{cool}} = \Phi S_{\text{cool}}$$

Since $S_{\text{cool}}$ consists of all the sub-stack channel wetted surfaces, and since the air gap and stator yoke outer surface areas are about 70 m², for example, a 5.2 m-diameter machine with 24 50-mm sub stacks; the heat flux density is in the range of $2 - 3 \text{ kW/m}^2$. Increasing the conduction surface area can only be done by reducing the sub-stack length, which results in a larger and subsequently heavier generator. Therefore to increase convective heat flux, other avenues must be explored.

For example, many grades of neodymium iron boron magnets used in the rotor of a PMSG must be kept below 100 °C, preferably under 80 °C to survive possible fault conditions. The mean coolant temperature is also subject to practical limits. In an air-to-liquid or air-to-air heat exchanger system, the temperature of the secondary coolant will remain above the mean temperature of its primary coolant. Consequently, the only way to increase convective heat flux is to increase the magnitude of the heat transfer coefficient.

The heat transfer coefficient for convection is proportional to the Nusselt number $Nu$.

$$h = Nu \frac{k_{\text{air}}}{D_h}$$

where $k_{\text{air}}$ is the thermal conductivity of air, and $D_h$ is the hydraulic diameter. The Nusselt number for convection problems is generally of the following form

$$Nu = CRe^aPr^b$$

The values for the coefficient $C$ and the exponents $a$ and $b$ depend on the flow surface geometry and the flow regime. $Pr$ is the Prandtl number of the coolant and $Re$ is the Reynolds number, which for flow through a channel or duct can be expressed as follows. The variable $w$ is the magnitude of flow velocity and $v$ is the kinematic viscosity.

$$Re = \frac{wD_h}{v}$$

The bulk of generator losses are in the stator copper windings. These power losses $P_{\text{loss}}$ are closely associated with rated power capacity and the losses increase at a rate proportional to the square of electrical current $I$.

$$P_{\text{loss}} \propto I^2$$

On the other hand, we know that the nominal power of the generator $P_e$ is proportional to current.

$$P_e \propto I$$

Combining (27) and (28) leads to the following relationship

$$P_{\text{loss}} \propto P_e^2$$

---

Fig. 2 Generator electrical efficiency as power doubles from 3 MW (same size and speed, ignoring fan power)
As mentioned previously, total cooling power must match electrical losses to maintain constant temperature. Therefore

\[ P_{\text{fan}} \leq P_{\text{cool}} \]  

(30)

For air as the coolant, the power required for circulation \( P_{\text{fan}} \) is proportional to the cube of its velocity \( w_{\text{air}} \). There are two reasons for this. First, cooling flow increases linearly with air velocity. Second, circulation pressure differential \( \Delta p \) increases with the resulting increased friction, which is proportional to the square of the air velocity. For a given \( A_{\text{coold}} \) cooling flow area

\[ P_{\text{fan}} \propto w_{\text{air}}^3 A_{\text{coold}} \Delta p \propto w_{\text{air}}^2 \]  

(31)

Cooling power is also coupled with velocity through (21), (23)–(25). For forced convection, setting the coefficient \( a = 4/5 \) leads to the following proportionality between cooling power and velocity

\[ P_{\text{cool}} \propto w_{\text{air}}^{4/5} \]  

(32)

Therefore cooling power can be related to fan power.

\[ P_{\text{fan}} \propto P_{\text{cool}}^{1/4} \]  

(33)

Finally, if all other variables remain constant, fan power is proportional to rated power as follows

\[ P_{\text{fan}} \propto P_{\text{15}}^{1/2} \]  

(34)

For example a typical value for fan power is about 50 kW at 4 MW. Applying (34) and setting fan power to 50 kW at 4 MW, fan power as a function of machine power level was plotted in Fig. 3.

Up to 3 MW power, fan losses are insignificant, but at 6 MW the increase in fan power consumption causes total efficiency to drop dramatically (i.e. to 79%).

To summarise, increasing generator rated power capacity necessitates designing stator coils with higher linear current density, which in practice produces more heat. Examining heat transfer first principles and the heat transfer mechanisms of convection and conduction, the parameters that can be adjusted to improve heat removal are the flowrate of the convective coolant and the properties of the coolant. There are practical limitations which prohibit continued increases of flowrate when using air as the coolant, so the most available means of improving heat removal must be to select a coolant type with better heat removal properties; a liquid such as water.

4 LC of stator for DD-PMSG generators for higher powers

In a DD-PMSG, the stator copper accounts for most losses. The number of pole pairs is typically high so the pole pitch remains small, and as a result, so do the stator and rotor yoke dimensions. Despite the large number of pole pairs, the slowly spinning rotor results in a low electrical frequency so iron losses remain low. The stator copper Joule heating losses in a modern 4 MW DD-PMSG can easily be four to seven times the iron losses.

Unfortunately, stator windings are thermally insulated. The winding conductor is copper, which has high thermal conductivity (~400 W/Km). The thermal conductivity of winding insulation ranges from 0.3 to 0.5 W/Km. As a result, the thermal resistance differs considerably in the axial and radial directions for stator windings. Along the copper conductor, heat flows freely. However, out of the conductor and through the insulation, heat flow is substantially less. It follows that heat removal would be best effected by cooling the copper directly.

As generator power capacities continue to grow, a heat-removal system using a liquid coolant flowing inside the stator winding copper conductor seems to offer an excellent heat-removal solution for DD-PMSGs. The best way to accomplish this is to manufacture the conductor with a hollow cross section. The hollow construction will permit the cooling liquid (e.g. deionised, oxygen-free water) to flow directly through the copper conductor making available a substantial convection coefficient. The convection coefficient from copper to water, depending on flowrate, can be as high as 5000 W/m²K. Direct LC is not unprecedented. Methods have been widely utilised in larger modern turbogenerators with power ratings of several thousand megavolt ampere (MVA). An example has been reported by Gray et al. [24].

Preliminary analyses suggest that total rotor losses for a DD-PMSG, designed with direct water cooling of the stator coils, are about 7% of total stator losses and copper losses make up about 92% of the total stator losses. Therefore mixing
5 Conclusions

The primary benefits associated with the DD-PMSG for large-scale wind turbine applications are improved reliability, lower maintenance, longer life and improved overall output. The limitations are primarily excessive size and weight. These reach critical proportions and negatively affect the economic viability of a wind turbine as generator capacities climb above the 3–4 MW level. Developing DD-PMSGs with higher power capacities whereas keeping them below 8 m in diameter and 100 t in weight would be an important advantage for the wind power industry.

One approach to achieving this goal is to run the generator at higher linear current densities. This results in smaller generators with higher-power capacities. The penalty for the higher current densities is a drop in efficiency and resulting higher stator and rotor temperatures. Replacing the currently typical air cooling of the generator rotor and stator with direct LC will keep the active materials sufficiently cool.

Therefore LC DD-PMSGs can be developed with higher-power capacities, whereas remaining below the 8 m and 100 t limits. In fact, it is possible to build a 6 MW LC DD-PMSG that is the same frame size as an air-cooled 3 MW generator. The balance is between the benefits of the DD-PMSG (i.e. improved reliability, lower O&M costs and longer life) and the slight decrease in generator efficiency, which is to a degree offset by an overall efficiency improvement as gearbox losses are eliminated. Because LC DD-PMSGs potentially offer the desired combination of inherently high reliability and longevity coupled with accompanying low O&M costs whereas still providing high overall electrical efficiency, they must be considered as wind turbine nameplate capacities continue to increase and as more offshore wind farms are planned.

6 Acknowledgments

The financial support of LUT and the Academy of Finland are greatly appreciated.

7 References

Reliability Analysis of a Direct-Liquid Cooling System of Direct Drive Permanent Magnet Synchronous Generator

Maria Polikarpova, Lappeenranta University of Technology
Scott Semkon, Lappeenranta University of Technology
Juha Pyrhönen, Ph. D., Lappeenranta University of Technology

Key Words: Reliability Analysis, Cooling System, Direct-Drive Permanent Magnet Synchronous Generator, Wind Generator

SUMMARY & CONCLUSIONS

Wind turbines intended for electricity production are growing in power capacity with each new generation. At the same time, wind farm economics is demanding increased reliability to minimize costs and maximize productivity. These trends are driving a need for more powerful and more reliable energy conversion, and the DD-PMSG is quickly becoming the standard wind turbine generator. Larger DD-PMSGs must be liquid cooled to meet upcoming power capacity demands without exceeding practical limits for size, weight, and capital cost. However, liquid cooling is a new technology for wind turbines and its impact on reliability must be evaluated. This paper presents a reliability analysis for a liquid-cooled 8 MW DD-PMSG coupled with primary and secondary liquid coolant systems. Reliability has been calculated analytically and assessed based on the following reliability metrics: MTBF, MDT, MTTF, failure intensity, and availability.

1 INTRODUCTION

To cope with an unusually rapid growth in wind turbine demand and the constant pressure to reduce cost, wind turbine manufacturers are seeking ways to maximize nominal power and availability. Extensive development of new wind turbine technologies is ongoing to achieve higher levels of reliability, produce better efficiencies, and simplify construction [1,2,3]. Currently available direct-drive permanent magnet synchronous generators (DD-PMSGs) with rated power capacities of more than 6 MW are oversized and too heavy, resulting in substantial capital, logistical, and installation cost penalties. Generator size and weight can be substantially reduced by increasing linear current density in the stator conductors, but the resulting increase in Joule heating calls for a more effective heat removal method, such as direct liquid cooling of the stator windings. In addition to its superior cooling performance, a closed primary cooling loop is not subject to the corrosion that attacks open, air-cooled solutions, so it is ideal for offshore installations.

However, the move to liquid cooling raises a reliability question: is a liquid cooled (LC) DD-PMSG as reliable as an air-cooled DD-PMSG? This is the question addressed in this work, which documents the reliability analysis for an 8 MW LC DD-PMSG. The analysis considered the LC DD-PMSG and its liquid cooling loop and secondary cooling to maintain coolant temperature in the primary loop. Both a liquid-to-liquid and liquid-to-air secondary cooling solutions were analyzed.

Reliability is important in the design of an LC DD-PMSG cooling system. Proper, long-term operation of the machine depends on effective and consistent cooling of its component parts. Inadequate cooling adversely affects reliability, which in turn reduces the electrical machine's availability to generate electrical power, reducing revenue. Understanding cooling system failure modes and which areas are most prone to failure is critical to achieving a robust generator design and optimizing wind turbine availability.

The reliability analysis described here focuses on generator and cooling loop components and does not consider the electrical power and control systems of the wind turbine. The primary and secondary cooling loops comprise several repairable components with published failure and repair intensities, so reliability evaluation includes repair effects (MDT). The analysis was executed to establish the following reliability metrics: Mean Time Between Failures (MTBF), Mean Down Time (MDT), Mean Time to Failure (MTTF), failure intensity, and availability. Various cooling system components have been examined and conclusions concerning their availability and reliability are presented. A MATLAB routine was developed to carry out the reliability calculations. The MATLAB routine made it possible to quickly compare the reliability of various approaches.

2 DESCRIPTION OF GENERATOR COOLING SYSTEM

The electrical power output of a wind turbine generator is equal to the mechanical power input multiplied by its conversion efficiency. Wind drives the rotation of multiple rotor blades to produce the mechanical input. Incoming mechanical power is the product of the torque produced by the rotor blades and their angular velocity. To capture more power from the wind, longer rotor blades are needed, but the longer blades must spin more slowly to keep mechanical stresses within safe limits. So for wind turbines, the dominant contributor to the power equation is the incoming torque.
This torque is balanced by an equal opposing torque produced in the gap between the generator rotor and stator, which is the product of gap tangential stress, the area over which it acts, and the diameter of the gap. In other words, opposing torque is proportional to tangential stress multiplied by active length and the square of the air gap diameter.

The mechanical power input conditions of the wind turbine define the mechanical input torque, and the D-DMSG designers balance this torque by establishing the generator size (diameter and length) and the air gap tangential stress. If size must be limited, as is the case with today’s high power wind turbines, the designers must think about increasing air gap tangential stress to achieve higher power capacities.

Tangential stress can be increased by raising the linear current density in the generator stator windings. The consequence is the rise in loss, which is in turn, in turn, a more effective heat removal. To achieve power capacities in excess of 6 MW in a D-DMSG generator weighing 90 tonnes and 6.5 m in diameter, the traditional air-cooling methods must be supplanted by a more effective liquid cooling approach.

2.1 A Stator/Generator

The studied machine is a low-speed, commutator pole winding, three-phase, synchronous generator with a rated power of 8 MW. Onshore and offshore wind farms are applications for this type of direct-drive generator. The rated speed, torque, and line-to-line voltage are 118 rpm, 6678 MVA, and 787 V, respectively. The rated operating frequency is 121 Hz, so copper losses in the stator winding are significant. The generator comprises 10 poles and 12 slots each on 12 arc segments for a total of 144 stator slots and 60 pole pairs. Table 1 summarises the generator geometry.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator Outer Diameter</td>
<td>7600 mm</td>
</tr>
<tr>
<td>Air Gap Diameter</td>
<td>900 mm</td>
</tr>
<tr>
<td>Length of Air Gap</td>
<td>7 mm</td>
</tr>
<tr>
<td>Length of Stator</td>
<td>11050 mm</td>
</tr>
<tr>
<td>Magnet Width x Height</td>
<td>133 x 20 mm</td>
</tr>
<tr>
<td>Slot Width x Height</td>
<td>65 x 122 mm</td>
</tr>
</tbody>
</table>

2.2 Direct Liquid Cooling Of A Stator Winding

The generator is subject to Joule heating in the copper windings and surrounding steel laminations of the stator segments. There is also some heating of the surface magnets in the rotor. Copper losses, resulting from the induced high linear current density, are about 415 kW. Joule heating of the stator steel laminations, the result of induced eddy currents, is much smaller at about 145 kW. Induced heating of the rotor surface, calculated to be 153 kW, is insignificant. Therefore, a very effective method of managing generator temperature is to cool the copper conductors of the stator windings with a continuous flow of liquid coolant. Keeping the stator cool in this way makes it possible to rely on passive air-cooling to manage rotor temperatures.

To accomplish this direct liquid cooling of the stator windings, an internal cooling duct passes through a longitudinal passage in the copper conductors. For the subject generator, the conductor is rectangular (5.5 x 15 mm) with a 6 mm diameter concentric passage surrounding a stainless steel tube, which has an internal diameter of 5.2 mm. The stainless tubing improves both cooling performance and reliability, because it enables higher liquid flow rates and can be integrated into the cooling loop by orbital welding.5

The generator stator is made up of 12 arc segments, each comprising 12 coils/skimmer conductors. Every coil has a coolant inlet and outlet so that for each stator segment there are 12 inlet and outlet ports connected in parallel to incoming and outgoing coolant manifolds. These connections must be thermally isolated from each other and from the remainder of the coolant system, so an insulated mechanical connection must be used between the stator coils and the coolant manifolds. The 288 mechanical connections, subject to potential corrosion or scaling issues of 144 copper coils in the primary loop, is its major weakness from the standpoint of reliability [14].

The primary liquid cooling loop is sized to remove heat buildup in the stator windings and surrounding steel laminations. The coolant can be demineralised water or a water-based mixture. Pure water offers superior heat transfer performance, but water cannot be used where ambient temperature can drop below the freezing point. For the subject 8 MW generator, the primary coolant flow rate is 1 m/s. The inlet coolant temperature is 40°C, and the resulting outlet temperature is less than 90°C. Based on these coolant conditions, the maximum generator stator winding temperature is 85°C and the maximum rotor magnet temperature is 50°C.

The design maximum flow rate and temperature of the primary coolant are important to coolant system reliability. Keeping coolant temperature below 90°C prevents seizure or pitting corrosion of the stainless steel tubing [15]. Also important, the coolant must be kept pure and deionized. Liquid recirculation systems typically fail due to material cracking, which results when internal stresses combine with material sensitization, a product of improper coolant chemistry [19].

2.3 Air-And Water-Based Cooling Systems

To manage primary coolant temperature, secondary cooling can be based on liquid-to-air (L.A) or liquid-to-air (L.L.A) heat exchangers. In either case, the primary loop is made up of the same auxiliary components: a deionizer, a centrifugal pump, a water reservoir, liquid filters, and an expansion vessel.

For the L.A approach, liquid-to-air heat exchangers connect the primary liquid loop with the secondary side. The auxiliary components on the secondary side include air filters and the blowers that force air through the heat exchangers.

Figure 1 offers a simplified illustration of the L.A exchange approach. Five fan and filter units serve five heat exchangers.
to ensure adequate cooling. Offshore applications call for specialized filtering of the corrosive seawater.

For LL heat exchangers, primary and secondary liquid loops connect through a liquid-liquid heat exchanger. The auxiliary components of the secondary loop include liquid filters and a centrifugal pump. Figure 1 offers a simplified illustration of the LL exchange approach. Because liquid-liquid cooling is more effective, a single heat exchanger unit is needed and the secondary side of the liquid-liquid cooling system is simpler and inherently more reliable. Figure 2 gives a schematic representation of the LL-based system.

3 RELIABILITY ANALYSIS OF COOLING SYSTEM

The LC DDP/MES cooling system is separated into two subsystems: the primary generator cooling system and the secondary system that removes heat from the primary (LL or LA). The reliability analysis divides the generator cooling systems into series and parallel-connected components. Both the LL and LA secondary side components are calculated in the same manner. The subsystems do not have redundant components.

Figure 2 Water-based cooling system of main liquid

Thus, two types of repairable systems are used: a series system with m components and a parallel system (identical components being non-redundant) [7]. The failure and repair probabilities for the components are assumed constant. Equations 1-5 calculate the asymptotic system unavailability, repair intensity, and failure intensity for a series system.

$$U_{k}(\infty) = \sum_{i=1}^{k} \left( \frac{\lambda_{i}}{\mu_{i}} \right)$$

(1)

$$U_{k}(\infty)$$ is the subsystem or system limiting unavailability, \( n \) is the number of component/subsystem for proper system operation, \( n \) is the number of components/subsystems, \( \lambda_{i} \) and \( \mu_{i} \) are the failure intensity and repair intensity of component/subsystem component, respectively.

$$\lambda_{i}(\infty) = \sum_{j=1}^{n} \lambda_{j}$$

(2)

$$\mu_{i}(\infty) = \sum_{j=1}^{n} \mu_{j}$$

(3)

\( \lambda_{i} \) and \( \mu_{i} \) are the failure intensity and repair intensity of component/subsystem, respectively. The total system unavailability, repair intensity, and failure intensity for a parallel m component system can be calculated by Equations 6-9 [7]. The variable \( k \) is the number of failed subsystems.

$$U_{k}(\infty) = \sum_{i=1}^{k} \left( \frac{\lambda_{i}}{\mu_{i}} \right)$$

(4)

$$\mu_{k}(\infty) = \frac{\mu_{1} \mu_{2} \cdots \mu_{k}}{\mu_{1} + \mu_{2} + \cdots + \mu_{k}}$$

(5)

$$\lambda_{k}(\infty) = \frac{\lambda_{1} \lambda_{2} \cdots \lambda_{k}}{\lambda_{1} + \lambda_{2} + \cdots + \lambda_{k}}$$

(6)

\( \lambda_{k} \) and \( \mu_{k} \) are the failure intensity and repair intensity of component/subsystem, respectively.

The total system MTTR, MDT, and MTBF for a parallel m component system can be calculated by Equations 7-9 [7].
MTTR_R = \frac{1}{\lambda_R} \tag{7}

MTT_R = \frac{1}{\lambda_R} \tag{8}

MTBF_R = MTTR_R + MDT_R \tag{9}

A repairable system is characterized by two main measurable reliability properties - reliability and availability. These can be defined by Equations 10 and 11 [7, 8].

\[ R(t) = e^{-\lambda t} \tag{10} \]

\[ A_R = 1 - U_A_R \tag{11} \]

R is the system reliability, \( A_R \) is the system availability and \( t \) is the system operation time.

3.1 Generator Liquid Cooling Loop

In addition to its auxiliary components, the generator cooling loop consists of its plumbing, which in this case is made up of stainless steel tubing, tube connections, and manifolds. The failure of any auxiliary component of the generator loop or any part of its plumbing parts is considered a failure of the cooling system.

Within the generator, the cooling system is divided into 12 identical parallel circuits, one for each of the 12 stator segments. Each of these circuits features a single inlet tube leading into an inlet manifold that connects to 12 conductor inlet tubes, one for each coil. Exiting the coils, 12 outlet tubes connect to the outlet manifold, which then combines the flows into a single outlet tube. Figure 3 illustrates a schematic representation of the stator segment flow. The reliability parameters for the circuit are defined by Eq. 14. Each generator segment comprises a cooling circuit with \( m = 12 \), \( n = 12 \), and \( k = 1 \).

![Figure 3: Schematic representation of a primary coolant flow path for a single stator segment.](image)

The reliability parameters of the 12 parallel circuits of the LC DD4-MSC loop are defined by Eq. 14, with \( m = 12 \), \( n = 12 \), and \( k = 1 \). The failure intensities and MDT values for the plumbing tubing, connectors, and manifolds were taken from the literature and are shown in Table II [9, 13].

The copper coil consists of 24 series-connected 1.15 m stainless steel tubes, so total length of the coil is 28 m. In the previous table MDT of the coil is presented, as in case of stainless steel tube failure - whole coil should be changed. The main reliability parameters of the generator cooling loop are presented in Table III.

\[ \text{TABLE II: Failure Intensity and MDT of tubing, connections, and manifolds of the generator cooling loop.} \]

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failure Intensity per Year</td>
<td>( 4.4 \times 10^{-6} )</td>
</tr>
<tr>
<td>MDT, minutes</td>
<td>390</td>
</tr>
<tr>
<td>MDT, hours</td>
<td>6.5</td>
</tr>
</tbody>
</table>

\[ \text{TABLE III: Reliability parameters of the generator cooling system.} \]

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failure Intensity per Year</td>
<td>( 4.4 \times 10^{-6} )</td>
</tr>
<tr>
<td>MTTF, years</td>
<td>305</td>
</tr>
<tr>
<td>MTTR, minutes</td>
<td>14</td>
</tr>
<tr>
<td>MTTR, hours</td>
<td>305</td>
</tr>
<tr>
<td>Availability</td>
<td>0.9</td>
</tr>
<tr>
<td>Unavailability</td>
<td>0.1</td>
</tr>
</tbody>
</table>

Fig. 4 presents the reliability of the generator cooling loop during 30 years of exploitation. The reliability of the generator cooling loop drops from 0.9586 to 0.953.

The more sophisticated models require many variables and associated parameters to represent the principal damage mechanisms in the life equations. The variables include elastic, inelastic, and total strain ranges, dislocation strain energy, temperature, frequency, hold time, strain rate, and mean stress [9].

![Figure 4: Reliability of the generator cooling loop over a 30 years life.](image)
3.2 Primary And Secondary Air- And Water-Based Cooling Systems

The reliability of the LI and LA primary and secondary side depends on auxiliary components reliability. The auxiliary components in either case are connected in series. Only multiple components of the same type are connected in parallel (e.g., water and air filters, fans, and air-water heat exchangers). The treatment equipment (filters and deionizer) for the primary and secondary side cooling fluids, air, or water was included also for this reliability analysis.

The failure intensities and MTD for the auxiliary equipment of LI and LA primary and secondary sides have been taken from the literature and are summarized in Table IV [10, 12, 13, 15, 18, 31].

**TABLE III: RELIABILITY PARAMETERS OF THE AUXILIARY COMPONENTS IN PRIMARY AND SECONDARY COOLING LOOPS**

<table>
<thead>
<tr>
<th>Auxiliary Component</th>
<th>Failure Intensity per year</th>
<th>Mean Down Time per hour</th>
</tr>
</thead>
<tbody>
<tr>
<td>Primary Side</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Steam Generator</td>
<td>0.04 g/kwh</td>
<td>8.0</td>
</tr>
<tr>
<td>Water Servo Valve</td>
<td>0.01 g/kwh</td>
<td>10</td>
</tr>
<tr>
<td>Water Heater</td>
<td>0.05 g/kwh</td>
<td>24</td>
</tr>
<tr>
<td>Water Pump</td>
<td>0.04 g/kwh</td>
<td>24</td>
</tr>
<tr>
<td>Secondary Side</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Water Generator</td>
<td>0.02 g/kwh</td>
<td>4.0</td>
</tr>
<tr>
<td>Water Pump</td>
<td>0.01 g/kwh</td>
<td>4.0</td>
</tr>
</tbody>
</table>

These component reliability values are used to calculate the metrics of the primary and secondary side cooling systems, and the reliability of the LI DDPMMSG cooling system as a whole. According to Table IV, the failure intensity for the deionizer, water filter, air filter, centrifugal pumps, and air filter must be replaced every 10 years for the centrifugal pumps and air filters. The reliability of the primary loop based on LA heat exchange drops significantly during the operating period (Figure 3).

The LA system exhibits a relatively poor performance because of the five identical liquid-air heat exchangers, which increase overall failure probability. The LI system includes only one liquid-air heat exchanger. The secondary cooling side includes auxiliary components for the cooling main fluid, such as the main pump, water filter, and air filter. The LI and LA cooling loops LI DDPMMSG and the cooling systems based on LI and LA systems including all their components.

The systems are characterized by their availability or capability of being used during a given time interval. When estimating the reliability metrics presented in the above Tables and Figures, it becomes evident the cooling system based on LI heat exchange is significantly better. However, LI DDPMMSG with a cooling system based on secondary side LI and LA heat exchange should be serviced at 6-month intervals, and servicing should take 4-6 hours.

**Figure 3: Reliability of LI and LA primary cooling loops during 30 years of operation.**

**TABLE IV: RELIABILITY PARAMETERS OF THE GENERATOR COOLING SYSTEM**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>LI DDPMMSG</th>
<th>LI DDPMMSG</th>
<th>LI DDPMMSG</th>
<th>LI DDPMMSG</th>
</tr>
</thead>
<tbody>
<tr>
<td>Primary Cooling Loop</td>
<td>1.9</td>
<td>1.7</td>
<td>2.4</td>
<td>2.4</td>
</tr>
<tr>
<td>Mean Down Time (h)</td>
<td>0.84</td>
<td>0.55</td>
<td>0.43</td>
<td>0.43</td>
</tr>
<tr>
<td>Failure Intensity (g/kwh)</td>
<td>0.54</td>
<td>0.54</td>
<td>0.43</td>
<td>0.43</td>
</tr>
</tbody>
</table>

7. CONCLUSIONS

To carry out a reliability analysis for a liquid cooled direct-drive permanent magnet synchronous generator, the LC DDPMMSG, the cooling system was broken out into two subsystems: the generator with its primary liquid cooling loop and the secondary side cooling system. Both the liquid-air-liquid and liquid-air secondary side cooling solutions were analyzed. Reliability metrics were calculated and assessed in terms of constant failure and repair rates. The analysis concluded the cooling system for the LC DDPMMSG has an average reliability of 0.96 over a 30-year design lifetime. Four to five hours of servicing is required.
every 5 months to change out consumables and shorter life components such as filters, deionizers, fans and pumps. An LC DDE/MSG cooling system based on liquid-air heat exchange between the primary and secondary sides is less reliable than an equivalent system based on liquid-liquid heat exchangers, because the LA system includes multiple heat exchangers, each with shorter life components.

Cycling system reliability can be improved by designing in redundancy. However, the economic feasibility of this approach must be studied.

REFERENCES
5. Ivanov, R., Steglitch, K., G. Perros and M. Varjus, “Large 60 Hz Turbinegenerator: Mechanical Design and Improvements”, Electric Machines and Drives Conference (EMDC), Miami, USA, 2009
6. Technical Letter, “5000 Copper-Rod vs. Type 316 Stainless Steel: A Functional Comparison of Two Condenser Tube Alloys”, Hitachi America, USA
15. Hurst, N.I.W. “Failure Rate for Pipework”, 1991

BIOGRAPHIES
Maria V. Polkampova
Lappeenranta University of Technology, Lappeenranta, Finland
email: Maria.Polkampova@lut.fi

Maria V. Polkampova was born in 1985 in Sevezechensk, Russia. She received the M.S. degree in electrical engineering from St. Petersburg Technological University of Paint, Polyurethanes, Russia in 2008 and the Master of Science (M.Sc.) degree from Lappeenranta University of Technology (LUT), Finland, in 2009. She is currently the PhD student in the Department of Electrical Engineering at LUT, where she studies heat transfer processes and cooling systems of electric motors, electric drives and power electronics.

Scott Semien
Lappeenranta University of Technology, Lappeenranta, Finland
email: Scott.Semien@lut.fi

Scott Semien is from the Rocky Mountain region of the United States. A degreed mechanical engineer, Scott’s early career focused on performing structural, dynamic, and thermal analyses for the energy industry. Pursuing his passion for machine systems, the focus changed to the design and development of complex electromechanical machinery for a variety of American capital equipment manufacturers. Most recently, Mr. Semien has begun working on his doctoral degree in mechanical engineering at LUT, participating in the development of large direct-drive permanent magnet wind turbine generators.

John Pyrkonen, Ph.D., Professor
Lappeenranta University of Technology, Lappeenranta, Finland
email: John.Pyrkonen@lut.fi

John Pyrkonen (IEEE member) born in 1987 in Kuusankoski, Finland, received the Doctor of Science (D.Sc.) degree from Lappeenranta University of Technology (LUT), Finland, in 1991. He became Associate Professor of Electrical Engineering at LUT in 1993 and Professor of Electrical Machines and Drives in 1997. He is currently the head of the Department of Electrical Engineering at the Institute of LUT Energy, where he is engaged in research and development of electric motors and electric drives. His current interests include different synchronous machines and drives, induction motors and drives and solid-state high-speed induction machines and drives.
Publication 3

Copyright © 2013, Reprinted with permission from Praise Worthy Prize.

Permanent Magnet Synchronous Generator Design Solution for Large Direct-Drive Wind Turbines

Y. Alexandrova¹, R. S. Semken², J. Pyrhönen³

Abstract – Turbine power generation in the range of 8-12 MW and beyond will be needed to accelerate the global growth of wind energy production and meet renewable energy targets. Described here is a theoretical framework for a compact, high-power, direct-drive permanent magnet synchronous generator (DD-PMSG) that uses direct liquid cooling (LC) of the stator windings to efficiently manage generator cooling, and therefore the temperature of the windings and permanent magnets. Direct liquid cooling enables a significantly lighter weight generator, and this lower mass design promises reliable and efficient power generation at substantially lower cost. It is clear that much improved cooling will become a necessity for DD-PMSGs targeting wind turbine applications as power ratings continue to grow. Discussed first here is the analysis of air gap tangential stress and its effect on generator torque density and efficiency. Major LC DD-PMSG design issues are presented along with a suggested integrated design solution in the form of an 8 MW example. Next, a fabricated, instrumented, and tested cooling loop is described. This prototype features a pair of typical LC stator coils embedded in a laminated stack attached to a forced recirculation liquid cooling loop. Finally, operating characteristics are evaluated for a large wind turbine equipped with the proposed 8 MW LC DD-PMSG revealing annual energy output and load factor calculated for typical North Sea wind conditions. Ultimately, the higher partial load efficiency inherent in the LC DD-PMSG design results in more efficient total electricity production. Copyright © 2013 Praise Worthy Prize S.r.l. - All rights reserved.

Keywords: Directly Cooled Winding, Permanent Magnet, Synchronous Generator, Wind Turbine

Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>Linear current density</td>
</tr>
<tr>
<td>a</td>
<td>Number of parallel branches</td>
</tr>
<tr>
<td>B</td>
<td>Flux density</td>
</tr>
<tr>
<td>C_p</td>
<td>Power coefficient</td>
</tr>
<tr>
<td>c</td>
<td>Scale parameter</td>
</tr>
<tr>
<td>E</td>
<td>Back EMF</td>
</tr>
<tr>
<td>F</td>
<td>Force</td>
</tr>
<tr>
<td>f</td>
<td>Frequency, probability density function</td>
</tr>
<tr>
<td>J</td>
<td>Surface current density</td>
</tr>
<tr>
<td>H</td>
<td>Magnetic field strength</td>
</tr>
<tr>
<td>I</td>
<td>Current</td>
</tr>
<tr>
<td>K</td>
<td>Constant</td>
</tr>
<tr>
<td>k</td>
<td>Coefficient, Weibull shape parameter</td>
</tr>
<tr>
<td>l</td>
<td>Length</td>
</tr>
<tr>
<td>m</td>
<td>Mass, number of phases</td>
</tr>
<tr>
<td>N</td>
<td>Number of turns in series</td>
</tr>
<tr>
<td>P</td>
<td>Power</td>
</tr>
<tr>
<td>p</td>
<td>Number of pole pairs</td>
</tr>
<tr>
<td>R</td>
<td>Resistance</td>
</tr>
<tr>
<td>r</td>
<td>Radius</td>
</tr>
<tr>
<td>S</td>
<td>Area</td>
</tr>
<tr>
<td>T</td>
<td>Torque</td>
</tr>
<tr>
<td>U</td>
<td>Voltage</td>
</tr>
<tr>
<td>X</td>
<td>Reactance</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>z_0</td>
<td>Number of conductors in slot</td>
</tr>
</tbody>
</table>

Subscripts

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>a_v</td>
<td>Average</td>
</tr>
<tr>
<td>Cu</td>
<td>Copper</td>
</tr>
<tr>
<td>d</td>
<td>Direct winding</td>
</tr>
<tr>
<td>ew</td>
<td>End winding</td>
</tr>
<tr>
<td>Fe</td>
<td>Iron</td>
</tr>
<tr>
<td>q</td>
<td>Quadrature</td>
</tr>
<tr>
<td>max</td>
<td>Maximum</td>
</tr>
<tr>
<td>n</td>
<td>Rated</td>
</tr>
<tr>
<td>ph</td>
<td>Phase</td>
</tr>
<tr>
<td>r</td>
<td>Rotor</td>
</tr>
<tr>
<td>s</td>
<td>Stator</td>
</tr>
<tr>
<td>str</td>
<td>Structural</td>
</tr>
<tr>
<td>tot</td>
<td>Total</td>
</tr>
<tr>
<td>wh</td>
<td>Working harmonic</td>
</tr>
</tbody>
</table>

Greek letters

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>α</td>
<td>Angle component in cylindrical coordinate system</td>
</tr>
<tr>
<td>β</td>
<td>Blade angle</td>
</tr>
<tr>
<td>δ</td>
<td>Load angle</td>
</tr>
<tr>
<td>η</td>
<td>Efficiency</td>
</tr>
<tr>
<td>λ_T</td>
<td>Torque density</td>
</tr>
<tr>
<td>λ</td>
<td>Tip speed ratio</td>
</tr>
<tr>
<td>ν</td>
<td>Linear speed</td>
</tr>
<tr>
<td>ρ</td>
<td>Resistivity, density</td>
</tr>
</tbody>
</table>
I. Introduction

For Europe, wind power is a crucial source of renewable energy production. In 2010, the European Union installed approximately 9.295 GW of new wind power for a total capacity of 64 GW. With an average load factor of 24.6%, this capacity produced about 180 TWh of electricity accounting for 5.3% of all EU electricity consumption [1]. In 2011, another 9.62 GW was installed, and the percentage of EU electricity consumption coming from wind energy rose to 10.5% [2]. In 2012, the EU wind energy sector installed 11.6 GW of wind turbine capacity, bringing the total to 105.6 GW [3]. The European Wind Energy Association forecasts that installed wind power capacity within the EU will grow to 230 GW by 2020. This capacity will produce some 600 TWh of electricity making up 14-18% of the EU’s total expected demand [4].

Currently, most land-based wind turbines have power ratings from 1 to 5 MW, and wind turbines designed for offshore use are typically 3 to 5 MW. Sustaining the high growth rate of wind energy will call for the development of wind turbines with even higher power ratings, and as Table 1 illustrates, there are currently several development projects underway targeting power ratings from 6 to 15 MW. Generator technologies represented in Table 1 comprise a mix of direct drive (DD), conventional fast-speed geared (IGS), and medium-speed geared concepts (MS). Table 1 includes electrically excited machines (EESG), induction machines (DFIG), and permanent-magnet synchronous machines (PMSG), as well as experimental technologies that introduce high temperature superconductors (HTS). Wind turbines using DD-PMSGs are most common, because of the configuration’s excellent performance and inherent reliability.

However, when developing powerful wind turbines, increased size and weight are significant shortcomings for traditionally designed direct-drive generators.

In a rotating machine, power is the product of torque and angular velocity [13]. Large direct-drive wind turbine generators use a large diameter rotor to convert wind energy to power. Furthermore, because of physical and environmental limitations, a maximum rotational speed limit is imposed on the spinning main rotor blades of the wind turbine.

Angular velocity is essentially fixed, so wind turbine power is proportional to the torque input of the rotor blades. For example, the Enercon E-126 (Table I) rotor blades spin at a maximum of 10.5 rpm (1.11 rad/s). At 10.5 rpm, the direct-drive generator must produce 6.91 MNm of opposing magnetic torque to produce 7.6 MW. This opposing torque can be expressed as a function of the tangential stress $σ_{tang}$ acting on the surface of the rotor, the rotor radius $r_r$, and the surface area $A_r$ as follows [13]:

$$ T = σ_{tang} r_r A_r $$

The 7.6 MW Enercon E-126 is the largest direct-drive wind turbine currently available. Its electrically excited synchronous generator (EESG) weighs 220 t [14]. Published photos documenting the assembly of an E-126 reveal the outer diameter of its generator to be about 12 m in diameter and 1 m long resulting in 50 kPa tangential stress, which may be regarded as a practical upper limit for air-cooled low-speed synchronous machines having significant losses on both stator and rotor.

Transporting and assembling components this large and massive is problematic and expensive. Specialized and expensive lifting equipment is needed. Moreover, an excessively large and massive generator increases costs across the board, a problem that will only worsen as power ratings increase.

A technological breakthrough that would enable the production of reliable direct-drive wind turbine generators of substantially smaller size and weight would be a welcome development contributing to better wind energy economics.

According to Eq. (1), the only way to increase torque that does not involve increasing generator size is to increase the tangential stress acting on the generator rotor. This tangential stress is proportional to the magnetic field strength generated by the permanent magnets of the rotor and the linear current density around the periphery of the stator. Greater linear current density results in increased torque; however, it also results in more Joule heating of the copper conductor. At present, wind turbine generators are air cooled, and maximum linear current density is limited by the effectiveness of the forced air heat removal.

Liquid cooling offers heat removal that is many times more effective and may offer a breakthrough solution to reduce direct-drive generator size and weight [15].

### Table 1: Wind Turbine Projects

<table>
<thead>
<tr>
<th>Manufacturer</th>
<th>Power, MW</th>
<th>Type</th>
<th>Gear</th>
<th>Rotor</th>
<th>Availability</th>
</tr>
</thead>
<tbody>
<tr>
<td>Siemens [5]</td>
<td>6.0</td>
<td>PMSG</td>
<td>DD</td>
<td>154 m</td>
<td>2014</td>
</tr>
<tr>
<td>Alstom [6]</td>
<td>6.0</td>
<td>PMSG</td>
<td>DD</td>
<td>150 m</td>
<td>2015</td>
</tr>
<tr>
<td>Repower [7]</td>
<td>6.6</td>
<td>DFIG</td>
<td>DD</td>
<td>154 m</td>
<td>2014</td>
</tr>
<tr>
<td>Enercon [8]</td>
<td>7.6</td>
<td>BFG</td>
<td>DD</td>
<td>190 m</td>
<td>in production</td>
</tr>
<tr>
<td>Vensys [9]</td>
<td>8.0</td>
<td>PMSG</td>
<td>N/A</td>
<td>164 m</td>
<td>2014</td>
</tr>
<tr>
<td>AMSC [10]</td>
<td>9.0</td>
<td>HTSC</td>
<td>DD</td>
<td>190 m</td>
<td>2015</td>
</tr>
<tr>
<td>Alstom [12]</td>
<td>15.0</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
<td>2020</td>
</tr>
</tbody>
</table>
This work describes the theoretical framework of a novel and compact, high-power, direct-drive permanent magnet synchronous generator (DD-PMSG) that uses direct liquid cooling (LC) to control the temperature of the stator windings and, indirectly, the temperature of the rotor. The low mass LC-DD-PMSG design promises reliable and efficient power generation.

Wind turbine economics is a trade-off between the expenses (initial capital costs and long-term operating expenses) and electricity production income over the life of the wind turbine.

A wind turbine generator should be inexpensive to purchase, operate at minimal cost for its design life, and maximize electricity production. Generator cost is heavily dependent on the mass of materials needed, particularly the magnetic materials, permanent magnets, and copper. To minimize operating cost, the generator must be simple and robust in design and construction and must function reliably. Electricity production is maximized when uptime and average efficiency of energy conversion are maximized.

Electrical machine efficiency is determined by several independent factors (type, size, operating speed, loading, materials, and operating regime) so there is no single efficiency for any particular generator [16]. For a low speed, low frequency DD-PMSG, stator Joule losses have the biggest impact on efficiency.

Large turbogenerators (100-1600 MW) are typically wound rotor synchronous machines, and their large size and speed ranges contribute to very high efficiencies reaching even 99%. DD-PMSGs for wind turbines are smaller and lighter. Efficiencies for traditional air-cooled wind turbine DD-PMSGs top out at about 94 - 95%.

The proposed LC DD-PMSG represents another significant drop in size, and when optimized for minimal mass, would have a peak load efficiency of around 93%. Although this peak power efficiency is a bit lower than that of traditional DD-PMSGs, the LC DD-PMSG offers excellent partial load efficiencies. Considering its lower capital, logistics, and installation costs, in a typical wind energy application, the LC DD-PMSG can still ultimately result in more electricity production at lower cost.

II. Fundamental Consideration on Tangential Stress in an LC DD-PMSG

Even though the more effective heat transfer offered by liquid cooling makes it possible to increase torque density and reduce the size and weight of direct-drive generators, increased power dissipation due to Joule heating results in full load efficiencies that are smaller than traditionally accepted.

Furthermore, raising torque density also increases the load angle of the synchronous generator, which results in lower active power reserve and could, therefore, be a source of instability.

However, this disadvantage can be overcome by sophisticated control of the generator.

### II.1. Tangential Stress

The following paragraphs briefly discuss tangential stress, \( \sigma_{\text{tan}} \), which has a direct impact on generator efficiency and torque density. The tangential force \( F_{\text{tan}} \) applied to a unit of rotor surface area for an electrical machine gives the average tangential stress in an air gap:

\[
\sigma_{\text{tan}} = \frac{F_{\text{tan}}}{2\pi r_f} = \frac{r_f}{2\pi r_f^2} \frac{\int_0^{2\pi} B_h H_{\text{tan}} \, d\alpha}{2\pi r_f^2} = \frac{1}{2\pi} \int_0^{2\pi} B_h H_{\text{tan}} \, d\alpha
\]

where \( B_h \) is the normal flux density, \( H_{\text{tan}} \) is the tangential field strength, and \( r_f \) is the equivalent length of the machine, \( \alpha \) is the coordinate in circumferential direction in cylinder coordinates. With the simplification \( B_h = B(a) \) and \( H_{\text{tan}} = A(a) \), and supposing that both the radial flux density \( B(a) \) and the stator surface current density \( A(a) \) are sinusoidal and exactly in phase, then the average value of the tangential stress in an air gap is:

\[
\sigma_{\text{tan}} = \frac{B_{\text{tech peak}} A}{\sqrt{2}}
\]

where \( A \) is the RMS value of linear current density, and \( B_{\text{tech peak}} \) is the peak value for air gap flux density of the working harmonic.

### II.2. Tangential Stress as a Function of Torque Density

The torque of the machine then results by multiplying the tangential force \( F_{\text{tan}} \) on the rotor by the rotor radius:

\[
T = F_{\text{tan}} r_i = 2\pi r_i^2 \sigma_{\text{tan}} \times r_i D_r^3
\]

According to Eq. (4), the output torque of a machine is proportional to the average tangential stress, the third power of the rotor surface diameter in the air gap, and the ratio of rotor equivalent length to the rotor diameter \( r_i \).

The total mass \( m_{\text{rot}} \) of the generator could be roughly given as follows:

\[
m_{\text{rot}} \propto 2k_{\text{vol}} \left( k_{\text{st}} D_r \right)^3 \rho_{\text{av}}
\]

where \( \rho_{\text{av}} \) is the average density of the rotor and stator active materials, and \( k_{\text{vol}} \) is the structural coefficient equal to the total mass of the generator structure divided by the mass of active materials. The torque density \( \lambda_T \) of the machine is then:

\[
\lambda_T = \frac{T}{m_{\text{rot}}} \propto \frac{\sigma_{\text{tan}}}{k_{\text{vol}} k_{\text{st}} \rho_{\text{av}}}
\]

Coefficient \( k_{\text{vol}} \) is mostly determined by the number of machine pole pairs \( p \). Therefore, Eq. (6) shows that machine torque density is proportional to the tangential...
stress and inversely proportional to the average mass density of the stator and rotor active materials and the \( k_{mz} \), \( k_0 \) coefficients, while it is fully independent of the rotor dimensions. Since low-speed generators usually have much higher pole-pair numbers than high-speed machines, the advantage of low-speed, high-tangential-stress PMSGs in terms of torque density is reasonably clear.

II.3. Impact of Copper Weight on the Efficiency of the LC DQ-PI MSG

The effect of copper mass on generator efficiency is now examined. The major source of loss in a low-speed high-torque machine is copper loss in the stator winding. Therefore, ignoring iron and other minor losses, the per phase power balance equation of a non-salient pole PMSG with \( J_e = 0 \) control in the d-q coordinate can be written as follows:

\[
I_q E_{PM} = I_q U_i \cos \delta + k_R R_q I_q^2
\]  

(7)

where \( E_{PM} \) is the induced electromotive force in the stator winding, \( I_q \) is the q-component of the stator current, \( k_R \) is the resistance increase factor caused by the alternating current, \( R_q \) is the stator phase resistance, \( U_i \) is the stator phase voltage, and \( \delta \) is the load angle. The first term on the right represents the active power of the machine and the second term, the losses. The copper loss contribution in Eq. (7) can be expressed approximately in terms of efficiency \( \eta \) and the electromagnetic power of the generator as follows:

\[
k_R R_q I_q^2 = (1-\eta) I_q E_{PM}
\]  

(8)

In turn, the active power, the first term on the right side of Eq. (7), can also be given in terms of efficiency \( \eta \) and the electromagnetic power of the generator:

\[
I_u U_i \cos \delta = \eta I_q E_{PM}
\]  

(9)

The ratio of copper loss, Eq. (8), to active power, Eq. (9), can be expressed according to Eq. (10):

\[
\frac{k_R R_q I_q^2}{U_i U_i \cos \delta} = \frac{k_R R_q I_q}{E_{PM}/k_R \cos \delta} = k_R \left( \frac{2 \rho_{Cu} k_{mz} N_{ph}}{S_{Cu} a} \right) S_{PM} \left( \frac{1}{\sqrt{2}} \right) = \frac{1}{\sqrt{2}} \rho_{Cu} k_{mz} N_{ph} \eta \cos \delta = k_R k_m k_{mz} k_{mz} k_{mz} \left( \frac{1}{\sqrt{2}} \right)
\]  

(10)

where \( a \) is the number of parallel paths, \( J_e \) is the stator current density, and \( \rho_{Cu} \) is the EMF factor (ratio of no-load induced voltage to the phase voltage).

The symbol \( k_m \) is the winding factor of the working harmonic, \( k_m \) is the end winding length factor. The number of turns in series per phase is \( N_{ph} \), and \( k_m \) is the copper area. Finally, \( \rho_{Cu} \) is the copper electrical resistivity, and \( \eta \) is the linear rotor surface velocity.

When multiplying Eq. (10) by the linear current density \( A \), the resulting expression can be written in the form:

\[
A J_e = \frac{k_m k_{mz} \cos \delta}{\rho_{Cu} k_m k_m k_m} \left( \frac{1-\eta}{\eta} \right) = K \left( \frac{1-\eta}{\eta} \right)
\]  

(11)

The numerator of the first fraction of Eq. (11) represents the average tangential stress from Eq. (4).

Therefore, \( A J_e \) in Eq. (11) can be written in terms of the tangential stress as follows:

\[
A J_e = K \sigma_{fus} \left( \frac{1-\eta}{\eta} \right)
\]  

(12)

The product \( A J_e \) is referred to as the heating factor. Pyrhönen et al. [13] present the following values of \( A J_e \) for non-salient pole synchronous machines.

For indirect air-cooled electrical machines, \( A J_e \) varies from \( 10 \times 10^{-3} \) to \( 40 \times 10^{-3} \) A²/m². For indirect hydrogen-cooled electrical machines, \( A J_e \) varies from \( 36 \times 10^{-3} \) A²/m² to \( 66 \times 10^{-3} \) A²/m². Finally, for direct liquid-cooled electrical machines, this heating factor varies from \( 100 \times 10^{-3} \) to \( 200 \times 10^{-3} \) A²/m².

Fig. 1 shows values of heating factor \( A J_e \) and current density for various tangential stress values calculated by maintaining constant efficiency as rotor surface linear speed increases. The following parameter set was assumed: \( k_m = 1.2, k_m = 0.8, k_m = 1.2, k_m = 0.966, \phi = \omega = \omega, \rho_{Cu} = 0.0029 \Omega \cdot mm²/m \) at a working temperature of 80°C. The current density in the stator winding was estimated from the \( A J_e \) values with a linear current density equal to \( A = 130 \) kA/m. The numbers in parentheses in Fig. 1 are generator efficiency and tangential stress (in kPa).

Copyright © 2015 Praise Worthy Prize S.r.l. - All rights reserved

International Review of Electrical Engineering, Vol. 8, N. 6

1731
Despite the fact that the obtained $A_{tot}$ values are below those stated in [13] for direct liquid-cooled electrical machines, the following important conclusions can be drawn from the present study.

1. Under equal conditions of tangential stress, the winding material, the ratio of active length to total conductor length, and the loading conditions required to maintain constant efficiency an increase in the linear speed of the rotor surface should be proportional to the increase in the heating factor and vice versa.

2. Since the volume of the copper appears in the denominator of $A_{tot}$ and mass is proportional to volume, a significant parameter in the design of a low-speed generator. According to Figure 1, if copper winding mass increases, machine efficiency increases, i.e., the value of $A_{tot}$ at 96% efficiency is less than the value at 92% efficiency. A smaller value for $A_{tot}$ corresponds to an increase in copper mass. As shown later in Figure 9, an 8 MW LC DD-PMMSG reaches an efficiency of more than 96% when the load is very low and the speed is high. In practice, reaching efficiencies greater than 97% is only possible if the machine diameter is extremely large.

III. Design Concept

Based on the above considerations, an LC DD-PMMSG that employs direct liquid stator cooling was designed. This design is presented in the following sections.

III.1. Main Design Issues

The large air gap diameter of a high-power wind turbine generator, e.g., 5 m or more, requires segmented construction. Rotor and stator magnetic circuits can be divided into some number of identical units. Furthermore, these units can be electrically connected in series or in parallel to get desired voltage levels.

Many variations for the number of units in this basic design are possible including 6, 8, 10, 12 units, and so on. This paper focuses on a machine constructed of twelve identical stator segments.

A tooth coil winding architecture was selected as it enables the most straightforward implementation of direct liquid cooling of the stator conductors.

The machine rotor is also made up of 12 segments. The concept design for the active region of the LC DD-PMMSG is shown in Fig. 2.

Each of the stator segments has 12 slots. The total number of slots for 12 stator segments is 144. The rotor comprises 10 magnet poles per segment for a total of 120 magnet poles or 60 pole pairs. The configuration results in a rated frequency of 10-12 Hz, which is acceptable for the cyclic loading of the converter's insulated-gate bipolar transistors (IGBTs). This type of machine works at the fifth harmonic of the stator current linkage, using it as the means of electromechanical power conversion.

A double-layer winding configuration offers lower sub-harmonics in the current linkage distribution and shorter end windings compared to single-layer windings. The operating harmonic winding factor is $\alpha = 0.966$ for a 6-phase (double three-phase winding with 30° spatial and time phase shifts between the independent three-phase winding groups), 10-pole, 12-slot, double-layer, concentrated winding. The differential leakage factor is $\alpha_s = 0.835$, based on Heller and Harnau calculation [17].

Fig. 2. Concept design of the active region of an outer rotor LC DD-PMMSG: (1) rotor support structure, (2) rotor lamination, (3) rotor-surface-mounted permanent magnets, (4) liquid-cooled stator winding, (5) stator lamination, (6) stator support structure

The stator windings are made of hollow copper conductors to manage Joule heating. To maximize copper cross section, the conductors are rectangular. An aspect ratio of 1.2 (width to height) was chosen for manufacturability. Cooling liquid, passing axially through the hollow copper, removes the internally generated heat through convection. Fig. 3 illustrates how the tooth coil is formed. The coils sit in the stator slots surrounding each stator tooth.

LC DD-PMMSG tooth coils must form a dense circular array for optimum electromagnetic performance.

The number of coils is fixed. In this case, there are 144 double-loop coils. The physical width of each coil is determined by the width of the copper conductor and its minimum practical bending radius, which is two times its width. This minimum bend radius also sets the minimum tooth width.

To minimize generator size, the diameter of the circular array should be as small as practical while still providing room for the 144 coils. Typically, the designer would select a copper conductor from a list of available sizes, and then determine the minimum width of each coil. Coil number and width determine the coil array diameter, which sets generator gap diameter.

For the 8 MW design, appropriate conductor cross sections were 15 mm x 18 mm (height x width) with a 7 mm coolant channel diameter.
The width of each coil tooth was 72 mm, and the minimum possible gap diameter was just less than 7 mm.

III.2. Defining Geometries for the LC DD-PMsG

The method of steepest descent with variable step size [18] was applied to find the combination of air gap diameter, stator active length, and the number of conductors per slot to minimize active mass within thermal and electrical constraints and the constraints of manufacturability. The optimization algorithm assumes a set of design parameters, and then calculates predicted generator performance. Each result guides adjustment of the input set for subsequent performance predictions.

This process continues until predetermined criteria are met. The methodology proposed by Pyhönen et al. [13] is used to predict electromagnetic performance. Thermal performance is estimated using the procedure described in [19]. Specifically, the objective function for the optimization was minimizing $m_{c}, m_{M}$, and $m_{s}$, the total copper, permanent magnet, and active steel masses.

A detailed description of the optimization algorithm was published in [20].

Table II summarizes the optimization results for an 8 MW LC DD-PMsG. The results presented in Table II support the findings reported in previous paragraphs about the relationship between tangential stress and the heating factor $A_f$ and their effect on generator efficiency. According to Table II, the power factor for the LC DD-PMsG design is low. This is because of the low induced voltage at no-load. Figs. 4 present the phasor diagrams for an 8 MW DD-PMsG operating at constant output power. According to simulation results, increasing the power factor by 0.17 units (cos $\phi = 0.8$) while maintaining rated output power requires a doubling of the permanent magnet material. See Fig. 4(b).

If converter cost is 40 €/kVA and permanent magnet material price is 100 €/kg, it is difficult to justify the additional investment in PM material. Increasing power factor by reversing stator current (negative $I_f$ direction) requires four extra copper conductors per slot to maintain rated output power, which results in a drop in peak output power reserve (Fig. 4(c)).

### Table II

<table>
<thead>
<tr>
<th>Component</th>
<th>Rating Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated power</td>
<td>8.5 MW</td>
</tr>
<tr>
<td>Rated speed</td>
<td>11 rpm</td>
</tr>
<tr>
<td>Line to line voltage</td>
<td>3.3 kV</td>
</tr>
<tr>
<td>Rated phase current</td>
<td>1150 A</td>
</tr>
<tr>
<td>Number of phases</td>
<td>6</td>
</tr>
<tr>
<td>EMF factor</td>
<td>0.74</td>
</tr>
<tr>
<td>Tangential stress</td>
<td>80 kPa</td>
</tr>
<tr>
<td>Linear current density</td>
<td>147 kA/m</td>
</tr>
<tr>
<td>Current density</td>
<td>4.8 A/mm²</td>
</tr>
<tr>
<td>Peak output power to rated output power ratio</td>
<td>1.6</td>
</tr>
<tr>
<td>Stator length</td>
<td>1.15 m</td>
</tr>
<tr>
<td>Number of conductors per slot</td>
<td>20</td>
</tr>
<tr>
<td>Copper mass</td>
<td>8.1 t</td>
</tr>
<tr>
<td>Steel mass</td>
<td>40 t</td>
</tr>
<tr>
<td>Magnet mass</td>
<td>4.5 t</td>
</tr>
<tr>
<td>Total generator with bearing mass</td>
<td>92 t</td>
</tr>
<tr>
<td>Lead angle</td>
<td>35 deg</td>
</tr>
<tr>
<td>Power factor</td>
<td>0.63</td>
</tr>
<tr>
<td>Copper losses</td>
<td>550 kW</td>
</tr>
<tr>
<td>Total losses</td>
<td>651 kW</td>
</tr>
<tr>
<td>Rated electrical efficiency</td>
<td>92.5%</td>
</tr>
<tr>
<td>Number of cooling circuit per tooth coil</td>
<td>1</td>
</tr>
<tr>
<td>Outlet liquid temperature</td>
<td>81 °C</td>
</tr>
<tr>
<td>Coolant velocity</td>
<td>1.0 m/s</td>
</tr>
</tbody>
</table>

Figs. 4. Phasor diagrams for an 8 MW DD-PMsG: $L_x = I_x - 1 p.u.$

- a) $E_{m1} = 0.74 p.u., X_m = 0.68 p.u., \phi = 35^\circ, P_{m1}/P = 1.67, n_o = 20, \text{cos} \phi = 0.63$
- b) $E_{m2} = 0.85 p.u., X_m = 0.5 p.u., \phi = 29^\circ, P_{m2}/P < 1.55, n_o = 20, \text{cos} \phi = 0.8$
- c) $E_{m3} = 0.89 p.u., X_m = 0.7 p.u., \phi = 41.5^\circ, P_{m3}/P = 1.45, \text{cos} \phi = 0.8$

IV. LC Tooth Coils Prototype

To demonstrate the new direct liquid-cooling technology applied to cool stator windings, a small prototype motorette with two liquid-cooled tooth coils and recirculating cooling loop was designed, built, and tested at the Lappeenranta University of Technology.

The specific goals for the prototype were to prove the feasibility of manufacturing the LC DD-PMsG tooth coils and demonstrate the effectiveness of direct liquid cooling at the stator copper conductors.
Fig. 5 is a photo of the small, instrumented prototype. It was located in a room without air movement and oriented so the slot conductors were horizontal. The installed coolant was ECOCUT HS, a polyalphaolefin (PAO) heat transfer fluid.

Each tooth coil in the prototype is formed from a 5.2 m long continuous copper conductor with a cross-sectional height of 13 mm and width of 15.6 mm. There are 4 turns per coil forming a two-row and four-column structure. The coolant conduits (316 series stainless steel tubing) are embedded in the copper conductor.

Each conduit has an inner diameter of 4 mm and a wall thickness of 1 mm. Each coil was formed, then wrapped with a layer of glass-fiber insulating tape (Thermaphile 45.021) to isolate conductor passes.

A second insulating wrap covered the active lengths, bundling four straight lengths of copper conductors on each side and electrically insulating the coil from the steel laminations. The steel laminations were laser cut from SURA® M400-50A coated electrical steel.

The lamination stack was bound tightly with eight hollow 316 series stainless steel tensioning rods. Plastic caps (PEI) secured the coils within the lamination slots.

Fig. 5. Experimental setup: (1) liquid cooled tooth coil; (2) inlet and outlet coolant manifold; (3) wires from the power source; (4) storage tank; (5) pump; (6) heat exchanger; (7) filter; (8) flow indicator; (9) thermocouples; (10) pressure indicator; (11) control system; (12) data processing system.

The two tooth coils were hydraulically connected to the cooling loop in parallel by bolting them to the PEI electrically isolating inlet and outlet coolant manifold. All stainless steel tubing connections were made via orbital welds or compression fittings. Compression fitting connections, necessary to accommodate the instrumentation, were kept to a minimum. The steel-to-plastic interfaces were sealed with Buna-N double-seal O-rings (quadrants).

Two concentric seals at each hydraulic connection offered four sealing surfaces to guarantee leak-free operation. Electrically, the tooth coils were connected in series. Incoming power was connected to the outer terminal lug of the first coil, and outgoing power was connected to the outer terminal lug of the second coil.

The series connection was made using a copper bridge between the coils fastened to the inner terminal lugs.

To evaluate the performance of a low-speed DD generator realistically, a 1110 A, 11 Hz power supply would have been required to produce characteristic system losses. Such a system was not available, and a higher frequency system was used to produce losses in the tooth coil conductors with lower current. The power source was a variable frequency 550 Hz synchronous generator capable of currents up to 150 A. Installed in our laboratory, it could operate continuously at 104 A without overheating the fuse.

To maximize Joule heating, testing was carried out using the highest current and frequency that could be sustained: 104 A and 540 Hz. Using this arrangement, the losses achieved were not sufficiently high.

To elevate system temperatures further, a 5 mm thick steel plate was set on top of the laminations above the coils to act as an additional source of eddy current loss heating. While no longer a precise simulation, the test setup can still be regarded as indicative, making it possible to observe how effectively the cooling system could remove heat.

With power on and the plate in position, 75 W of heating was produced in each coil and 3.1 kW was produced in the solid plate. RTDs (Resistance Temperature Devices) were attached to each coil to monitor copper temperature at the inlet, outlet, and middle of each conductor length. Inlet and outlet coolant temperature was monitored using Type K thermocouples. Fig. 6 shows the temperatures measured over a five hour period.

Fig. 6. Measurement results for a 5-hour period.

On the left in Fig. 6, where the temperature curves are relatively flat, the coolant loop is operating normally with 2.05 l/min coolant flow through each coil. Coolant pressure at each inlet is 2.9 Pa. At approximately 15:40 on the time of the day axis, the pump was switched off stopping coolant flow. The result is the rapid increase in copper temperatures and drop in coolant temperatures shown near the right side of the plot. At 16:16, the pump was switched back on, and measured temperatures quickly stabilized once again.
For the duration of testing, all thermocouples were continuously monitored and data was recorded at 1 s intervals.

Results reveal that because power loss in the copper conductors is uneven, copper temperature at the middle of each tooth coil length could be similar or even higher than copper temperature at the coolant outlet end.

Despite the low copper tooth coil heating observed during the laboratory test, the thermal test demonstrates the effectiveness of direct liquid cooling. When the pump was switched off, in the second test copper temperatures began increasing rapidly at 0.015°C per s.

To emphasize the direct liquid cooling capability of the tooth coil design, Table III gives the calculated temperatures in steady state for 3 kW power losses in a similar prototype tooth coil.

<table>
<thead>
<tr>
<th>Component</th>
<th>Temperature, °C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet oil temperature</td>
<td>40</td>
</tr>
<tr>
<td>Outlet oil temperature</td>
<td>83.2</td>
</tr>
<tr>
<td>Steel laminazation</td>
<td>72.9</td>
</tr>
<tr>
<td>Copper temperature (near oil inlet)</td>
<td>46.8</td>
</tr>
<tr>
<td>Copper temperature (middle of tooth coil length)</td>
<td>85.2</td>
</tr>
<tr>
<td>Copper temperature (near oil outlet)</td>
<td>86.6</td>
</tr>
</tbody>
</table>

The temperature difference between the inlet and outlet steady-state temperatures is 43.2°C, and based on the heat balance equation, the heat transferred by the oil is 2.9 kW, showing that direct liquid tooth coil cooling is extremely effective and a relatively simple cooling approach.

V. Drive Train Structure and AEO Calculation for Proposed 8 MW LC DD-PMSC

The operating characteristics for a wind turbine equipped with the proposed 8 MW LC DD-PMSC are evaluated, and its annual energy output and load factor are then calculated using North Sea wind data as a starting point.

V.1. Wind Turbine Characteristics for 8 MW LC DD-PMSC

Variable-speed wind turbines are becoming more common than wind turbines operating at constant speed, because they offer improved power quality impact, lower drive train stress, and because of resulting reductions in weight and cost [21]. For low to moderate wind speeds, the control goal is to maintain constant aerodynamic efficiency. For high wind speeds, the control goal is to maintain constant rated output power without overloading the system. Wind generator output depends on the amount of wind swept by the blades. The power extracted from the wind can be described as follows [22]:

\[ P_{WT} = \frac{1}{2} \rho_{air} \pi r_{blade}^2 v_{wind}^3 C_p (\lambda, \beta) \]  

(13)

where \( \rho_{air} \) is the air density, \( r_{blade} \) is the radius of the rotor blade, \( v_{wind} \) is the wind velocity, \( C_p (\lambda, \beta) \) is the dimensionless power coefficient, \( \lambda \) is the tip speed ratio, and \( \beta \) is the blade angle.

Low-noise requirements limit wind turbine tip speeds. Most 5-6 MW class offshore turbines have a rated tip speed of about 84-90 m/s. A 156 m diameter rotor for an 8 MW wind turbine operating at a maximum tip speed of 90 m/s rotates at 11 rpm. The calculated performance characteristics for an 8 MW wind turbine are shown in Figs. 7 and 8.

Several operating regions for a DD wind turbine can be defined based on incoming wind speed, rotor speed, and wind turbine mechanical power. The control strategy used in this wind turbine study has been described in [23]. In this control strategy, the frequency converter directly controls generator speed. The operating regions are illustrated in Fig. 7, which shows a family of curves showing how the power varies with rotor speed for various wind speeds corresponding to the various regions. Between points A and B, rotor speed is varied linearly to track the maximum power coefficient \( C_p (\lambda = 8.54, \beta = 0 = constant) = 0.43 \). At 10.5 m/s of wind speed, tip speed reaches 90 m/s (point B). Therefore, rotor blade speed is held constant between points B and C. At 12.5 m/s, the rotor reaches its rated power. Region CD corresponds to wind speeds larger than the rated wind speed. The reference output power is the rated electrical power, while the reference rotor speed is the rated rotor speed. Thus, for each wind speed, the power coefficient can be calculated from Eq. (13).

The corresponding values for blade angle are then obtained from the \( C_p (\lambda, \beta) \) table. Pitching the blade angle reduces aerodynamic conversion efficiency, therefore, less mechanical torque acts on the generator and rotor power is held constant. In Fig. 7, for a wind speed of 25 m/s, power is no longer controlled, and it becomes mandatory to stop turbine rotation. Figure 8 shows the blade angle versus incoming wind speed as well as the regulation trajectory for the \( C_p \) in the various operating regions.

![Fig. 7. Wind turbine power curve and corresponding power coefficient curves](image)
V.2. LC DD-PMSG Partial Load Efficiencies

Output power resulting from the wind passing through the main rotors of a LC DD-PMSG is given by the product of the mechanical output power of the wind turbine and the efficiency of the generator.

Generator efficiency varies with load, so partial load efficiencies must be used to determine total wind turbine energy production over time with varying wind conditions.

Fig. 9 maps generator efficiency as a function of main rotor torque and speed.

The torque-speed characteristics of a LC DD-PMSG are shown by the bold black curve. At low rotor speeds, efficiency increases gradually to 96.5%. Clearly, the LC DD-PMSG generator offers impressive partial load efficiencies.

V.3. Calculation of Annual Energy Output and Load Factor

The annual energy production calculation has been performed using the wind speed distribution for North Sea coastal waters with an annual average wind speed of 9.02 m/s at an elevation of over 30 m. Wind speed distributions are commonly modelled using Weibull and Rayleigh probability density functions $f(v)$ [24]. The Weibull shape parameter $k$ and scale parameter $c$ of 2.7 and 10.6, respectively, for the North Sea were used [25].

The average power available from the wind turbine was estimated by integrating the product of the power output of the generator at each wind speed by the probability of the occurrence of that wind speed.

Finally, to calculate the annual energy production available, the total average generator output power was multiplied by the number of hours in the year and factor $k_t = 0.97$, which accounts for yearly turbine maintenance.

The generator efficiency $\eta(v)$ is shown in the following equation in integral form:

$$8760k_t \int \eta(v) P_{WT}(v)f(v) \, dv$$

Fig. 10 shows the resulting distribution of the wind energy content superimposed on the Weibull wind speed distribution that caused it and wind turbine power output for an 8 MW LC DD-PMSG. The estimated annual energy output is approximately 33.8 GWh per year.

Fig. 10. Wind speed versus Weibull probability distribution, power curve for 8 MW LC DD-PMSG, and wind energy content.

Load factor is then defined as the ratio of average power output over the theoretical maximum output over a year as expressed by the following:

$$\hat{k} = \frac{8760 \int \eta(v) P_{WT}(v)f(v) \, dv}{P_a}$$

The estimated load factor for the 8 MW LC DD-PMSG studied is 48.9%. The high load factor makes the wind turbine assembled with the proposed 8 MW LC DD-PMSG to be best matched to the North Sea site from the energy capture point of view.

VI. Conclusion

It is sometimes argued that high-power, direct-drive PMSGs will prove to be of limited practical importance due to their relatively large size and weight. However, applying direct liquid cooling to the stator winding makes it possible for high-power DD-PMSGs to become major contributors to wind power.

A concept for an LC DD-PMSG design solution has been introduced and considered based on theoretical
Y. Alexandrova, R. S. Semken, J. Pyrhonen

analysis. Key aspects related to the proposed LC DD-PMSG concept have been discussed here: tangential stress, current density, linear current density, heating factor, and generator efficiency at full and partial load. The suggested LC DD-PMSG design solution was based on optimization for minimal generator mass.

An instrumented small prototype motorrotte with two liquid-cooled tooth coils was built to provide measurement data to validate predictions. The prototype demonstrated the ability of effective direct cooling of the tooth coils. When coolant circulation ceased, copper temperature began increasing rapidly. The prototype demonstrated the technical feasibility of the LC tooth coil design. Furthermore, this combined theoretical and experimental study strengthens the presumption that the proposed cooling approach makes an LC DD-PMSG possible and can solve the problems associated with the growing dimensions of high-power DD wind turbine generators. Finally, the characteristics of a variable-speed wind turbine equipped with a proposed LC DD PMSG were investigated in terms of annual energy production and load factor. Although the peak load efficiency of this LC DD-PMSG is slightly less than ideal, its higher partial load efficiency performance ultimately results in more electricity production.

Acknowledgements

The authors would like to thank the Academy of Finland for their support for this research.

References


Authors' information

1Electrical Engineering Department, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 53851, Finland.
2Mechanical Engineering Department, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 53851, Finland.

Yulia Alexandrova completed the M.Sc. degree in Electrical Engineering from StPetersburg \" Luther \" (Russia, Saint-Petersburg) and Lappeenranta University of Technology (Finland, Lappeenranta) in 2009. She continued the educational process toward the D.Sc. degree at Department of Energy, Electrical Engineering. Her current interest focuses on the analytical calculations and numerical simulations of high-torque low-speed electrical machines.

Robert Scott Semken is from the Rocky Mountain region of the United States. A degree mechanical engineer, Scott's early career focused on preparing structural, dynamics, and thermal analyses for the energy industry. Forming his passion for machine systems, the focus changed to the design and development of complex electro-mechanical machinery for a variety of American capital equipment manufacturers. Most recently, Mr. Semken has begun working on his doctor's degree in mechanical engineering at LUT, participating in the development of large direct-drive permanent magnet wind turbine generators.

John Pyrhonen received the D.Sc. degree from Lappeenranta University of Technology (LUT), Finland in 1991. He became an Associate Professor of Electrical Engineering at LUT in 1993 and a Professor of Electrical Machines and Drives in 1997. He is currently the Head of the Department of Electrical Engineering, LUT, Finland, where he is engaged in research and development of electric motors and electric drives. His current interests include different synchronous machines and drives, induction motors and drives and solid rotor high-speed induction machines and drives.
Publication 4

Copyright © 2014, IEEE. Reprinted with permission from IEEE.

Defining Proper Initial Geometry of an 8 MW Liquid-Cooled Direct-Drive Permanent Magnet Synchronous Generator for Wind Turbine Applications Based on Minimizing Mass

Y. Alexandrova, S. Semken, M. Polikarpova, J. Pyrhönen

Abstract – The minimum mass of the active materials needed to build an 8 MW liquid-cooled direct-drive permanent-magnet synchronous generator (LC DD-PMSG) is sought. Proper set of generator dimensions is defined based on a constrained steepest descent method with variable step size. The design variables have been defined in terms of two quantities: stator length and the ratio of induced voltage to rated voltage (EMF factor).

Index Terms – generator, optimization, wind turbine.

I. INTRODUCTION

Wind energy resources may make up a significant share of future electricity generation capacity requirements. The European Wind Energy Association predicts that in 2020 there will be 230 GW of wind power installed in the EU with 580 TWh annual electricity generation capacity [1]. To reach this target, it will be necessary to build new wind farms, e.g., in offshore areas. Offshore wind speeds tend to be higher and the winds less turbulent and steadier compared to corresponding inland figures and offer, therefore, possibilities for high power generator parks.

Despite the fact that offshore wind has the potential to provide substantial amounts of energy there are many challenges to be overcome as environmental stresses are high and installation and maintenance are difficult and costly in offshore areas. Therefore, the properties of new offshore technology must be extremely competitive. One of the most important challenges is the issue of developing the next generation of offshore wind turbines, including exploring concepts of very large scale turbines in the 10–20 MW range [2].

For a direct-drive (DD) generator, two critical factors are crucial: outer diameter and mass. Both of these are indirectly affected by the cooling method of the generator. Although, in cases of traditional air cooling, the rationale for increasing the generator diameter is clear, the logistics of transporting such oversized generator over long distances becomes problematic. To limit the physical dimensions of the DD generator, cooling must be intensified. Direct liquid cooling is the most efficient phase before entering superconducting machines. One obvious benefit of the direct liquid cooling in this case is that no stator sub-stacks are needed and the whole stator stack can be made uniform which greatly helps in intensifying the tangential stress $(\sigma_T)$ that produces the torque $T$ of the machine as $T = \sigma_T S$ with the rotor radius $r$ and rotor active surface $S$. Just leaving away the cooling ducts which are needed for air cooling increases effective $S$ and, therefore, the apparent tangential stress value by 10–20 % [3].

However, arriving at the proper initial design for an electrical machine with a direct liquid-cooled stator winding is complicated due to a) the large number of unknowns, b) the nonlinear relationship between electrical machine parameters, and c) the presence of certain discrete variables, such as available sizes of hollow-copper conductors. In addition, the objective function, which represents the sum of the masses of the permanent magnets, copper, and steel of the generator is also nonlinear and causes great difficulty in the optimization of the generator design. Ultimately, the solution depends largely on the experience of the engineer who performs the optimization.

In this paper, the steepest descent method that allows variable step size [4] is applied to find such a combination of stator air gap diameter, stator length and number of conductors per slot of an 8 MW liquid-cooled direct-drive permanent-magnet synchronous generator that minimizes total generator masses under manufacturability, thermal and electrical constraints. It should be noted that optimization used in this paper should be treated as a variation of the input data and a following computation. The results of each computation guide adjustments of the decision variables while the predetermined criteria are fulfilled.

II. BASIC DESIGN CONCEPT

It is well known that a liquid cooling typically performs significantly more efficiently than air-cooling, and at the same time it is much quieter. But with liquid cooling, there is always the possibility of leaks that can be costly, and liquid cooling is more expensive and more difficult to install. However, when a high-power electrical generator is constructed using liquid cooling, compromises can be made to keep the cooling arrangements simple. Using concentrated non-overlapping windings (tooth coil windings) [5], very low operating frequency limiting conductor skin effect, and permanent magnets offers simplicity that can be utilized in direct liquid-cooled applications without the complicated systems familiar in liquid-cooled turbo-generators.

The stator cores of conventional radial flux machines of large diameter usually comprise of the stator lamination segments joined to each other in order to obtain the full circumference. The original idea of this PMSG design is to use a tooth coil multiple phase segmented construction. Tooth coil base machines offer multiple choices but the one with 12 stator slots and 10 rotor poles, resulting in 0.4 slots per pole and phase $q = 0.4$ for three phase winding and $q = 0.2$ for six phase winding, is one of the convenient solutions with its nice overall properties. In case of a large diameter relatively low power machine the mass and the price of the
construction are key issues. As simple a construction as possible reduces both the mass and the cost. The winding of the studied PMSG is designed of hollow copper conductors through which cooling liquid such as distilled water can be circulated. The cooling water removes heat by forced convection. Such a coil is illustrated schematically by Fig. 1.

![Fig. 1. Liquid-cooled tooth coil.](image)

The tooth coil sets several boundary conditions for the machine design as e.g. clearly there is a minimum practical bending radius for the conductor. Therefore the minimum tooth width at the same time results, in practice, also in a certain arc length for the basic 12-10 machine. Recent trends in medium-voltage wind turbine generator and converter combinations have shown potential for reliability and efficiency improvement. Table I shows the chosen specification data to initialize the iterative process.

**TABLE I**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated power</td>
<td>8 MW</td>
</tr>
<tr>
<td>Rated speed</td>
<td>11 rpm</td>
</tr>
<tr>
<td>Rated torque</td>
<td>6.95 MMin</td>
</tr>
<tr>
<td>Rated line-to-line voltage</td>
<td>3.3 kV</td>
</tr>
<tr>
<td>Number of segments</td>
<td>12</td>
</tr>
<tr>
<td>Number of poles</td>
<td>120</td>
</tr>
<tr>
<td>Rated frequency</td>
<td>11 Hz</td>
</tr>
<tr>
<td>Number of slots</td>
<td>144</td>
</tr>
</tbody>
</table>

The key component of a directly water cooled tooth coil generator is the coil itself. The selection of the dimensions for a copper cross section suitable for the tooth coil is highly dependent on the minimum bending radius to which a hollow conductor can be bent without deformation and on the fact that the combination of conductor maximum width and minimum height for a certain hole radius probably allows a decrease in the synchronous inductance of the generator, and therefore an increase in the maximum overload generator torque capacity. Minimum bending radius also establishes the minimum tooth width, whose flux density conditions should be checked then. Therefore, by taking into account both these arguments, the following conductor cross sections have been used in this case study: 15 mm height × 18 mm width with 2.25 mm hole radius. The material masses needed for a generator are calculated from the analytical model of the generator. In order to design the PMSG, the methodology proposed in [6] is used for the electromagnetic design, while the thermal performance is estimated using the procedure given in [7].

**III. DESIGN PROCEDURE**

A. Algorithm Description

The flowchart of the algorithm is shown in Figure 2. The two variables to be used as design variables are the EMF factor $k_e$ and the stator length $l$. For each iteration ($r$) only one of the two design variables is permitted to change while the other one remains fixed. $n = [1, 2]$ is used to indicate the order number of the design variable $y = \{k_e, l\}$ permitted to change: $n(1) = 1$ means that the EMF ratio $k_e$ is permitted to change, while the stator length $l$ is fixed; $n(2) = 2$ means that the stator length $l$ is permitted to change, while the EMF ratio $k_e$ is fixed. Before the iteration, the EMF ratio and the stator length have to have starting values ($y_{\text{init}} = \{k_e, l\}_{\text{init}}$). $\Delta = [\Delta k_e, \Delta l]$ defines the discretization step size for each design variable. $\alpha \in \{1, 2\}$ is a scaling factor for $k_e$ or $l$, with $\alpha$ which is the minimum value of $\alpha$ used to stop the algorithm. The objective function is the total active mass of the generator materials (permanent magnets $m_{\text{mag}}$, steel $m_{\text{s}}$ and copper $m_{\text{Cu}}$). The value of $\Delta(n)$ at each iteration is determined by scaling its previous values using a scaling factor $\delta_1$, i.e., $\Delta(n)_{\text{next}} = \Delta(n)_{\text{old}} \times \delta_1$. The first iteration begins with $\alpha = 1$. For each iteration round ($r$) the objective function is calculated at three different points $\xi(1) = 1, 2, 3$. The second point $\xi(2)$ refers to the current value $\xi(2) = \gamma(n)$ for the design variable, while the first $\xi(1)$ and the third points $\xi(3)$ are respectively equal to the $\xi(1) = \gamma(n) = \Delta(n)$ and $\xi(3) = \gamma(n) + \Delta(n)$ of the current design variable which are permitted to change. Based on the analytical model of a PMSG the geometrical dimensions (stator slots, magnet height, etc.), the electromagnetic parameters (inductances, resistances, current components, flux densities, losses, efficiency, maximum torque, power factor angle, etc.), water-cooling parameters (outlet water temperatures, water pressure, water flow) are calculated as well as the objective function is evaluated for three values of design variable $\xi$. The following PMSG parameters are chosen to be constant:
- the conductor cross section (width × height × hole radius),
- the slot wedge height, the number of basic 12-10 machines, the inlet coolant water temperature, the allowable flux densities in the yokes; the insulation thickness;
- the airgap diameter $D_{\text{g}}$ is defined as a function of conductor width as 385 × $r_b$. The value 385 just happens to give an appropriate starting point for the iteration and does not represent any higher intelligence;
- the air gap length is kept at its minimum value limited by mechanical considerations to 0.125 % of the stator bore diameter;
- the effective permanent magnet width is kept as a constant fraction of the pole pitch $b_{\text{w}} = 0.8 \times r_p$;
- the number of conductors per slot width is kept constant and equals four enabling two coils having two adjacent conductors.

Obtained results are checked to be under the given set of constraints. The maximum water outlet temperature $T_{\text{out}}$, maximum water pressure $p$, the minimum efficiency $\eta$, the minimum power factor $\cos \eta$ and the minimum value of generator torque overload ability under constant speed are used as constraints in the algorithm. The overload multiple is ratio between maximum output torque and rated output torque ($T_{\text{out}}/T_{\text{rated}}$). If any of the constraints is not satisfied, this iteration is removed from further consideration.

Throughout the iteration $r$, we select whichever value of the
design variable $\xi(i)$ gives a minimum value for the objective function $m^*_r$, under the given set of constraints $1$ [2]. In case when the objective function value $m^*_r$ in the current iteration $r$ is bigger than the value of the objective function in the previous iteration $m^*_{r-1}$, and there have been two successive iterations for both design variables $\gamma = [k_\xi, l_\xi]$, EMF ratio and stator length) with the same value of the scaling factor $\alpha$ (i.e. $n = n(2)$), there is no change in the value of the objective function $1$. Then the scaling factor is gradually changed by $\alpha = 0.99 \times \alpha$. The decision to emphasize the "search in depth" instead of "search in breadth" is due to the fact that it is not possible to specify exactly which minimum value of $\Delta(n)$ must be set for a certain design variable, and therefore, it is set in terms of what a reasonable maximum step size could be. If $\alpha < 0.1$, we start another iteration ($r = r + 1$) [3] with the same starting value for the design variables $\gamma = [k_\gamma, l_\gamma]$ EMF and stator length ratio) as in the previous iteration $\gamma = \gamma_r$), but with reduced variation step $\Delta(1)$ for the first design variable $n = n(1)$, [3] the next iteration starts with the previous scaling factor $\alpha = 1$ and the initial discretization step size $\Delta = \Delta_0$. The starting values for the EMF ratio and stator length for the next iteration in this case are the final values of the PMSG designed in the current iteration $\gamma = \gamma_r$. [4] This step takes place with no change in the value of the objective function (i.e. $m = m^*_r$) and therefore the next iteration starts with the previous scaling factor $\alpha = \alpha$ and the $n = n(2)$ design variable (i.e. stator length) will be permitted to change during the next iteration. The starting values for the design variables (EMF ratio and stator length) are the same as in the previous iteration $\gamma = \gamma_r$. [5] The algorithm stops when there cannot be found any better combination of stator length and EMF factor, even with the smallest variation step $\Delta(n)$. Simulation results have shown that $\Delta(n)$ drops by a factor of $\alpha = 0.01$ when the objective function will not more be corrected, and therefore the value of $\alpha = 0.01$ to stop the iteration is appropriate.

**B. Set of constraints**

First, we limited the temperature at the outlet of the cooling channels and the total pressure drop along the cooling circuit, which both are critical from the machine reliability point of view. To maintain the outlet coolant temperature below a certain limit e.g. 80 °C, we placed a restriction on the pressure drop along the cooling circuit (< 2 bar) to limit the pumping power required. Second, because computation results showed that the generator efficiency drops continuously with increasing tangential stress, we placed a lower limit of $q \geq 92$ % on the generator efficiency.

Third, again for reasons of reliability, we required that the resulting generator design must be capable of withstanding at least the 130 % of its rated torque.

Finally, to limit the converter overhead to a sensible level, it was necessary to limit the power factor no lower than $\cos \varphi = 0.7$ ($\varphi = 45^\circ$).

**C. Objective function**

The objective function minimizes the total mass of the copper $m_{Cu}$, permanent magnets $m_{pm}$, and steel $m_{Fe}$ materials. It can be written in the form:

$$ m = m_{Cu} + m_{pm} + m_{Fe} \rightarrow \min. $$

$$ m_{Cu} = l_i \alpha S \rho_{Cu} \rightarrow \min. $$

**Fig. 2. Flow chart of the iteration process to find an proper dimension set of the LC DD-PMSG**
\[ m_{\text{set}} = 2 \pi m_{\text{pole}} \rho_{\text{m}} \delta_{\text{m}} \Delta_{\text{m}} \delta_{\text{m}} \]
\[ m_{\text{set}} = \frac{\pi}{4} \left[ (D_e^2 - D_i^2) \delta_{\text{m}} \rho_{\text{m}} + \ldots \right] \]
\[ Q h_b w_d = \frac{\pi}{4} \left[ (D_e^2 - D_i^2) \delta_{\text{m}} \rho_{\text{m}} + \ldots \right] \]
\[ m_{\text{f}} = (m + m_{\text{active}}) \times 1.35 \]

where \( D_e \) – outer diameter of the rotor, \( D_i \) – inner diameter of the rotor, \( D_r \) – outer diameter of the stator, \( b_r \) – stator yoke height, \( h_m \) – height of magnet, \( b_t \) – tooth height, \( z_p \) – number of conductors in slot, \( \delta_{\text{m}} \) – space factor for steel, \( \delta_{\text{m}} \) – average conductor length, \( l \) – rotor core length, \( l_m \) – permanent magnet length, \( m_{\text{active}} \) – active mass of steel lamination, \( p \) – number of pole pairs, \( Q \) – number of stator slots, \( w_d \) – conductor cross-sectional area, \( \rho_{\text{m}} \) – permanent magnet width, \( w_u \) – tooth width, \( \rho_{\text{Cu}} \) – copper resistivity, \( \rho_{\text{Fe}} \) – steel resistivity, \( \rho_{\text{Fe}} \) – permanent magnet resistivity. Inactive material consisting mainly of steel, which provides structural support to maintain the airgap between the stator and the rotor was estimated as 35% of the active material of steel lamination and copper mass.

**IV. CASE STUDY AND RESULTS**

The method is then applied to the LC DD-PMSG, which consist of twelve basic 12-10 machines, 6-phase stator windings and conductor cross section dimensions of 15 mm in height by 18 mm in width and with a 2.25 mm hole radius. The initial discretization step size \( \Delta \) is: \( \Delta E = 0.1 \) and \( \Delta t = 0.1 \) m for EMF factor and stator length respectively. Table 2 lists the proper design dimensions for this study case.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator airgap diameter</td>
<td>6.93 m</td>
</tr>
<tr>
<td>Stator length</td>
<td>1.1 m</td>
</tr>
<tr>
<td>EMF factor</td>
<td>0.8</td>
</tr>
<tr>
<td>Copper mass</td>
<td>9.21</td>
</tr>
<tr>
<td>Steel mass</td>
<td>311</td>
</tr>
<tr>
<td>Magnet mass</td>
<td>3.61</td>
</tr>
<tr>
<td>Total mass</td>
<td>45.8 t</td>
</tr>
<tr>
<td>Efficiency</td>
<td>92.9%</td>
</tr>
<tr>
<td>Tangential stress</td>
<td>93 kPa</td>
</tr>
<tr>
<td>Linear current density</td>
<td>150 kW/km</td>
</tr>
<tr>
<td>Peak torque p.u.</td>
<td>1.3</td>
</tr>
<tr>
<td>Voltage per segment</td>
<td>275 V</td>
</tr>
<tr>
<td>Number of phases</td>
<td>6</td>
</tr>
<tr>
<td>Number of conductors per slot</td>
<td>24</td>
</tr>
<tr>
<td>Copper losses</td>
<td>580 kW</td>
</tr>
<tr>
<td>Total losses</td>
<td>690 kW</td>
</tr>
<tr>
<td>Outlet water temperature</td>
<td>80°C</td>
</tr>
<tr>
<td>Total pressure drop</td>
<td>1.1 bar</td>
</tr>
<tr>
<td>Flow rate</td>
<td>3.5 kg/s</td>
</tr>
<tr>
<td>Coolant velocity</td>
<td>1.02 m/s</td>
</tr>
<tr>
<td>Run time of the computations</td>
<td>7 min</td>
</tr>
</tbody>
</table>

The results of the analytical calculations have been verified by numerical simulation in commercial finite element software Flux 2D (Cedrat). Main dimensions given in Table II have been used. Induced voltage, rated torque and copper loss have been determined in order to check the analytical results.

The linear current density, \( B_0 \) is the air gap flux density.

High tangential stress results in a decrease in size and weight of the generator at the same torque level. According to Eq 6 the tangential stress is directly related to the linear current density. In traditionally air-cooled electrical machines the linear current density is mostly limited by the maximum allowable winding temperature rise. For 8 MW LC DD-PMSG according to Table II the linear current density is 150 A/m at 93 kPa tangential stress level. Therefore, applied direct liquid cooling of the stator windings becomes justified [6] since it guarantees that the winding temperature is kept being adjusted around the 80 °C.

The calculations show that after 9132 iterations a desired solution is found. The requirements of the generator and the cooling system are precisely met. The total computational time is about 7 minutes. Using this approach the proper design dimensions for a complex system, such as a direct-water cooled PMSG, can be determined in a robust manner. While this approach gives the specification of feasible direct-water cooled PMSG, in practice, customer requirements would finally define the generator's main dimensions.


2D FEA calculated total copper losses are 620 kW at rated point. RMS value of induced voltage at no-load is 1500 V which results in EMF factor equals 0.79. Temperature inside the conductors does not exceed 80°C.

Obtained results are quite close to the analytical ones given in Table II. The difference in the results is due to the simplifications used in the analytical model in order to keep computation times low, while the FEM simulations describe the real situation more detailed. However, we believe the results of the analytical optimization are only preliminary, and significant improvements of the design are still possible by further FEM iteration process.

V. CONCLUSION

With the constrained steepest descent method with a variable step size the proper specification of LC DD-PMSG with minimum mass has been determined. Within this approach only the stator length and EMF factor are considered variables, while the number of 10-poles/12-slots segments and conductor cross-section dimensions are imposed by the user. Using this approach, the mass of the generator is minimized, while the tangential stress becomes higher during the iterative process. This method is not too complicated and can provide useful information for the analysis, such as the tendency of generator parameters obtained from the continuous monitoring.

VI. ACKNOWLEDGMENT

The authors would like to thank the Academy of Finland for their support for this research.

VII. REFERENCES


VIII. BIOGRAPHIES

Yulia Alexandrova completed the M.Sc. degree in Electrical Engineering from SPbETU "LETI" (Russia, Saint-Petersburg) and Lappeenranta University on Technology (Finland, Lappeenranta) in 2009. She continues the educational process toward the D.Sc. degree at Department of Energy, Electrical Engineering. Her current interest focuses on the analytical calculations and numerical simulations of high-torque low-speed electrical machines.

Scott Semken is from the Rocky Mountain region of the United States. A degreed mechanical engineer, Scott's early career focused on performing structural, dynamics, and thermal analyses for the energy industry. Pursuing his passion for machine systems, the focus changed to the design and development of complex electro-mechanical machinery for a variety of American capital equipment manufacturers. Most recently, Mr. Semken has began working on his doctor's degree in mechanical engineering at LUT, participating in the development of large direct-drive permanent magnet wind turbine generators.

1254
Maria Polikarpova was born in 1985 in Severodvinsk, Russia, received the Specialist Degree in Industrial Heat and Power from Saint-Petersburg Technological University of Plant Polymers, Russia in 2008 and Master of Science (M.Sc.) degree from Lappeenranta University of Technology (LUT), Finland in 2009. She is currently the PhD student in the Department of Electrical Engineering in LUT, where she studies heat transfer processes and cooling systems of electric motors and electric drives.

Juha Pyrhonen received the D.Sc. degree from Lappeenranta University of Technology (LUT), Finland in 1991. He became an Associate Professor of Electrical Engineering at LUT in 1993 and a Professor of Electrical Machines and Drives in 1997. He is currently the Head of the Department of Electrical Engineering, LUT, Finland, where he is engaged in research and development of electric motors and electric drives. His current interests include different synchronous machines and drives, induction motors and drives and solid-rotor high-speed induction machines and drives.
Publication 5

Copyright © 2015. Reprinted with permission from the Institution of Engineering and Technology (IET).

This paper is a postprint of a paper submitted to and accepted for publication in IET Renewable Power Generation and is subject to Institution of Engineering and Technology Copyright. The copy of record is available at IET Digital Library.

Lightweight Stator Structure for a Large Diameter Direct-Drive Permanent Magnet Synchronous Generator Intended for Wind Turbines

R. Scott Semken, Charles Nutakor, Aki Mikkola
Department of Mechanical Engineering
Yulia Alexandrova
Department of Electrical Engineering
Lappeenranta University of Technology
Lappeenranta, Finland

ABSTRACT

A concept has been developed for a novel lightweight wheel structure intended for rotor and stator use in direct-drive wind turbine generators. It uses a slanted spoke and rim architecture to provide maximum static structural performance with minimum weight. A unique attribute of the structure is its use of layered sheet steel elements to form the spokes and rim. Friction between layers establishes structural integrity. The interaction between layers and the resulting increase in damping normal to the stack offers improved dynamic performance. A static structural analysis of a full-scale stator wheel structure for an 8 MW permanent magnet machine demonstrates structural effectiveness of the architecture. To understand vibration characteristics of the lightweight wheel structure, a quarter-scale prototype was built and an experimental modal analysis was carried out to measure actual radial vibration responses. This data was used to verify a numerical model, and there was good agreement between measured and predicted behaviors. Finally, a modal analysis was carried out for the full-scale stator wheel structure. The dynamic performance of the wheel is acceptable for the stator of the 8 MW DD-PMSG embodiment.

1 Introduction

The primary benefits associated with today’s Direct-Drive Permanent Magnet Synchronous Generators (DD-PMSGs) for large-scale wind turbine applications are improved reliability, lower maintenance, longer life, and improved overall output [01, 02]. However, a key limitation is excessive size and the corresponding greater weight and material costs. At and above the 5 MW level, DD-PMSGs based on traditional cooling concepts become too large to be economically viable [03]. The direct-drive annular generator used in the 7.6 MW Enercon E-126 wind turbine, for example, is 12 m in diameter with a stator diameter of more than 10 m [04]. The large rotor and stator wheel structures needed to support its electromagnetic forces are massive, and the E-126 is approximately 220 tonne [05, 06, 07, 08]. Floating, offshore wind turbines are especially susceptible to problems associated with generator mass, because movement of the floating platform can result in excitation of the generator that affects the air gap between stator and rotor. The mass of the generator for a floating wind turbine must be kept as small as possible [09]. To continue improving the economic performance of wind turbines for electric power transmission, generator diameters should be kept below 8 m, and generator masses should be below 100 tonne.

Combining permanent magnet (PM) excitation with an optimal PMSG topology is the first step in reducing generator mass and cost [10, 11]. However, several clever mechanical design approaches that promise further weight reductions have also been investigated for large low-speed direct drive machines. Spooner et al. and Mueller and McDonald proposed an ironless stator generator based on a spoked wheel structure [12, 13]. Without stator steel, the strong radial forces between the stator and rotor are eliminated and the wheel structures can be more lightweight. However, stator and rotor diameters must be made larger in the ironless concepts, which offsets the weight reduction advantage of their less robust wheel structures. Other designs propose positioning large-diameter multi-row roller bearings between stator and PM rotor to manage the resulting large radial magnetic forces [14]. Bearing reliability coupled with impractical stiffness requirements for the bearing supporting rings make this approach less feasible and expensive for large diameter PMSGs.

Engström and Lindgren introduced a generator design in which supporting steel rollers placed adjacent to the air gap eliminate the structural effects of magnetic radial loading. Mechanical spherical roller bearings provide support for the steel rollers. With a 9 m air-gap diameter, the prototype 4 MW NewGen weighs in just under 37 tonne [15]. The downside to this approach is an increase in complexity and a subsequent reduction in reliability. The steel wheels, which roll in large multiples of the generator’s 19 rpm design speed, will be subject to accelerated fatigue stress [16, 17]. Furthermore, the complex bearing system will excite numerous harmonic vibrations.

The first approach taken to minimize weight for the DD-PMSG described in this work was to minimize its diameter. Since power is proportional to rotating speed and torque, and since the rotating speed of a large direct-drive wind turbine generator is essentially fixed (10-15 rpm), the developed torque of the machine determines power level. Developed torque is the product of air-gap radius, active length, and tangential stress. For a PMSG, the electromagnetic interaction between the magnetic
fields provided by the magnet poles and the electrical current flowing through the windings determines the level of tangential stress. As reported by Semken et al. [18], taking advantage of the dramatically more effective heat removal made possible by direct liquid cooling of the copper conductors, a DD-PMSG can be designed with substantially higher linear current density in the windings, which results in substantially higher levels of tangential stress.

Developing higher tangential stress levels by increasing linear current density makes it possible to reduce air gap diameter resulting in a significantly smaller generator. Because the saturation properties of most core steels are similar, magnetic loading does not vary substantially from one machine to another. Therefore, it is possible to achieve equivalent air gap flux density using less magnet material with a smaller air gap diameter. This is particularly important for a PM generator, which is subject to the pricing volatility of the magnetic material [19].

On the other hand, electrical machines are subject to wide variations in electrical loading, and greater applied electrical loading leads to greater tangential stress levels. At higher levels of tangential stress, less copper material can be used in a smaller diameter machine at the cost of slightly reduced efficiency; a penalty offset by an improvement in partial load efficiency characteristics. As a result, for smaller air gap diameters and increased levels of tangential stress, the weight, and therefore the cost, of both the passive and active materials that make up the generator can be dramatically cut [20].

Once diameter has been minimized, the design of the wheel structure for its stator and rotor has the next biggest influence on generator mass. In a PMSG, the stator and rotor structures must work together to resist the large attraction forces that pull together the facing surfaces of the rotor and stator. Since air gap length must be kept small to optimize efficiency, the structures must hold radial deformations to an absolute minimum. The structures must also resist the large tangential electromotive forces produced by the generator. Furthermore, understanding dynamic performance is important. The dynamic behaviours of both the stator and rotor wheel structures must be determined and evaluated with respect to possible excitations. Particularly, as generator diameter becomes larger, dynamic response becomes more of an issue. For the structural designer, managing static structural deformation and dynamic performance while keeping internal stresses within safe limits are primary objectives.

Presented here is a novel concept for a lightweight rotor and stator wheel structure. It uses a spokeed-wheel architecture; however, the spokes do not radiate outward normal to the hub. Instead, they are slanted and come out from the hub tangentially. This orientation enables the wheel structures to provide maximum static structural performance with minimum weight when the wheel rim is subjected to both tangential and radial forces.

To verify the static mechanical performance of the proposed concept, a static structural analysis of the stator structure was carried out using magnetic and torsion forces predicted via electromagnetic analysis. To determine vibration characteristics, a numerical model for modal analysis was developed and experimentally verified.

The actual size of the proposed structure prohibited building a full-scale prototype. Therefore, a quarter-scale prototype was designed and built. Next, a numerical model of the prototype was developed and a modal analysis was carried out to evaluate dynamic responses. Comparing the measured vibration characteristics of the prototype against the predicted modal responses served to verify the numerical model and the modelling approach.

Finally, a similar modal analysis was carried out for a full-scale stator structure. The agreement seen between the predicted and measured responses for the quarter-scale prototype gives credence to the predicted vibration characteristics of the full-scale structure.

2 Conceptual Design of Lightweight Stator Structure

The subject 8 MW liquid-cooled (LC) DD-PMSG has a 6.5 m air gap diameter and an active length of 1.1 m. Overall, it is 7 m in diameter and approximately 85 tonne.

The novel slanted-spoke structural wheel architecture is applied for both the stator and rotor. The spokes of the inner stator slant in the direction of the tangential forces acting on the outer diameter surfaces. Applying radial magnetic forces causes the outer rim of the wheel structure to deform outward. Applying tangential forces pulls the rim back into position. Figure 1 illustrates. In the same way, slanting the spokes of the outer rotor away from the tangential forces pulls its outer diameter outward in opposition to the inward pull of the magnetic forces, which again results in less overall radial deformation.

Another unique and important attribute of the slanted-spoke structural wheel architecture is its use of layered sheet-steel elements to form the spokes and rim of the wheel faces. When appropriately bound, friction between the layered sheet-steel elements establishes structural integrity with a substantial increase in structural damping perpendicular to the stack. In addition, with the stacked sheet-metal approach, substantial spoke-and-rim wheel structures can be built up without welding together overly thick steel elements. Eliminating deep structural welds
reduces manufacturing cost and eliminates problems associated with fatigue cracking and failure of welded connections. Fatigue cracking is a serious problem for a large structure with a 30-year design life that is subjected to continual flexing and extreme temperatures [21].

The electromagnetic machine is based on the use of duplex-helical, double-layer, non-overlapping tooth-coil windings. 144 tooth-coil windings are arrayed around the outer perimeter of the stator wheel in 12 segments. Cooling liquid is fed into each coil via the array of 12 cooling manifolds, which also receive the return flow. Bearings are positioned on each side of a fixed hollow stator support axle to accommodate the outer rotor (not shown). The 12 segments of the stator are fixed to the stator wheel via crossing tubes that pass through the electrical steel laminations.

Figure 2 shows the bare stator wheel structure. The layered sheet-steel elements are arranged and stacked to form a wheel face with slanted spokes and a rim. For the full-scale 8 MW LC DD-PMSG, a single sheet element is 5 mm thick. A wheel face bolts onto each side of two inner hub plates resulting in a pair of wheel structure sides. Spacers sit between the wheel faces in line with the spokes to maintain the spacing established by the hub. For this embodiment, a polymer material is envisioned for the spacers (e.g., high density polyethylene) to introduce structural damping in the axial direction. Next, a circular array of crossing tubes connects the two wheel sides to form the complete lightweight wheel structure, which is stiffened axially with internal cross bracing.

3 Radial Forces, Tangential Forces, and Excitation Frequency

The dominant forces acting upon the stator and rotor of a large PMSG are the electromagnetic forces, which act tangentially and radially across the air gap. The stator and rotor structures must resist the greater magnetic attraction forces and the lesser tangential forces. The PM flux of the rotor acting on the different relative permeabilities of the air gap and the stator electrical steel laminations produces the radial forces. The tangential stress is mainly the Lorentz force resulting from the interaction between the PM flux and the current passing through the stator conductors.

$$\mathbf{F} = q (\mathbf{E} + \mathbf{v} \times \mathbf{B})$$

(1)

In Equation (1), force \( \mathbf{F} \) develops as a particle charge \( q \) moves with velocity \( \mathbf{v} \) in an electric field \( \mathbf{E} \) and a magnetic field \( \mathbf{B} \).

An electromagnetic Finite Element Analysis (FEA) was carried out using CEDRAT Flux 2D to determine the electromagnetic forces for a 30° segment of an 8 MW LC DD-PMSG. Table 1 shows the input parameters used in the electromagnetic analysis.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Parameter</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power [MW]</td>
<td>Air gap diameter [m]</td>
</tr>
<tr>
<td>8</td>
<td>6.9</td>
</tr>
<tr>
<td>Speed [rpm]</td>
<td>Number of stator slots</td>
</tr>
<tr>
<td>11</td>
<td>144</td>
</tr>
<tr>
<td>Line-to-line voltage [kV]</td>
<td>Number of rotor poles</td>
</tr>
<tr>
<td>3.3</td>
<td>120</td>
</tr>
<tr>
<td>Stator current [A]</td>
<td>Conductors per slot</td>
</tr>
<tr>
<td>1060</td>
<td>20</td>
</tr>
<tr>
<td>Number of phases</td>
<td>Electrical efficiency [%]</td>
</tr>
<tr>
<td>6</td>
<td>93</td>
</tr>
<tr>
<td>Stator length [m]</td>
<td></td>
</tr>
<tr>
<td>1.15</td>
<td></td>
</tr>
</tbody>
</table>

The outputs of the FEA analysis were the resultant radial and tangential components of flux density under rated load in the air gap as a function of rotor position.

Applying the Maxwell stress method described by Pyrhönen et al. [22], the local distribution of radial (\( \sigma_r \)) and tangential (\( \sigma_t \)) force densities (N/m²) in the air gap can be expressed as shown by Equations (4) and (5). In the equations, \( B_r \) and \( B_t \) are the radial and tangential components of force density in the air gap, and \( \mu_0 \) is the permeability of air.

$$\sigma_r = \frac{1}{2\mu_0} (B_r^2 - B_t^2)$$

(2)

$$\sigma_t = \frac{1}{\mu_0} B_r B_t$$

(3)

Applying the Maxwell stress method described by Pyrhönen et al. [22], the local distribution of radial (\( \sigma_r \)) and tangential (\( \sigma_t \)) force densities (N/m²) in the air gap
can be expressed as shown by Equations (4) and (5). In the equations, $B_r$ and $B_t$ are the radial and tangential components of force density in the air gap, and $\mu_0$ is the permeability of air.

$$\sigma_r = \frac{1}{2\mu_0} (B_r^2 - B_t^2) \quad (4)$$

$$\sigma_t = \frac{1}{\mu_0} B_t B_r \quad (5)$$

Figure 3 illustrates the radial and tangential force density distribution along the air gap as calculated by the equations. The force density distribution repeats itself every 5th pole or every 15° corresponding to the rotational symmetry associated with the machine’s 144 slot and 120 pole configuration. There is significant variation in the force density distribution. This variation is a product of the slotted geometry of the stator electrical steel laminations and the discrete positioning of the winding conductors. The peak values calculated for both the radial and tangential force density components are roughly equal.

Assuming that both components of force density are distributed across the ten magnetic poles of the segment, the actual two-dimensional electromagnetic force can be obtained as a line integral along the air gap. Using the radius at the middle of the air gap ($r_s$) for the integration path, tangential and radial force as a function of angular position is defined by Equations (6) and (7).

$$F_r(\theta_r) = I_s \int_{0}^{2\pi} \sigma_r r_s \cos \theta \, d\theta \quad (6)$$

$$F_t(\theta_t) = I_s \int_{0}^{2\pi} \sigma_t r_s \cos \theta \, d\theta \quad (7)$$

In the equations, $F_r$ and $F_t$ are the radial and tangential components of force, $I_s$ is the effective stator stack
length, $\alpha$ is the angle in the cylindrical coordinate system, and $\theta_i$ is rotor position.

The radial and tangential electromagnetic forces at rated load calculated from these equations using the Maxwell stress method for the radial and tangential forces averaged over a single 30° segment are $F_r = 492.5\, \text{kN}$ and $F_t = 169.3\, \text{kN}$, respectively.

Static loadings during phase-to-neutral and phase-to-phase short-circuit fault conditions were also investigated for the subject LC DD-PMSG. Electromagnetic forces were calculated using the Maxwell stress method. Respectively, the corresponding maximum radial forces per segment are $F_{r,\text{max}} = 396\, \text{kN}$ and $F_{t,\text{max}} = 437\, \text{kN}$. The maximum tangential forces per segment are $F_{t,\text{max}} = 57.2\, \text{kN}$ and $F_{r,\text{max}} = 161\, \text{kN}$. For the LC DD-PMSG design, the maximum values of these force components are below those calculated for rated load.

There is circumferential variation in both the radial and tangential forces. These circumferential variations are excitations that can induce vibration during operation. To ensure that vibration does not become a problem, the natural radial and torsional vibration frequencies of the generator wheel structures need to be about 25% higher than any existing radial or torsional excitation frequencies.

If the rotor speed of a synchronous machine is $n_m$, and the rotor has the number of poles $P$, then the fundamental frequency of the machine in hertz $f_e$ can be expressed as follows [23].

$$f_e = \frac{n_m P}{120} \quad (5)$$

The rotor of the proposed LC DD-PMSG has 120 magnetic poles and the rated rotor speed is 11 rpm, so the fundamental electrical frequency is 11 Hz.

There are two frequencies of excitation relevant to the vibration responses of the LC DD-PMSG wheel structures. One of these is the mechanical result of the 120 magnetic poles of the rotor pulling radially on the stator teeth as they pass by 11 times per minute. This radial excitation frequency is 22 Hz (120 \cdot 11 / 60). The second excitation is torsional. Torque cogging is produced at the sixth harmonic of the fundamental machine frequency, so the torsional excitation frequency is 66 Hz (11 \cdot 6) [24].

Therefore, for optimum dynamic performance, the natural radial vibration frequencies of the full-scale generator wheel structures should be greater than 27.5 Hz, which is 25% above 22 Hz, and the natural torsional vibration frequencies should be greater than 82.5 Hz or 25% above 66 Hz.

### 4 Static Structural Analysis of Full-Scale Lightweight Stator Structure

To verify the structural performance of the proposed wheel, a static structural analysis was carried out using ANSYS® Workbench™ v15.0. The model was constructed in SolidWorks® 2013 SP4.0, and then imported into ANSYS®. A fixed constraint was applied to the inner surfaces of the axle, and the predicted radial and tangential forces were applied. The geometry of the wheel structure was simplified for computational efficiency.

The 12 circumferentially arrayed bodies penetrated by crossing tubes represent idealized stator segments (electrical steel lamination and tooth coils). The magnetic attraction force of 492.5 kN per segment was applied as a 0.254 MPa pressure to the outer diameter surface of each body. The tangential force of 169.3 kN was applied as 50.01 N/mm line pressure to each of the outer circumferential edges. With respect to the figure, the magnetic force pulls outward on the outer diameter surface, and the tangential forces pull in the clockwise direction.

The FEA-model of the wheel structure was meshed with tetrahedral elements using program-controlled course meshing. Convergence was ensured by repeating the analysis with finer mesh sizes. The specific solid elements included SOLID186 and SOLID187. Surface-to-surface contact between elements was defined using CONTA174 and TARGE170. There were 494,039 elements with 3,024,795 degrees of freedom.

Material properties were assumed linearly elastic. Table 2 summarizes the relevant material properties used for the model components.

<table>
<thead>
<tr>
<th>Material</th>
<th>Young's modulus (MPa)</th>
<th>Density (kg/m³)</th>
<th>Poisson's Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>structural steel</td>
<td>200000</td>
<td>7850</td>
<td>0.3</td>
</tr>
<tr>
<td>polyethylene</td>
<td>1100</td>
<td>950</td>
<td>0.42</td>
</tr>
</tbody>
</table>

As mentioned previously, the key aim for the structure in terms of static mechanical performance is to keep radial deformation to an absolute minimum. Figure 4 illustrates the radial and circumferential deformations predicted for the lightweight stator structure when subjected to the outward magnetic forces and the clockwise tangential forces.

The design principle depends on the slanted spokes pulling the rim inwards in response to the tangential forces to counteract the radial deformation outward caused by the magnetic forces. As the FEA results illustrate, the structure is performing as expected. In the
left image, the 2.4 mm deformation (orange) of the spoke region near the rim corresponds to the spokes moving inward by about 0.2 mm. Although the cross tubes are not shown, they are deforming outwards by about 0.2 mm in response to the pull of the magnetic forces. As a result, the maximum outward radial deformation, which occurs on the outer diameter of the stator segment body, is shown to be a negligible 0.04 mm.

The outer diameter of the stator moves 2.6 mm circumferentially in the clockwise direction (image on the right in the figure). This movement produces the inward deformation of the rim. The relative values of radial and circumferential deformation can be adjusted (by design) by varying spoke, rim, and cross tube geometries, which makes it possible to tune the structure to achieve the desired deformation performance.

Figure 5 illustrates the equivalent stress (Von Mises stress) fields determined for the stator wheel structure. The maximum predicted stress of 99.6 MPa occurs in the fillet where the axle and wheel flanges meet as shown in the image on the right in the figure. This stress could be reduced further by increasing the fillet radius. Elsewhere in the structure, the stresses are relatively low.

The lightweight stator wheel structure is designed to accommodate the application of radial and tangential forces reversed. The maximum deformations predicted were 0.34 mm outward radial and 3.1 mm clockwise circumferential. The predicted maximum equivalent stress (also at the flange axle interface) was 100 MPa.

While not ideal for continuous operation, these values indicate that no damage will occur if the generator is run in reverse.

5 Vibration Characteristics of 1/4-Scale Wheel Structure: Predicted vs Measured

A first step towards understanding the dynamic performance of the proposed lightweight stator wheel structure is to determine the normal modes and natural frequencies that define its vibration characteristics. A simplified numerical model is needed that can accurately predict these patterns of motion, because the complex nature of the nonlinear interaction between layers in the structure cannot be readily modelled. However, before this kind of numerical model can be applied with confidence, it must be verified, and a good way to do this is to compare predicted with measured results.

For this purpose, a quarter-scale layered-sheet-steel wheel structure was designed, fabricated, and assembled. Its radial vibration characteristics were measured using a Polytec Scanning Laser Doppler Vibrometer (SLDV). Next, a simplified numerical model of the quarter-scale structure was developed, and a modal analysis was carried out using ANSYS® Workbench™ v15.0.

Quarter-Scale Wheel-Structure Prototype

The wheel-structure prototype comprises an axle with wheel hub flanges on either end, two wheel-face pairs bolted to either side of the two hub flanges, and a circular array of 144 threaded rods to simulate the structure’s crossing tubes and connect the rims of the near and far
side wheel-face pairs. Each of the four wheel-faces consists of five layers of 12 laser cut sheet-steel elements laid out in a circular array. Two of the sheet-steel elements are shown in the middle photo of the figure. There are 60 total elements in each wheel face (240 total for 4 faces), and each is 6.2 mm total thickness. Every layer of arrayed sheet-steel elements is oriented 15° from the previous, so the seams between elements are staggered (right-side photo).

The spoke layers and spacers are bound together using nylon cable ties. There are also metal spacers (not shown) on the crossing threaded rods to maintain spacing of the wheel-face pair at the rim. Interior braces cross diagonally from the near to far sides of the wheel structure for axial rigidity. Each pair of braces is tensioned by a Sorbothane® elastomeric damper inserted between and at the intersection of the pair. The elastomer also serves effectively to dampen brace vibrations. The completed quarter-scale wheel structure measured 1564 mm in diameter by 442 mm wide. Total weight was 375 kg.

Experimental Modal Analysis

Experimental Modal Analysis (EMA) can determine the actual vibration characteristics of a linear and time invariant structure. The output of EMA is a modal model comprising natural frequencies, modal damping ratios, and mode shapes. From the experimentally derived modal model, constituents such as mass, damping, and stiffness can be extracted. A typical EMA setup comprises four main components: 1) the structure being evaluated, 2) an excitation system to provide a measurable input force function, 3) a transducer to transform mechanical motion into an electrical signal, and 4) an analyser to carry out the measurement and signal processing tasks. Figure 6 is a photo image of the setup used to make the radial SLDV measurements.

On the left in the figure, a Bruel & Kjaer 4814 modal exciter connects to a bar that is clamped to a pair of the wheel structure’s crossing rods. The relatively stiff clamp bar helps to direct excitation away from the threaded rods and into the wheel sides. Mounted on a tripod at the far right of the photo is the transducer, a Polytec PSV-500 scanning vibrometer. The analyser, a Polytec OFV-5000 vibrometer controller, sits in the rack behind the transducer along with the system’s processing unit.

The EMA system was configured to measure the vibrational velocity and displacement of a vertical array of programmed target points covering an arc section of the near and far side rims. Both the exciter and the wheel structure are hung from above to simulate a free-free constraint. A pair of ropes attached to the bottom of the wheel (one is visible at the bottom right in the figure) were added to keep the structure from swinging and rotating.

With the PSV-500 positioned as shown in the figure, it was not possible to target the full circumference of the wheel structure. The twin vertical arrays of target points covered only the 110° arc section visible to the vibrometer. Without measuring target points for the full circumference, the SLDV system cannot determine the
global mode shapes of the wheel structure. However, measuring the movement of the target points across the visible 110° arc yields the frequency response data needed to carry out the necessary comparison between predicted and measured motions to verify the numerical model.

The modal exciter pulsed the wheel structure with a pseudo-random signal, within the excitation frequency range of 0-to-800 Hz. Ninety-five scanning points were measured using a complex average of three, and the sampling rate for the measurement was 2 kHz with 1600 fast Fourier transfer lines.

**EMA Results for Prototype**

The SLDV measurement procedure and subsequent data processing produced a Frequency Response Function (FRF) plot relating velocity magnitude to frequency. By comparing the plot with the numerically predicted modal results, modal shapes can be associated with the peaks of the FRF plot.

Figure 7 shows the relevant range of frequencies of the FRF plot for the radial SLDV measurements. The first five radial vibration mode peaks have been tagged in the plot. They occur at 270, 291, 304, 318, and 332 Hz, respectively.

**Numerical Model of Prototype and Predicted Modal Results**

A simplified model of the prototype wheel structure was developed to carry out a modal analysis and predict the vibration characteristics of the quarter-scale layered sheet-metal wheel structure.

The two layered wheel-face pairs, comprising the slanted spokes and wheel rims, were meshed as SHELL181 elements. All other structural components were meshed with SOLID186 and SOLID187 elements. The FE model consists of 211,181 total elements with 2,336,571 degrees of freedom. To facilitate the connection of these element types and varying degrees of freedom, a bonded contact with a Multi-Point Constraint (MPC) formulation is used. The MPC formulation helps to couple translational DOF from the solid surfaces to the rotational DOF of the shell edges. A free-free boundary constraint and surface-to-surface contact elements, were implemented on the solid element interface [25].

In a modal analysis, material and geometric nonlinearities are difficult to model, so nonlinear effects resulting from, for example, fastening methods were ignored. In addition, the elastomeric dampers used to tension and dampen the cross bracing were not modelled. Finally, linear elastic behaviour was assumed for the wheel structure materials.

The analysis options were set to discover up to 600 modes between 20 and 500 Hz with the solver type controlled by the program. The same linearly elastic material properties that were used previously in the static structural analysis were used here. Refer to Table 2. The modal analysis solution revealed four distinct radial vibration modes at 301, 310, 320, and 333 Hz, respectively. Their shapes correspond to radial modes 2, 3, 4, and 5 [25].

**Predicted versus Measured Modal Results**

Table 3 compares the numerically predicted frequencies for radial modes 2 through 5 with the frequencies identified for the same modes in the FRF plot.
numbers show, the agreement between the predicted and measured values is good.

Table 3  Summary of modal frequency results comparing predicted versus measured values

<table>
<thead>
<tr>
<th>Mode</th>
<th>Predicted Frequency (Hz)</th>
<th>Measured Frequency (Hz)</th>
<th>Difference (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2</td>
<td>301</td>
<td>291</td>
<td>3.3</td>
</tr>
<tr>
<td>3</td>
<td>310</td>
<td>304</td>
<td>1.7</td>
</tr>
<tr>
<td>4</td>
<td>320</td>
<td>318</td>
<td>0.3</td>
</tr>
<tr>
<td>5</td>
<td>333</td>
<td>332</td>
<td>0.3</td>
</tr>
</tbody>
</table>

The similarity between the predicted and measured modal analysis results for the quarter-scale prototype serves to verify the numerical model and suggests that predictions based on a similar numerical model for the full-scale structure should yield accurate predictions.

6 Modes and Frequencies Predicted for Full-Scale Stator Wheel

To examine the vibration characteristics of the full-scale stator wheel, the FEA model previously developed for static structural analysis was simplified to eliminate the nonlinear geometric features not readily accounted for in modal analysis. The FE model of the full scale wheel structure was meshed using solid tetrahedral elements with 1,008,265 nodal degrees of freedom. Contact between elements was handled as in the quarter-scale modal analysis. The analysis options were set to discover up to 450 modes between 5 and 5000 Hz with the solver type controlled by the program. The same linearly elastic material properties that were in the previous analyses were used here. Refer to Table 2.

The modal analysis solution did not predict any torsional vibration modes for the full-scale stator wheel. However, it did identify radial vibration modes 2, 3, 4, and 5 at 111, 129, 150, and 177 Hz, respectively. Figure 8 shows the graphical presentation of these radial modes. As previously discussed, with a radial excitation frequency of 22 Hz, the minimum radial modal frequency should be greater than 27.5 Hz. Although the solution did not identify the first radial mode, based on mode 2, which occurs at 111 Hz, there seems to be a substantial margin of safety.

7 Conclusions

A novel concept was introduced for a lightweight wheel structure intended for the rotor and stator of a large DD-PMSG. The concept is a variation on the traditional spoked-wheel architecture that uses slanted-spokes to control radial deformation as the rim is subjected to combined radial and tangential forces. The sides of the wheel structure comprise layered sheet-steel elements. Each layer consists of identical elements arranged in a circular array to form the spokes and rim. Steel cross tubes span the gap between wheel sides and provide attachment and support for the active electromagnetic materials.

The geometry of the wheel structure results in an exceptionally low mass. The layering of the sheet-steel elements introduces additional damping to the structure, especially in the axial direction. Moreover, the deep structural welds that would normally be needed to build up a wheel structure of this size are not required with the layered sheet-steel architecture. As a result, issues with fatigue cracking and failure at the welds over the life expectancy of the structure are eliminated.

A detailed conceptual model for a large 8 MW LC DD-PMSG was introduced to illustrate one possible embodiment of the new lightweight wheel structure concept. The 8 MW machine has an air gap diameter of 6.5 m and an active length of 1.1 m. Overall, the generator embodiment is 7 m in diameter and approximately 85 tonne. Electromagnetic analysis determined the approximate radial and tangential electromagnetic forces that develop in the air gap. The total radial force predicted was 5910 kN. The total predicted tangential force was 2032 kN.

The rotor of the proposed LC DD-PMSG has 120 magnetic poles, and the rated rotor speed is 11 rpm. This results in a radial excitation frequency of 22 Hz as the 120 magnetic poles of the rotor pull radially on the stator teeth.
Figure 8  Radial vibration modes 2 through 5 and their respective frequencies (111, 129, 150, and 177 Hz) predicted for the full-scale stator wheel each time they pass by at a rate of 11 times per minute. Therefore, for the full-scale machine, the natural radial vibration frequencies of the generator wheel structures should be greater than 27.5 Hz to give sufficient margin of safety.

A static structural analysis was carried out on a simplified structural model representing the proposed conceptual embodiment of the stator. The predicted radial and tangential forces were applied as loads to the FEA model. According to the analysis, the structure performs as expected. Total maximum outward radial deformation was shown to be a negligible 0.04 mm. Maximum torsional deformation was 2.6 mm, and maximum predicted equivalent stress was 99.6 MPa.

A quarter-scale wheel structure was built so its vibration characteristics could be measured and compared against characteristics predicted by an ANSYS numerical modal analysis. The vibration characteristics of the prototype were measured via experimental modal analysis using a scanning laser Doppler vibrometer. Radial vibration modes 2 through 5 were observed. Modal analysis of the numerical model also identified radial vibration modes 2 through 5. There was excellent agreement between the measured and predicted frequencies for these vibration modes, suggesting that the numerical approach yields a good approximation of actual dynamic behaviour.

Finally, a modal analysis was carried out on a similar numerical model of a full-scale stator wheel structure. The same four radial vibration modes (2-5) were predicted at frequencies of 111, 129, 150, and 177 Hz, respectively. Since the target minimum radial vibration frequency is 27.5 Hz and the predicted second modal frequency is 111 Hz, resonant vibration will be avoided. Dynamic performance seems to be acceptable for the stator of the 8 MW LC DD-PMSG embodiment based on the introduced lightweight, layered, wheel structure.

The modal analyses carried out here revealed that most of the low frequency vibration modes are axial. The increased damping due to the layered sheet-steel architecture also is in this direction. Although there is theoretically no axial excitation, further work should be carried out to postulate any possible deleterious effects of the axial vibration frequencies and identify possible solutions. In addition, it would be interesting to experimentally verify the results of the static structural analysis.

Acknowledgements

The authors would like to thank the Department of Mechanical Engineering at the Lappeenranta University of Technology and the European Commission’s European Regional Development Fund for their funding of the work reported here.
References


Publication 6
Copyright © 2014, IEEE. Reprinted with permission from IEEE.

Abstract -- The power of wind turbine is steadily increasing and nowadays machine powers up to 10-20 MW and their possible realizations are discussed. At the moment wind turbines are available up to 8 MW powers. The biggest generators are planned to be installed in offshore applications. More powerful and reliable generators must be designed to be used together with them. Therefore, direct drives are considered. The development of compact high-power direct drive wind turbine generators necessitates design of more effective cooling system to ensure their safe operation. The focus of this paper is in the steady-state thermal analysis of 8 MW DD PMSG generator with direct water cooling of the stator winding based on Lumped-Parameter Network (LPN) and Finite Element Analysis (FEA). Thermal design of the generator based Computational Fluid Dynamics (CFD) is also presented here to evaluate the influence of passive air cooling on the rotor.

Index Terms-- electrical machines, wind turbine generator, direct water cooling system, thermal analysis.

I. INTRODUCTION

Wind farms have become ordinary sources of electric energy. However, the rated powers of up-to-date wind generators seldom exceed 4 MW. Turbine producers are searching ways to maximize the power density to reduce the energy cost. The modern inventions in this direction include gearless drive trains and utilization of permanent magnets to achieve high reliability, efficiency and simple rotor construction [1]. Even high temperature superconductors (HTS) are suggested to lessen the generator weight and increase efficiency. HTS based electro magnets are, however, expensive because of their very low operation temperature (20-55K), which is related with difficulties in the cooling system [2,3]. Direct drive permanent magnet synchronous generators (DD PMSG) for wind farms with the rated power slightly more than 4 MW are available on the market, but these machines are heavy and enormous to compete with installations based on conventional generators and gearboxes. As the torque, and in this case power, production in a machine is directly proportional to the air gap Maxwell stress i.e. proportional to the product of the flux density and the linear current density, then an obvious way to reduce the weight of PMSG is to increase the linear current density by increasing the current density in the stator winding. This causes high heat losses in the winding, which can be dangerous for both the stator coil insulation and for the rotor surface permanent magnets with temperature sensitive properties.

Traditional air cooling system is no more applicable in cases of high Maxwell stress generators as it does not allow removing the generated heat losses and ensuring proper operation of the rotor mounted permanent magnets. Hence in case of high current density (5-10 A/mm²) direct water cooling system of the stator winding becomes useful solution, as it can provide an adequate temperature of the winding and the permanent magnets. The operating magnet temperature must in all cases be lower 120-150 °C for NdFeB. In practice, if the generator rotor must tolerate also the possible short circuit and therefore the magnet temperature must be normally, depending on the material selection, be even less then 100°C. In an offshore site, closed water cooling systems can help the internal components of the wind turbine nacelle to avoid the maritime air moisture and corrosion atmosphere inside the generator. However the direct water cooling system complicates the generator design and therefore its design requires special attention. The main failure causes of the cooling system are leaks in the clip-to-strand connections due to the crevice-corrosion mechanism, loose seal and copper erosion [4].

The main interest of this work consists of the analysis of the DD PMSG thermal behavior with internal cooling system of the stator copper winding. The description and evaluation of the direct water cooling system is based on analytical and FEM simulations. The thermal designs of the studied generator are simulated based on lumped parameter network, 3D FEM and CFD. The LPN is the most used method, as it does not consume much time and computational resources [19]. The finite–element softwares are used to give a detailed temperature distribution within the studied generator.

II. DESCRIPTION OF GENERATOR COOLING SYSTEM

A. A Studied Generator

The machine is a low-speed, concentrated non-overlapping tooth coil winding, three-phase synchronous generator with rated power 8 MW. The construction of the rotor is relatively simple, as permanent magnets are located on the surface of its outer diameter. Onshore and offshore wind farms are applications of such direct-drive generator. The rated speed, torque and line-to-line voltage are 11.8 rpm, 6.478 MNm, and 787 V respectively. The rated operated frequency is 12 Hz, and therefore copper losses in
the stator winding are the dominating ones. The studied generator consists of the machine segments having 10 poles and 12 slots each. In total the generator has 144 stator slots, 60 pole pairs. Table 1 provides the geometrical data of the studied generator.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Quantity</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator Outer Diameter</td>
<td>7600 mm</td>
</tr>
<tr>
<td>Air Gap Diameter</td>
<td>5900 mm</td>
</tr>
<tr>
<td>Length of Air Gap</td>
<td>7 mm</td>
</tr>
<tr>
<td>Length of Stator</td>
<td>1700 mm</td>
</tr>
<tr>
<td>Magnet Width x Height</td>
<td>133 x 30 mm²</td>
</tr>
<tr>
<td>Slot Width x Height</td>
<td>63 x 122 mm²</td>
</tr>
</tbody>
</table>

The total pressure losses are determined by the pressure losses along the conductors’ length and in the fittings.

The above mentioned problem of high ohmic losses is solved through the direct water cooling of the stator winding. Because of the eddy currents and the hysteresis, the iron losses appear in the machine. The heat generation distributes unevenly among the conductors in the stator slot and despite the low frequency some skin-effect exists in copper. Table 2 lists the heat sources in the studied generator.

<table>
<thead>
<tr>
<th>Heat Source Position</th>
<th>Quantity</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator Copper Windings</td>
<td>425 kW</td>
</tr>
<tr>
<td>Stator Core</td>
<td>14.5 kW</td>
</tr>
<tr>
<td>Rotor Core</td>
<td>1.5 kW</td>
</tr>
<tr>
<td>Permanent Magnets</td>
<td>23 kW</td>
</tr>
<tr>
<td>Additional</td>
<td>40 kW</td>
</tr>
</tbody>
</table>

C. Cooling System Introduction

The above mentioned problem of high ohmic losses is solved through the direct water cooling of the stator winding and passive air cooling of rotor. The stator winding is designed as hollow rectangles copper conductors 15 x 15 mm with extruded tubes 6 x 0.4 mm inside them. Use of tube inside the copper conductor allows improving the cooling system performance and reliability, as higher velocity of water flow can be adopted and simpler clip-to-strand connections are used.

Direct-water cooling system are designed to remove the heat losses of the stator copper winding and steel frame. The cooling system consists of 144 parallel cooling circuits which ensure uniform temperature along the copper conductor surface. Each cooling circuit contains 24 tubes connected in series. Fig. 1 presents the temperature increase and pressure losses in a coil of 45 meters total length having demineralised water flowing along the cooling circuit.

The inlet temperature of demineralized water is 40°C and the flow rate is 1 m/s. The outlet temperature of demineralized water is up to 80-90°C to prevent the corrosion of tube [5]. Such parameters of cooling system allow to keep the temperature of the stator winding lower than 90°C and permanent magnets even in less than 60 °C.

The total pressure losses of the cooling demineralized water is determined by the inlet temperature of the demineralized water and the cross-section of the copper conductor in the conditions of the constant heat rate [6].

\[
T_{dw} = T_{dw0} + \frac{Q}{\varepsilon_{pdw} S_{hole} \rho_{dw} U_{dw}}
\]

where \( T_{dw0} \), \( T_{dw} \) are the temperatures of inlet and outlet demineralized water flows, \( Q \) is the heat rate, \( \varepsilon_{pdw} \) is the heat capacity of the demineralized water, \( \rho_{dw} \) is the density of the demineralized water, \( U_{dw} \) is the velocity of the demineralized water, \( S_{hole} \) is the cross-section area of the conductor hole.

Temperature to which the copper conductor can be cooled is mainly determined by the convective heat transfer coefficient and the heat exchange rate.

\[
T_s = T_{dw} + \frac{Q}{\pi D_{hole} \varepsilon c h_{dw/c}}
\]

where \( T_s \) is the temperature of the internal conductor surface, \( D_{hole} \) is the inner diameter of the extruded tube, \( \varepsilon \) is the conduciveness of the demineralized water and the tube extruded in the conductor.

The convection heat transfer is assumed from the definition of the Nusselt number by Gnielinski correlation [6].

\[
Nu = \frac{h_{dw/c} D_{hole}}{k_{dw}}
\]

\[
Nu = \frac{\varepsilon}{8} \left( \frac{Re - 1000}{Pr} \right) \left( \frac{Pr}{\Pr^3 - 1} \right)
\]

\[
\varepsilon = 0.11 \left( \frac{k_{dw}}{D_{hole}} \right)^{1/4} \left( \frac{68}{Re} \right)^{1/4}
\]

where \( Nu \) is the Nusselt number, \( k_{dw} \) is the thermal conducance of the demineralized water, \( \varepsilon \) is the friction factor, \( Re \) is the Reynolds number, \( Pr \) is the Prandl number, \( \kappa \) is the absolute value of average tube surface roughness.

The total pressure losses are determined by the pressure losses along the conductors’ length and in the fittings.
(bends, inlet and outlet of the conductors’ junction / cooling circuit) \[6\].

\[ P_{\text{loss}} = \rho \frac{k}{2} \frac{U}{d} \left( \frac{2}{2} \right) \left( \sum k + \frac{k}{D_{\text{hole}}} \right) \]  

where \( \Sigma k \) is the sum of pressure losses coefficients in the fittings (bends, inlet and outlet).

For simulation of the outer conductors temperature in the stator slot the 3D model of the copper conductor is created by the FEM software (Fig.2). The volumetric copper losses are assumed within the copper conductor. The inlet velocity and the temperature of the demineralized water are assumed 1 m/s and 40 \(^\circ\)C as boundary conditions. As can be seen from the above figure, the outer temperature of the conductor differs from the demineralized water temperature lower than 1 \(^\circ\)C.

**D. Coolant for Internal Cooling System**

Water is the best coolant in many energy conversion applications, but limitations can arise, when it is used as a coolant in the cooling system of a wind turbine generator. At temperatures below 0\(^\circ\)C water freezes and expands that can result in failure in the cooling system. The freezing problem is solved through the mixing of the cooling water with antifreeze additives (chemical treatment). Fig. 2 present the comparison of Ethylene Glycol 50% Vol., Glykosol N 50% Vol., Pekasol N 50% Vol. and water as coolants for the direct cooling system of the stator copper winding. The values of density, thermal conductivity, dynamic viscosity, heat capacity and Prandl number were taken from Products Technical Data of these fluids [7-9].

![Fig. 1. Temperature of the inner conductor surface and pressure losses along one cooling circuit (analytical calculation).](image1)

For simulation purposes mesh with 57300 nodes, 122000 surface elements and 303000 volume elements was created with the generator model.

**III. DESCRIPTION OF THE SIMULATED GENERATOR THERMAL MODEL BASED ON FEM**

The FEM thermal analysis is achieved using the commercial FEM software the thermal Analysis™ – 3D Flux [10]. The simplified 3D – model of the generator (Fig. 1) represents only 1/288 part of the machine because of the machine symmetry. A small slice is selected to be able to generate the most dense mesh and therefore to achieve the most reliable results. The generator model comprises the stator frame, the stator yoke, the stator tooth, the slot wedge, the copper winding, the copper end-winding, the insulation, the rotor iron, the rotor mounted magnet and the air gap.

For the simulation purposes mesh with 57300 nodes, 122000 surface elements and 303000 volume elements was created with the generator model.

**A. Thermal Conductivities**

The thermal conductivities of the materials constituting
the different parts of the generator are simulated with uniform or non-uniform conduction in case of rotor iron, stator copper winding, stator tooth and yoke. Table 3 lists the thermal conductivities of the used materials in different directions [11,12].

The non-uniform conduction is associated with the composite structure of some parts. The stator tooth and yoke are split up into many laminations to reduce magnetic flux alternation caused eddy current losses in the magnetic circuit. The laminations are connected to each other by the lamination insulation surfaces and possible air in the middle whose thermal conductivities are quite low, which results in a poor thermal conductivity in the axial direction. The number of conductors in a slot is 24. Each conductor has extruded stainless steel tube with the demineralized water inside and it is impregnated by insulation whose thermal conductivity is only 0.26 W/Km. These design properties cause very poor conduction inside the slot both in the radial and the tangential directions.

**TABLE III**

<table>
<thead>
<tr>
<th>Material of Model Component</th>
<th>Thermal Conductivities, W/Km, Direction, cylinder coordinates</th>
</tr>
</thead>
<tbody>
<tr>
<td>Iron</td>
<td>39 39 443</td>
</tr>
<tr>
<td>Air in Air Gap (60°C)</td>
<td>0.2 0.2 0.2</td>
</tr>
<tr>
<td>Stator Copper End Winding</td>
<td>1.1 1.1 386</td>
</tr>
<tr>
<td>Aluminum</td>
<td>237 237 237</td>
</tr>
<tr>
<td>Permanent Magnets</td>
<td>9 9 9</td>
</tr>
<tr>
<td>Glass Fiber</td>
<td>0.3 0.3 0.3</td>
</tr>
<tr>
<td>Epoxy Resin</td>
<td>0.26 0.26 0.26</td>
</tr>
</tbody>
</table>

For prediction of the winding thermal conductivity the analytical calculation of two-phase solid-to-solid mixture of Maxwell can be used [14,15].

$$k_{eq} = \frac{k_{cu} \left( \frac{k_{cu}}{k_{ins}} + 2 \frac{k_{cu}}{k_{ins}} - 2 \frac{A_{sh}}{A_{cu}} \frac{(k_{cu} - k_{ins})}{k_{ins}} \right)}{\frac{k_{cu}}{k_{ins}} + \frac{k_{cu}}{k_{ins}} + \frac{A_{sh}}{A_{cu}} \frac{(k_{cu} - k_{ins})}{k_{ins}}}$$  

(7)

$$k = \frac{N \cdot S}{d}$$  

(8)

where $k_{eq}$ is the equivalent thermal conductivity of the winding, $k_{cu}$ - the thermal conductivity of copper, $k_{ins}$ - the thermal conductivity of insulation, $A_{sh}$ - the conductive surface area, $k_{sh}$ - the winding packing ratio, $N$ - the number of conductors in the slot, $A_{cu}$ - the cross-section of the conductor, $A_{sh}$ - the cross-section of the slot.

**B. Convection**

An accurate thermal model of the studied machine is simulated based on the empirical formulations of the convection coefficients. The air gap is defined as solid with higher thermal conductivity, than ordinary air has. The surfaces in the air gap can be considered smooth cylindrical. Then experimental results of Ball, Farouk and Dixit [13] are used to determine the effective thermal conductivity of air in the air gap. This parameter is defined as the thermal conductivity that the stationary air should have in order to transfer the same amount of heat as the moving air [11].

$$k_{ag} = 0.069 \cdot a^{-2.9084} \cdot \frac{0.4614 - \ln(3.3361 \cdot t)}{\Re_{ag}}$$  

(9)

$$\eta = \frac{\text{out}}{\text{inst}}$$  

(10)

$$\Re_{ag} = \frac{r_{out} \cdot \frac{L_{ag}}{60}}{v_{air}}$$  

(11)

where $k_{ag}$ is the effective thermal conductivity of air in the air gap, $\Re_{ag}$ - the Reynolds number, $r_{out}$ - the rotor outer radius, $L_{ag}$ - the stator inner radius, $L_{ag}$ - the length of air gap, $n$ - the synchronous speed of rotor, $v_{air}$ - the air kinematic viscosity.

The convection between the end parts and air is slightly intensified by the rotor rotation. The most complex task is the calculation of the convection coefficients in the end cap regions. The following empirical equations introduced by [16, 17] were used to define the forced convection on the side surfaces of stator, rotor.

$$h_{rotair} = 16.5 \cdot (U_{r})^{0.65}$$  

(12)

$$h_{stairair} = 15.5 \cdot (1 + 0.29 \cdot U_{stair})$$  

(13)

$$U_{stair} = \sqrt{(0.5 \cdot U_{r}^{3}) + U_{s}}$$  

(14)

where $h_{rotair}$ is the convective heat transfer coefficient between the rotor end surfaces and air, $h_{stairair}$ - the convective heat transfer coefficient between the stator end surfaces and air, $U_{end}$ - velocity of air in the endcap region, $U_{r}$ - velocity of the outer rotor surface, $U_{s}$ - air velocity in the axial direction of the air gap.

The convection outside the motor frame was defined as natural convection for air/frame interaction.
\[ Ra = \frac{g \beta D_{\text{out}} (T_s - T_{\text{amb}})}{v_{\text{air}} \alpha} \]  

(17)

where \( k_{\text{air}} \) is the thermal conductivity of air, \( k_i \) – the finned coefficient, \( N_{\text{Nu,amb}} \) - the Nusselt number, \( l_s \) - the length of stator frame, \( Ra \) - the Rayleigh number, \( Pr \) - the Prandtl number, \( g \) - the gravitation constant, \( \beta \) - the coefficient of thermal expansion \([1/K]\), \( D_{\text{out}} \) – the stator outer diameter, \( \alpha \) – the thermal diffusivity \([m^2/s]\), \( T_s \) and \( T_{\text{amb}} \) – the surface and ambient temperatures.

Fig. 4 shows the temperature field resulting from the FEM modeling in the 3D Flux. The temperature of the stator copper winding (80°C) and the convection coefficients on the end surfaces were defined as boundary conditions. The heat losses presented in Table 2 are imposed as heat sources of the simplified generator parts.

IV. CFD THERMAL DESIGN

CFD thermal design is very useful in case of fluid flow inside the machine parts. It allows obtaining temperature distribution within the machine without definition of convection heat transfer coefficient in the air-gap and air of the hollow support structure by empirical equations.

The simplified model of the studied generator presents 1/18 part of the machine. The model includes the stator yoke, the stator copper winding, the slot wedge, the air gap, the rotor iron, the permanent magnets, the support structure and the shaft. All machine parts are constructed as hollow cylinders. For the simulation purposes 61899 boundary elements and 203745 tetrahedral elements were created within the generator model. Figure 5 illustrates a numerical grid mesh with 38202 mesh points that was generated within the model.

The heat losses are generated in the stator yoke, stator winding, air gap, permanent magnets and rotor iron. The stator and rotor iron has non-uniform conductivities, because of their laminated structure. The stator copper winding presents composition of some insulated conductors and extruded in them stainless steel tubes with coolant flow, so its conductivity is also non-uniform.

The convective heat transfer coefficient on the end-back surfaces is defined based on the empirical equation (12-14) presented in the previous section. The air inlet velocity 0.001 m/s is considered, as boundary condition, in the inlet face of the air gap and the support structure. The stator copper winding has constant temperature 80°C. The rotor surfaces and shaft have rotation 11.8 rpm.

Fig. 6 illustrates the CFD thermal model of the studied generator. The CFD modeling was implemented by the modes Convection and Conduction, and of the commercial software Comsol.
The CFD thermal design allows obtaining the temperature distribution within the rotor, as it is impossible to implement by the FEM analysis.

V. THERMAL ANALYSIS BASED ON LPN

The thermal analysis using LPN is based on dividing the studied generator into several components – the frame, the stator yoke, the stator tooth, the stator copper coils, the air gap, the rotor yoke, the rotor embedded magnets, the support structure, the air in hollow support structure, the end-cap air and the shaft. The stator copper coil is divided into three regions – copper winding in slot, copper end-winding and coolant. All of the machine components are represented by isothermal nodes and assumed thermally symmetrical in the radial directions [16] to reduce their number. Each node is coupled with the neighbours by means of conduction and convection resistances. The heat flows propagate in the radial and axial directions. Fig. 7 shows an equivalent network of thermal resistances for the studied motor.

The convection and conduction resistances of the components are defined by the following equations [18] and presented in Table 4. The convection resistances mainly between fluid and solid regions.

\[
R_{\text{cond}} = \frac{1}{l \cdot k}
\]
\[
R_{\text{conv}} = \frac{1}{A \cdot h}
\]

where \( l \) is the length of the body in the heat flow direction, \( k \) - the thermal conductivity, \( S \) - the cross-sectional area, \( A \) - the surface area and \( h \) - the convection coefficient [11]. In Table III the used thermal resistances are explained.

The heat losses generated in the different parts of the generator are defined by the power loss vector that contains copper losses, iron losses, permanent magnet losses, friction and additional losses separating between the generator components. The cooling of the machine components is presented by the cooling matrix with the thermal resistances of the cooling fluid flow passing the nodes [18].

The temperature rises at each node are calculated by solving the following equation.

\[
\Delta T = (R) \cdot (Q) - 1
\]

where \([R]\) is the square connection matrix containing the thermal resistances of the motor components, \([C]\) - the cooling matrix with the thermal resistances of the cooling fluid flow passing the nodes, \([P]\) - the power loss vector containing the losses at the generator components and \([\Delta T]\) - the temperature rise of the components compared with the initial surface temperature. The specific developed code in program Mathcad was used to compute the temperature rise.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Meaning of Thermal Resistance</th>
</tr>
</thead>
<tbody>
<tr>
<td>( R_{1a} )</td>
<td>Radial Resistance from Frame to Ambient</td>
</tr>
<tr>
<td>( R_{2a} )</td>
<td>Radial Resistance between Frame and Yoke</td>
</tr>
<tr>
<td>( R_{3a} )</td>
<td>Radial Resistance between Yoke and End-Cap Air</td>
</tr>
<tr>
<td>( R_{4a} )</td>
<td>Radial Resistance between Stator Yoke and End-Winding</td>
</tr>
<tr>
<td>( R_{5a} )</td>
<td>Radial Resistance between Stator Yoke and Air Gap</td>
</tr>
<tr>
<td>( R_{6a} )</td>
<td>Radial Resistance between Rotor Iron and Support Structure</td>
</tr>
<tr>
<td>( R_{7a} )</td>
<td>Radial Resistance between Air and Rotor Iron</td>
</tr>
<tr>
<td>( R_{8a} )</td>
<td>Radial Resistance between Shaft and Support Structure</td>
</tr>
<tr>
<td>( R_{9a} )</td>
<td>Radial Resistance between Air and Shaft</td>
</tr>
<tr>
<td>( R_{10a} )</td>
<td>Interconnecting Resistances between Yoke, Tooth, Magnets, Rotor Iron, Support Structure and Shaft</td>
</tr>
<tr>
<td>( R_{11a} )</td>
<td>Axial Resistance between Frame and End-Cap Air</td>
</tr>
<tr>
<td>( R_{12a} )</td>
<td>Axial Resistance between Yoke and End-Cap Air</td>
</tr>
<tr>
<td>( R_{13a} )</td>
<td>Axial Resistance between Tooth and End-Cap Air</td>
</tr>
<tr>
<td>( R_{14a} )</td>
<td>Axial Resistance between Coils and End-Winding</td>
</tr>
<tr>
<td>( R_{15a} )</td>
<td>Axial Resistance between Coils and End-Cap Air</td>
</tr>
<tr>
<td>( R_{16a} )</td>
<td>Axial Resistance between Magnets and End-Cap Air</td>
</tr>
<tr>
<td>( R_{17a} )</td>
<td>Axial Resistance between Magnets and Rotor Iron</td>
</tr>
<tr>
<td>( R_{18a} )</td>
<td>Axial Resistance between Rotor Iron and Support Structure</td>
</tr>
<tr>
<td>( R_{19a} )</td>
<td>Axial Resistance between Support Structure and End-Cap Air</td>
</tr>
<tr>
<td>( R_{20a} )</td>
<td>Axial Resistance between Support Structure and Air</td>
</tr>
<tr>
<td>( R_{21a} )</td>
<td>Axial Resistance from Shaft to Frame through Bearings</td>
</tr>
</tbody>
</table>
VI. CONCLUSIONS

DD PMSG is the most reliable among the high power generators presented on the market. The using of the internal cooling system of the stator copper winding allows solving the main problem of this generator type – its tremendous dimensions.

This paper presents description and analysis of the direct water cooling system of the stator copper winding. It removes heat losses of the stator winding (425 kW) and therefore ensures the adequate temperature of the copper conductors and safe operation of the rotor mounted permanent magnets.

The thermal models of the studied PMSG are implemented by LPN, 3D FEM and CFD. The obtained temperature distribution with the machine parts are presented in Table 5.

As can be seen from the above table the direct water cooling system of the stator copper winding is a very efficient cooling method, as the temperature of the permanent magnets is limited to only 63°C and the highest temperature in the winding is limited to 80°C. Such a temperature can be regarded as an advantage as the stator copper loss is the dominating one and such a low operating temperature guarantees a significantly lower stator resistance for the machine compared to machines operating e.g. in 130°C. Because of the highly efficient cooling the machine is not thermally limited but its peak torque is limited by the synchronous inductance. Therefore the machine operates in a lower temperature than normally but is still remarkably lighter than an air-cooled counterpart.

The presented thermal analysis methods can be used for electrical machine thermal design with direct liquid cooling system. The LPN method is the best for preliminary analysis of temperature distribution within the studied machine. In the last stages of the machine design the FEM and CFD analyses should be considered to define the hot spots in the machine parts. The CFD thermal analysis is also necessary for definition of cooling system parameters.

VII. REFERENCES


VIII. BIOGRAPHIES

Maria V. Polikarpova was born in 1985 in Severodvinsk, Russia, received the Specialist Degree in Industrial Heat and Power from Saint-Petersburg Technological University of Plant Polymers, Russia in 2008 and Master of Science (M.Sc.) degree from Lappeenranta University of Technology (LUT), Finland in 2009. She is currently the PhD student in the Department of Electrical Engineering in LUT, where she studies heat transfer processes and cooling systems of electric motors and electric drives.

Pekka Röyttä was born in Turku, Finland in 1981. He received his M.Sc. in thermodynamics and fluid mechanics and PhD in fluid mechanics from Lappeenranta University of Technology in 2006 and 2009, respectively. He is now working as a Post doctoral researcher at the Institute of Energy at the Lappeenranta University of Technology.

Jüri A. Aleksandrova completed the M.Sc. degree in Electrical Engineering from SPbPU “LEIT” (Russia, Saint-Petersburg) and Lappeenranta University on Technology (Finland, Lappeenranta) in 2009. She continues the educational process toward the D.Sc. degree at Department of Energy, Electrical Engineering. Her current interests include analytical calculations and numerical simulations of high-torque low-speed electrical machines.

Scott Semken is from the Rocky Mountain region of the United States. A degreed mechanical engineer, Scott’s early career focused on performing structural, dynamics, and thermal analyses for the energy industry. Pursuing his passion for machine systems, the focus changed to the design and development of complex electro-mechanical machinery for a variety of American capital equipment manufacturers. Most recently, Mr. Semken has begun working on his doctor’s degree in mechanical engineering at LUT, participating in the development of large direct-drive permanent magnet wind turbine generators.
Juha Pyrhönen (IEEE member) born in 1957 in Kuusankoski, Finland, received the Doctor of Science (D.Sc.) degree from Lappeenranta University of Technology (LUT), Finland in 1991. He became Associate Professor of Electrical Engineering at LUT in 1993 and Professor of Electrical Machines and Drives in 1997. He is currently the Head of the Department of Electrical Engineering in the Institute of LUT Energy, where he is engaged in research and development of electric motors and electric drives. His current interests include different synchronous machines and drives, induction motors and drives and solid-rotor high-speed induction machines and drives.

Janne Nerg received the M.Sc. degree in electrical engineering, the Licentiate of Science (technology) degree, and the D.Sc. degree from Lappeenranta University of Technology (LUT), Finland, in 1996, 1998 and 2000, respectively. He is currently a Senior Researcher with the Department of Electrical Engineering, LUT. His research interests are in the field of electrical machines and drives, particularly numerical modeling and design of electromagnetic devices.
Publication 7

Copyright © 2014 Reprinted with permission from Elsevier B.V.

Permanent magnet synchronous generator design solution for large direct-drive wind turbines: Thermal behavior of the LC DD-PMSG

Yulia Alexandrova a, *, Robert Scott Semken b, Juha Pyrhönen a

a Electrical Engineering Department, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 53851, Finland
b Mechanical Engineering Department, Lappeenranta University of Technology, P.O. Box 20, Lappeenranta 53851, Finland

HIGHLIGHTS

- Lumped-parameter thermal model of LC DD-PMSG was proposed.
- Steady-state and time-dependent temperature distribution for liquid-cooled winding were examined.
- Laboratory liquid-cooled tooth coil prototype was built.
- The predicted and measured values were found to be in reasonably good agreement.

ABSTRACT

Wind is one of the most compelling forms of indirect solar energy. Available now, the conversion of wind power into electricity is and will continue to be an important element of energy self-sufficiency planning. This paper is one in a series intended to report on the development of a new type of generator for wind energy: a compact, high-power, direct-drive permanent magnet synchronous generator (DD-PMSG) that uses direct liquid cooling (LC) of the stator windings to manage Joule heating losses. The main parameters of the subject LC DD-PMSG are 3 MW, 3.3 kV, and 11 Hz. The stator winding is cooled directly by deionized water, which flows through the continuous hollow conductor of each stator tooth-winding. The design of the machine is to a large degree subordinate to the use of these solid-liquid tooth-coils. Both steady-state and time-dependent temperature distributions for LC DD-PMSG were examined with calculations based on a lumped-parameter thermal model, which makes it possible to account for uneven heat loss distribution in the stator conductor and the conductor cooling system. Transient calculations reveal the cooling temperature distribution for an example duty cycle during variable-speed wind turbine operation. The cooling performance of the liquid-cooled tooth-coil design was predicted via finite element analysis. An instrumented cooling loop featuring a pair of LC tooth-coils embedded in a lamination stack was built and laboratory tested to verify the analytical model. Predicted and measured results were in agreement, confirming the predicted satisfactory operation of the LC DD-PMSG cooling technology approach as a whole.

© 2014 Elsevier Ltd. All rights reserved.

1. Introduction

In recent years, intensive research efforts have focused on the development of high power electrical generators for wind turbine applications. Today, most land-based wind turbines have power ratings from 3.5 MW to 3 MW, and wind turbines designed for offshore applications are typically 3 MW–5 MW. Sustaining the rapid growth in wind energy will require developing wind turbines with even higher power ratings, and there are several development projects under way targeting power ratings up to 15 MW (e.g., Sway 10 MW wind turbine [1], Amur Project of 15 MW wind turbine [2]).

Over the past decade, direct-drive (DD) electrical generators have become pervasive in wind turbines rated below 6 MW. They are now being used in higher power wind turbines. DD electrical generators enable direct coupling to the turbine blades, eliminating the gear box. The primary reason for the rapid acceptance of DD technology has been the energy savings associated with running an electrical generator at a lower speed, as well as the general reliability of such a drive system [3].

A review of electrical generators for DD wind turbines has been presented in Ref. [4]. As a result of progress in power electronics,
software engineering, and materials: permanent magnet synchronous generators (PMSGs), made from modern rare earth magnet materials, are receiving increased attention for DE wind turbine applications.

The most unique feature of DE wind turbine electrical generators is their size. They operate below 10 rpm (typically 15–15 rpm at 3 MW) and feature a relatively large diameter to length ratio. DE wind turbine electrical generators look like large diameter, slender rings. The large diameter allows high torque to be developed. Furthermore, substantial structural reinforcement is required to support the high torque developing in high power DE wind turbine electrical generators. Larger diameter and the need for adequate structural reinforcement leads to a truly massive structure. Therefore, one of the main problems facing the designers of large-power, large diameter DE wind turbine electrical generators is how to minimize its size and weight.

Size reduction of wind turbine electrical generators is ultimately limited by the ability to remove heat from the stator windings. The application of direct liquid cooling of the stator winding enables operation of the electrical generators at a higher output for a particular generator size as compared to an air-cooled generator of the same size. Therefore, liquid-cooled electrical generators may be smaller than forced air-cooled electrical generators having the same rated power.

This paper is one in a series intended to report on the conceptual development of a new type of generator for wind energy; a compact, high-power DE-PMSG that uses direct liquid cooling (LC) of the stator windings to manage high Joule heating losses.

Casting of the 8 MW DE-PMSG is examined in more detail in this paper, and both steady-state and time-dependent temperature distributions are calculated and validated. These are typically three approaches used to predict the thermal performance of electrical machines: making calculations using simplified mathematical models, applying numerical methods and taking experimental measurements. For instance, Palmarini et al. [5] described an experimental cooling study for a closed electric motor based on measurements. Modifications were suggested, and a new prototype casing was realized. Vlachos et al. [6] conducted an experimental, flow and local heat transfer in the air gap of an open four-pole synchronous motor. Based on their heat transfer measurements, a correlation for heat transfer was derived corresponding to the stator, motor shaft, and rotor. However, for large electrical machines, it is cost prohibitive to build a full-scale prototype to make comprehensive thermal performance measurements.

Computational fluid dynamics (CFD) would be useful to understand heat transfer performance, and a number of CFD models of electrical machines have been developed. For instance, Simms et al. [7] used a CFD model to calculate the heat distribution in a switched reluctance motor with natural cooling. Jungmeister et al. [8] modeled PMSG fluid flow and found good correspondence between temperatures predicted by the CFD analysis and values measured experimentally. Recently, CFD was used to evaluate the thermal performance of a 35 kW 12-pole, motor operating in cooled mode. Kolačinski et al. [9] applied CFD computational analysis to investigate the temperature rise phenomena in a high-speed permanent magnet machine. However, CFD is computationally intensive, and therefore the approach is not feasible when repeated calculations are required for large complex electrical systems.

Thermal analysis with lumped parameters could be applied as a preliminary approach to predicting the approximate thermal performance of large electrical machines. To date, research papers have been published describing lumped parameter thermal models of electrical machines equipped with indirectly cooled multi-turn coils [11-13]. An equivalent thermal circuit for a multi-barrier interior PMSG was reported in Ref. [11] that assumed no circumferential heat flow and temperature variance only in the radial direction. Nerg et al. [12] described a lumped parameter thermal model for radial-flux electrical machines of high power density under steady state thermal conditions. That model has been applied to a high-speed induction motor, and a low-speed PMSG and PMIG in considerably different size categories. Ref. [13] describes the authors’ proposed a lumped parameter thermal model for a radial-flux permanent magnet machine. This model was validated by comparing results in experimental data. In Ref. [14], the thermal network method based on two-dimensional heat conduction in hollow cylinder geometry was applied to build the thermal model of an electric motor and to calculate its maximum temperature rise. An analytical layered thermal model to calculate the temperature distribution in the winding of an oil-filled transformer with zigzag cooling ducts is described in Ref. [15]. However, there have been few papers published that describe the application of lumped parameter models to analyze the forced cooling of stator windings composed of form-wound copper conductors.

The authors of Ref. [16] presented a lumped parameter model of a 12 MW DD-PMSG using the equivalent thermal conductivity of the stator windings. Here, we extend their lumped parameter thermal model to include the detailed thermal model of the stator slot and to include the previously excluded co-flow heat flow arrangement. As a result, the new model more accurately estimates temperatures by accounting for the non-uniform distribution of copper losses resulting from the skin effect. The proposed lumped-parameter thermal model is suitable for integration with the algorithm presented in Ref. [17] to optimize the DE-PMSG design.

2. Design and cooling concept of the LC DD-PMSG

The large air gap diameter of a high-power wind turbine generator, e.g. 5 m or more, requires segmented construction. The rotor and stator magnetic circuits with the number of identical units. These units are connected electrically in series or in parallel to get desired voltage levels. There are many variations for number of units in DD basic design, including 5, 6, 8, 10, 12 units, and so on. This paper focuses on an 8 MW inner rotor LC DD-PMSG constructed of 12 identical stator and rotor segments. Stator air-gap diameter and active stator length are 6.93 m and 3.15 m, respectively. Each of the stator segments has 12 slots. The total number of slots for the 12 stator segments of the generator is, therefore, 696. The rotor comprises 36 magnet poles per segment for a total of 120 permanent magnet poles (60 pole pairs). This configuration results in a rated frequency of 10–12 Hz, which is still acceptable for the cyclic operating of the converter’s insulated-gate bipolar transistors. The combination of 60 pole pairs and 144 stator slots results in a fractional slot concentrated non-overlapping winding (tc-tooth-coil winding) in which each winding phase coil is concentrated around individual teeth and thus does not span adjacent teeth or coils, simplifying direct liquid cooling. Such a machine works at the fifth harmonic of the stator current linkage using it as the means of electromechanical power conversion. The fundamental harmonic is of minimal significance. Fig. 1 shows a detailed view of the LC DD-PMSG. This LC DD-PMSG has liquid-cooled windings in the stator and rotor-surface-mounted permanent magnets. Both the stator and rotor are based on a laminated steel structure.

A tooth-coil winding architecture offers the most straightforward approach in direct liquid cooling of the stator windings. Each tooth-coil is an elongated continuous two-layer winding of copper that is shaped to fit into the slots on either side of a laminated stator tooth. See a photo in Fig. 2. The cross section of the wound conductor is rectangular. Since it forms a closed loop, the tooth-coil itself can be used as a coolant transport conduit. Stainless
steel tubing embedded within the copper at its centerline serves as the coolant channel. The stainless steel (316 series) offers high electrical resistivity, good mechanical strength, and corrosion and erosion resistance. Tooth-coil coolant enters the inner winding layer from the bottom, draws heat out of the copper, and leaves the coil again from the bottom of the outer layer.

The copper is wrapped with a thin layer of insulating material to electrically isolate adjacent conductors. The two winding layers for each tooth-coil form two conductor columns on either side of the tooth. Between each conductor column and between the tooth-coil and the stator laminations are another more substantial layer of insulation. Each tooth-coil is connected to the cooling loop via the stainless steel tubing through an electrically isolating coolant manifold. The manifold can be made of Ultra low polyethylene (PE). Electrical connections are made using terminal buses (shown in the photos of Fig. 2). Table 1 gives the relevant characteristics for an 8 MW LC DC-PMSG tooth-coil.

3. Layered lumped-parameter thermal model

An accurate evaluation of temperature distribution is necessary to design a liquid-cooled generator with an acceptable thermal performance margin. Lumped parameter thermal model was used to carry out the required thermal analysis. The lumped parameter thermal model neglects axial temperature variation. The heat transfer paths between the different generator parts are represented using thermal resistances at constant temperature. The heat flow paths defined for the equivalent steady-state lumped parameter thermal model of the LC DC-PMSG are shown in Fig. 3.

A portion of the heat localized within each stator tooth flows through the slot and tooth-coil insulation material, through the copper, and into the coolant. Heat also flows along the stator teeth to the air gap and along the stator teeth to the core. Some of the heat generated in the permanent magnets of the rotor can flow into the rotor, and some can flow into the stator through the air gap.

The stator laminations are bound tightly together with a number of tensioned rods passing through them. This construction increases the total surface area of the laminations exposed to the cooling air, which enhances heat removal since the additional heat transfer is proportional to the increase in surface area.

Temperature distribution analysis began by assigning the following losses or heat source values for an 8 MW LC DC-PMSG. The losses are subdivided into the contributions by the various parts, i.e., the stator yoke and teeth, the rotor yoke, the permanent magnets, stator winding, and the air gap. Table 1 reports the loss division at the rated load.

Because of the negligible stator iron losses in a low-speed, low-frequency generator; the steady-state temperature rise in the stator winding depends mainly on the copper losses. The complex heterogeneous structure of indirectly air-cooled stator windings is usually replaced with a single homogeneous material that reproduces a representative thermal behavior. Equivalent thermal conductivity is used [19]. However, to improve temperature prediction accuracy, a detailed thermal model of the stator slot is developed, in which all the adjacent conductors in the cooling circuit are modeled separately with a corresponding equivalent convective thermal circuit. The convection thermal circuit is executed so that, for each cooling circuit, the outlet coolant temperature of each preceding conductor becomes the inlet coolant temperature of the subsequent one. Some heat transfer from the end windings is not modeled separately. End windings were taken into account by increasing coolant channel length by factor $k_{\text{sw}}$.

4. Analysis of heat transfer performance

4.1 Limiting temperature

The minimum inlet coolant temperature of a liquid cooled winding is limited by the strength of the conductor insulation material. If the cooling liquid is too cold, thermal shock may lead to insulation failure. On the other hand, if the inlet coolant temperature is too high, heat will not be effectively removed, and there is an even greater danger of insulation failure. According to Ref. [20], the recommended water temperature range at the inlet of a direct liquid cooling circuit is from 33 °C to 50 °C. According to Ref. [21], the temperatures range from 45 °C to 50 °C. Coolant temperature at the coil outlet is usually maintained below 80 °C to provide a sufficient margin to ensure that stator conductor temperatures do not exceed safe levels even during abnormal operating conditions. According to the international standard for rotating electrical machines (IEC 60034-1) [22], coolant outlet temperatures must not exceed 90 °C in any event. Coolant temperature rise, the difference between outlet and inlet temperatures, must be kept below 30 °C at rated power, assuming an average ambient temperature of 35 °C.
4.2. Steady state thermal loads

Copper temperature is a function of coolant temperature, as the temperature of the coolant must be known to predict copper temperature. Two equations describing heat transfer in single phase steady state flow are used to evaluate outlet coolant temperature. Equation (1) relates to the physical change in the coolant brought about by the heat transfer system. Equation (2), an expression of Newton's law of cooling, is system related and considers cooling system specifics such as flow and heat transfer surface area.

\[ P_{Cu-o-j}R_{kj} = \dot{q}_{ht}\cdot \Delta T_{oj}\cdot \frac{T_{oj}}{T_{o-j}} \]  

\[ P_{Cu-o-j}R_{kj} = \frac{\sum_{j=1}^{N} P_{Cu-o-j}R_{kj}}{1 - (\dot{q}_{ht}\cdot \Delta T_{oj}\cdot \frac{T_{oj}}{T_{o-j}})} \]  

where \( \dot{q}_{ht} \) is coolant heat capacity, \( j \) is the index of the conductor belonging to the coolant circuit, \( R_{kj} \) is the factor for electrical resistivity increase at temperature increases, \( L_{o} \) is static length, \( P_{Cu-o-j} \) is heat removed by the coolant, \( S_{CJ} \) is the cooling channel cross-sectional area, \( T_{o-j} \) is coolant outlet temperature, \( T_{oj} \) is conductor average temperature, \( \alpha_{i} \) is heat transfer coefficient, \( \Delta T_{oj} \) is cooling channel hydraulic perimeter, \( \rho_{c} \) is coolant density, and \( v_{c} \) is coolant velocity.

The heat transfer coefficients for convection rely on the proven empirical convection correlations models available in heat transfer literature [23].

In Equations (1) and (2), the inlet coolant temperature of the jth conductor depends on the total heat removed by the coolant having flowed through adjacent \((j-1)\) conductors of the cooling circuit. These equations interest in a related way, and the heat transfer balance equations can be written as:

\[ T_{cj} \cdot \frac{P_{Cu-o-j}R_{kj}}{\sum_{j=1}^{N} P_{Cu-o-j}R_{kj}} = \frac{1}{C_{p_c} \cdot \rho_c \cdot \Delta T_{cj} \cdot \frac{T_{cj}}{T_{o-j}}} + \frac{\sum_{j=1}^{N} P_{Cu-o-j}R_{kj}}{C_{p_c} \cdot \rho_c \cdot \Delta T_{cj} \cdot \frac{T_{cj}}{T_{o-j}}} \]  

where the heat \( P_{Cu-o-j} \) is evaluated from the thermal circuit iteratively.

4.3. Transient thermal loads

Short term generator current overloading may increase the temperature of the stator winding to dangerous levels. Theoretical temperature transient calculations have been made by Kazovskij [20]. Temperature does not respond immediately to changes in

<table>
<thead>
<tr>
<th>Power loss component</th>
<th>Value [kW]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator copper winding loss, ( P_{cu} )</td>
<td>550</td>
</tr>
<tr>
<td>Stator yoke iron loss, ( P_{y} )</td>
<td>5</td>
</tr>
<tr>
<td>Stator pole iron loss, ( P_{p} )</td>
<td>3.7</td>
</tr>
<tr>
<td>Exciter current loss in permanent magnets, ( P_{em} )</td>
<td>50</td>
</tr>
<tr>
<td>Rotor yoke iron, ( P_{ry} )</td>
<td>1</td>
</tr>
<tr>
<td>Air gap friction loss, ( P_{ag} )</td>
<td>1.5</td>
</tr>
</tbody>
</table>
generates load. Its time variation follows an exponential law, where the slope of the curve decreases with time. The transient temperature solution for the copper conductor can be written as follows.

\[ T_{Cu}(t) = T_{Cu,in} \left( 1 - e^{-t/T} \right) + T_{Cu,in} \left( 1 - e^{-t/T} \right), \]

where \( T_{Cu,in} \) is the initial temperature of the conductor. \( T_{Cu,in} \) is the steady-state temperature. The heat transfer coefficient from the coolant to the conductor is a function of the coolant temperature and the thermal conductivity of the coolant.

Equation (7) gives the thermal time constant, \( T \), which depends on the thermal properties of the conductor and the coolant.

\[ T = \frac{P_{Cu} \alpha_{Cu} \rho_{Cu} c_{p,Cu} \Delta T}{T_{Cu,in} + T_{Cu,in} \left( 1 - e^{-t/T} \right)}, \]

where \( \alpha_{Cu} \) is the thermal conductivity of copper, \( \rho_{Cu} \) is the density of copper, \( c_{p,Cu} \) is the specific heat capacity of copper, and \( \Delta T \) is the temperature difference between the conductor and the coolant.

5. Validation of the model and results

The developed analytical models have been applied to the design of a 6 MW LC DD-PMG to predict thermal performance. Water was selected as the coolant. The dimensionless model was adjusted to the values shown in Table 1. According to Table 4, the model achieves its rated power at an average operating temperature of approximately 81 °C coolant. The temperature profiles of the tooth-coil and the slot region are shown in Fig. 5. The temperature distribution is calculated by solving the transient heat conduction equation, which considers the convective and radiative heat transfer between the coolant and the copper structure.

A failure of the pump or a break in the coolant line can result in a loss of coolant to the stator winding. In the absence of coolant circulation, the coil temperatures increase considerably. According to Table 5, the temperature distribution of the stator winding increases by almost 5°C at 70% of the rated current following a loss of coolant event.

### Table 3: Permissible operation durations for overcurrents based on rated coolant flow properties

<table>
<thead>
<tr>
<th>Operation duration</th>
<th>Stator current [A]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Continuous</td>
<td>110</td>
</tr>
<tr>
<td>15 min</td>
<td>115</td>
</tr>
<tr>
<td>6 min</td>
<td>120</td>
</tr>
<tr>
<td>5 min</td>
<td>125</td>
</tr>
<tr>
<td>4 min</td>
<td>135</td>
</tr>
<tr>
<td>3 min</td>
<td>140</td>
</tr>
<tr>
<td>2 min</td>
<td>150</td>
</tr>
<tr>
<td>1 min</td>
<td>160</td>
</tr>
<tr>
<td>30 sec</td>
<td>180</td>
</tr>
</tbody>
</table>

The results of the transient analysis, presented in Table 5, were computed using the procedure described in Sections 4.3 and 4.4. The transient model enables prediction of the time evolution of the stator winding temperature. The calculated thermal time constant of copper is 236 sec. The maximum temperatures of each conductor with respect to time can be determined from Fig. 5. The initial total copper loss was 500 kW. The effect of increasing temperature on the electrical resistivity of copper conductors, and therefore the copper losses, was accounted for by the \( k_L \) factor. For the first case, the copper loss was calculated to be 1800 kW. The results show the generator can operate continuously without reaching the temperature limit. For the second case, the
Table 6
Calculated steady-state temperatures at rated load at different parts of 8 MW LC DD-PMSG.

<table>
<thead>
<tr>
<th>Conductor number in the coolant path</th>
<th>Copper (liquid) temperature, °C</th>
<th>Conductor number in the coolant path</th>
<th>Copper (liquid) temperature, °C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cond. 1 (150 W)</td>
<td>42.4 (42.0)</td>
<td>Cond. 11 (255 W)</td>
<td>63.7 (83.1)</td>
</tr>
<tr>
<td>Cond. 3 (150 W)</td>
<td>44.5 (45.9)</td>
<td>Cond. 12 (255 W)</td>
<td>66.3 (45.7)</td>
</tr>
<tr>
<td>Cond. 3 (150 W)</td>
<td>46.5 (46.8)</td>
<td>Cond. 13 (215 W)</td>
<td>66.6 (45.7)</td>
</tr>
<tr>
<td>Cond. 4 (180 W)</td>
<td>48.1 (47.6)</td>
<td>Cond. 14 (182 W)</td>
<td>68.4 (46.0)</td>
</tr>
<tr>
<td>Cond. 5 (184 W)</td>
<td>56.1 (56.6)</td>
<td>Cond. 15 (182 W)</td>
<td>70.7 (70.1)</td>
</tr>
<tr>
<td>Cond. 6 (184 W)</td>
<td>51.9 (51.4)</td>
<td>Cond. 16 (185 W)</td>
<td>72.6 (72.2)</td>
</tr>
<tr>
<td>Cond. 7 (222 W)</td>
<td>54.1 (55.5)</td>
<td>Cond. 17 (185 W)</td>
<td>73.2 (72.9)</td>
</tr>
<tr>
<td>Cond. 8 (222 W)</td>
<td>54.1 (55.6)</td>
<td>Cond. 18 (165 W)</td>
<td>75.6 (74.1)</td>
</tr>
<tr>
<td>Cond. 9 (247 W)</td>
<td>58.7 (58.1)</td>
<td>Cond. 19 (147 W)</td>
<td>79.5 (79.2)</td>
</tr>
<tr>
<td>Cond. 10 (283 W)</td>
<td>61.1 (60.9)</td>
<td>Cond. 20 (147 W)</td>
<td>61.1 (60.9)</td>
</tr>
</tbody>
</table>

Stator tooth, °C                     52.7
Stator yoke, °C                      53.5
Permanet magnet, °C                  67.7
Air gap, °C                          67.3
Rotor yoke, °C                       57.9

The current density was increased by a factor of 2. Within 20 sec, the copper conductor heats up from the rated operational temperature to the temperature limit of 95 °C.

Installed in a variable-speed wind turbine, the LC DD-PMSG is supposed to be operated according to its torque curve (Fig. 6).

Fig. 6 shows that copper and coolant temperatures increase at approximately 0.05 °C per s when there is no coolant flow and 50% of generator rated current is applied.

Therefore, the studied LC DD-PMSG can operate without coolant circulation for about 60 s at 50% of its rated current.

6. LC tooth-coils prototype

To validate the simulation results and to demonstrate the liquid-cooling technology with its applications in a stator implementation, a small prototype with two liquid-cooled tooth-coils was designed, built, and tested at the Lappeenranta University of Technology. The specific goals for the prototype were to prove the feasibility of manufacturing the LC DD-PMSG tooth-coils and demonstrate the effectiveness of direct liquid cooling of the stator copper conductors. The instrumented loop provided thermal performance data to clarify the dependence and distribution of temperatures within the coils as well as to make comparison between theoretical and practical results.

Fig. 8 is a photo of the instrumented small prototype. It was located in a room free of forced air movement and oriented so the slot conductors were horizontal. The apparatus comprises the coils (1), the inlet and outlet coolant manifold (2), connection to the power source (3), a coolant reservoir (4), the pump (5), the heat...
exchanger (6), coolant filters (7), a volumetric flow transducer (8), conductor temperature measurement RTDs (9), coolant pressure transducers and thermocouples (10), the power control system (11), and the data acquisition and processing system (12). All the instruments were calibrated prior to testing. The coolant was ECOCUT 115, a polyalkylsiloxane (PAO) heat transfer fluid.

Each tooth-coil in the prototype is formed from a 5.2 m long continuous copper conductor with a cross-sectional height of 15 mm and width of 15.6 mm. There are 4 turns per tooth-coil forming two rows and four columns structure. Coolant conduits (316 series stainless steel) are embedded in the conductor copper. The conduits have an inner diameter of 4 mm and a wall thickness of 1 mm. Each coil was first formed, and then the copper was wrapped with a layer of glass-fiber insulation tape (Remikalox 45481). A second insulating wrap covered the active lengths, bundling four straight lengths of copper conductors on each side and electrically insulating the coil from the steel laminations. The steel laminations were laser cut from SURA M600-59A coated electrical steel. The lamination stack was bound tightly with 8 hollow 316 series stainless steel tensioning rods. Plastic caps (PEI) secured the coils within the lamination slots.

The two tooth-coils were hydraulically connected to the cooling loop in parallel by bolting them to the PEI electrically isolating inlet and outlet coolant manifold. All stainless steel tubing connections were via orbital welds or compression fittings. The steel-to-plastic interfaces were sealed with Buna-N double-seal e-rings (quad seals). Two concentric seals at each hydraulic connection offered 4 sealing surfaces to guarantee leak-free operation.

Electrically, the tooth-coils were connected in series. Incoming power was connected to the outer terminal lug of the first coil, and outgoing power was connected to the outer terminal lug of the second coil. The series connection was made using a copper bridge between the coils fastened to the inner terminal lugs.

![Fig. 5. Conductor temperature distribution with coolant velocity 1 m/s for current density increase factor $k_1 = 11$ and $k_2 = 2$.](image1)

![Fig. 6. 1 MW LE DC/PMG output torque curve drawn on the generator efficiency map.](image2)

![Fig. 7. Data cycle of variable speed wind turbine equipped with LE DC/PMG and LE start with winding temperature response.](image3)

![Fig. 8. Temperature rate of the copper and coolant when coolant is not circulated.](image4)
Fig. 9. Experimental setup: (1) liquid-cooled tooth-coil; (2) inlet and outlet coolant manifold; (3) wires from the power source; (4) storage tank; (5) pump; (6) heat exchanger; (7) filter; (8) flow indicator; (9) thermocouple; (10) pressure indicator; (11) control system; (12) data processing system.

Fig. 10. Measurement results for a 5 h period.

To realistically evaluate the performance of a low-speed D0 generator, a 1110 A, 11 Hz power source should have been used to produce characteristic system losses. Such a system was not available, and a higher frequency system was used to produce losses in the tooth-coil conductors with lower current. The power source was a variable frequency 550 Hz synchronous generator capable of generating currents up to 150 A and operating continuously at 104 A (limiting by fuses overheating). To maximize joule heating, testing was carried out using the highest current and frequency that could be sustained for long periods: 104 A and 540 Hz. The losses achieved using this arrangement were not sufficiently high. To further increase losses and elevate system temperatures, a 5 mm thick steel plate was set on top of the laminations above the coils to act as a source of eddy current loss heating. While no longer a precise simulation, the test setup can still be regarded as indicative. It made it possible to observe how effectively the cooling system could remove heat.

With power on and the plate in position, 75 W of heat was produced in each coil and 3.5 kW of heat was produced in the solid plate. Three KIDs (Resistance Temperature Devices) were installed on each coil to monitor copper temperature at the inlet, outlet, and middle of the conductor length. Inlet and outlet coolant temperature was monitored using Type K thermocouples. Fig. 10 shows the temperatures measured over a 5 h period.

On the left in Fig. 10, where the temperature curves are relatively flat, the coolant loop is operating normally with 2.05 l/min coolant flow through each coil. Coolant pressure at each inlet is 2.5 kPa. At the approximate time of 15:40, the pump was switched off causing the coolant flow to stop. The result is the rapid increase in copper temperatures and drop in coolant temperatures shown near the right side of the plot. At 16:16, the pump was switched back on and measured temperatures quickly stabilized once again. For the duration of testing, all thermocouples were continuously monitored and recorded at 1 s intervals.

A 2D finite element model was prepared as shown by Fig. 11, taking into account the geometry of each part of the prototype.

Table 5 gives a summary of the steady-state results recorded during the experiment and calculated from the analytical and finite-element models.

Predicted and measured temperatures reveal similar trends and show no significant differences, supporting the validity of the developed models. The difference between measured and calculated results is below 5% and within the measurement error of ±1 °C. These accurate results, considering the thermal model does not deal with the axial direction. Results reveal that because power losses in the copper conductors are uneven, copper temperature at the middle of each tooth-coil length can be similar or even higher than copper temperature at its end, the coolant outlet. Actual copper temperatures at the coil inlets and outlets were lower than calculated temperatures. This may be due to the additional convective cooling provided by the terminal lugs and the copper bridge. Despite the low copper tooth-coil heating observed during the laboratory test, the performed thermal test demonstrates the effectiveness of direct liquid cooling. When the pump was switched off, in the second test, coolant circulation ceased, and...
copper temperatures began increasing rapidly at 0.015°C per s. The predicted temperature rate, based on Equation (8), was 0.02°C, which is a good agreement rate between calculated and measured values.

To emphasize the direct liquid cooling capability of the tooth-coil design, the last column in Table 9 gives calculated temperatures for 3 kW power losses in a similar tooth-coil. The temperature difference between the inlet and outlet steady-state temperatures is 43.2°C, and based on Equation (1), the heat transferred by the oil is 2.9 kW, showing that direct liquid tooth-coil cooling is extremely effective and a relatively simple cooling approach.

7. Conclusion

Thermal modeling of the 8 MW LC DFIG was performed on the basis of an analytical thermal model, which makes it possible to account for uneven heat loss distribution in the stator conductors and conductor cooling arrangement. A steady-state thermal analysis with the rated thermal load predicted a maximum temperature in the liquid-cooled tooth-coil winding under forced water cooling of 81°C for a cooling water flow of 1 m³/s. The thermal results for both the analytical thermal model and the finite element analysis were similar. An analytical transient thermal model was driven by a pulsed load. The thermal load was a square wave that varied with time. Results showed that LC DFIG power capacity may be increased without changing the cooling system.

A comparison between analytical results and finite-element calculation pointed out the suitability of the developed analytical models. Therefore, the proposed analytical thermal models can be incorporated into the electromagnetic LC DFIG model to perform a multiphysics analysis.

An instrumented small prototype with two liquid-cooled tooth-coils was built to provide measurement data to validate predictions. The prototype coils were tested with 104 A of alternating current, which produced 75 W of copper loss in each coil. No anomalous heating was observed. The prototype demonstrated the ability of effective direct cooling of the tooth-coils. When coolant circulation ceased, copper temperature began increasing rapidly.

The prototype showed the technical feasibility of the LC tooth-coil design. Furthermore, this combined theoretical and experimental study strengthened the presumption that the proposed cooling approach makes an LC DFIG possible and can solve the problems associated with large dimensions of future high-power DFIG wind turbine generators.

Acknowledgements

The authors would like to thank the Academy of Finland for their support for this research.

References


[19] D. Tabaev, A. Bagiotti, A. Cagnoni, Solving the more difficult aspects of electric motor thermal analysis in small and medium size industrial induction


591. IKÄHEIMONEN, TUULI. The board of directors as a part of family business governance – multilevel participation and board development. 2014. Diss.

592. HAJIALI, ZUNED. Computational modeling of stented coronary arteries. 2014. Diss.

593. UUSITALO, VILLE. Potential for greenhouse gas emission reductions by using biomethane as road transportation fuel. 2014. Diss.


595. HEIKKINEN, JANNE. Vibrations in rotating machinery arising from minor imperfections in component geometries. 2014. Diss.

596. GHALAMCHI, BEHNAM. Dynamic analysis model of spherical roller bearings with defects. 2014. Diss.

597. POLIKARPOVA, MARIJA. Liquid cooling solutions for rotating permanent magnet synchronous machines. 2014. Diss.

598. CHAUDHARI, ASHVINKUMAR. Large-eddy simulation of wind flows over complex terrains for wind energy applications. 2014. Diss.

599. PURHONEN, MIKKO. Minimizing circulating current in parallel-connected photovoltaic inverters. 2014. Diss.

600. SAUKKONEN, ESA. Effects of the partial removal of wood hemicelluloses on the properties of kraft pulp. 2014. Diss.


603. SSEBUGERE, PATRICK. Persistent organic pollutants in sediments and fish from Lake Victoria, East Africa. 2014. Diss.

604. STOKLASA, JAN. Linguistic models for decision support. 2014. Diss.

605. VEPSÄLÄINEN, ARI. Heterogenous mass transfer in fluidized beds by computational fluid dynamics. 2014. Diss.

606. JUVONEN, PASI. Learning information technology business in a changing industry landscape. The case of introducing team entrepreneurship in renewing bachelor education in information technology in a university of applied sciences. 2014. Diss.

607. MÄKIMATTILA, MARTTI. Organizing for systemic innovations – research on knowledge, interaction and organizational interdependencies. 2014. Diss.

608. HÄMÄLÄINEN, KIMMO. Improving the usability of extruded wood-plastic composites by using modification technology. 2014. Diss.

609. PURHONEN, MIKKO. Minimizing circulating current in parallel-connected photovoltaic inverters. 2014. Diss.

610. SUUKKANEN, HEIKKI. Application and development of numerical methods for the modelling of innovative gas cooled fission reactors. 2014. Diss.

611. LI, MING. Stiffness based trajectory planning and feedforward based vibration suppression control of parallel robot machines. 2014. Diss.
KOKKONEN, KIRSI. From entrepreneurial opportunities to successful business networks – evidence from bioenergy. 2014. Diss.


MBALAWATA, ISAMBI SAILON. Adaptive Markov chain Monte Carlo and Bayesian filtering for state space models. 2014. Diss.

UUSITALO, ANTTI. Working fluid selection and design of small-scale waste heat recovery systems based on organic rankine cycles. 2014. Diss.

METSO, SARI. A multimethod examination of contributors to successful on-the-job learning of vocational students. 2014. Diss.

SIITONEN, JANI. Advanced analysis and design methods for preparative chromatographic separation processes. 2014. Diss.

VIHAVAINEN, JUHANI. VVER-440 thermal hydraulics as computer code validation challenge. 2014. Diss.

AHONEN, PASI. Between memory and strategy: media discourse analysis of an industrial shutdown. 2014. Diss.

MWANGA, GASPER GODSON. Mathematical modeling and optimal control of malaria. 2014. Diss.

PELTOLA, PETTERI. Analysis and modelling of chemical looping combustion process with and without oxygen uncoupling. 2014. Diss.


HYVÄRINEN, MARKO. Ultraviolet light protection and weathering properties of wood-polypropylene composites. 2014. Diss.

RANTANEN, NOORA. The family as a collective owner – identifying performance factors in listed companies. 2014. Diss.

VÄNSKÄ, MIKKO. Defining the keyhole modes – the effects on the molten pool behavior and the weld geometry in high power laser welding of stainless steels. 2014. Diss.


GRUDINSCHI, DANIELA. Strategic management of value networks: how to create value in cross-sector collaboration and partnerships. 2014. Diss.

SKLYAROVA, ANASTASIA. Hyperfine interactions in the new Fe-based superconducting structures and related magnetic phases. 2015. Diss.