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Contributions to Permanent Magnet Wind Generator Design
Including the Application of Soft Magnetic Composites

By: Mohamed Azeem Khan

Thesis submitted to the Department of Electrical Engineering, University of Cape Town, in complete fulfilment of the requirements for the degree of Doctor of Philosophy

15 August 2006
Declaration

This dissertation is submitted to the Department of Electrical Engineering, University of Cape Town, in complete fulfilment of the requirements for the degree of Doctor of Philosophy. It has not been submitted before for any degree or examination at this or any other university. The author confirms that this thesis is based on his own work. Portions of this work have been published in peer-reviewed journals and at refereed international conferences. The author confirms that he was the primary researcher in all instances where work described in this thesis was published under joint authorship.

M.A. Khan

15 August 2006
Acknowledgments

In the name of Allah, the Most Gracious, the Most Merciful. All praises are due to Allah, Lord of the worlds. Peace and salutations upon our beloved Prophet Muhammad (PBUH). I testify that there is no God but Allah and I testify that none is worthy of worship, except Allah and I testify that Muhammad (PBUH) is the final Prophet of Allah.

This dissertation is dedicated to my dearest mother, Mrs Sher Banu Khan, whose love, understanding, support, sacrifice and pain could not be surpassed. May she rest in peace and may the Almighty Allah grant her a very high place in paradise, Insha-Allah.

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Finally, a message to my daughter Aasiyah and son Ayyoob, you have brought much more love and joy into our lives. May the Almighty Allah grant you strong Imaan and may you aspire to the examples set by both your namesakes.
Abstract

Wind energy has received renewed interest in recent years as the demand for renewable energy sources has increased. Small wind energy conversion systems are generally intended for end-user and rural applications, where they are interfaced to isolated loads or weak electrical grids. These systems generally have three-bladed horizontal-axis wind turbines, permanent magnet (PM) wind generators, power electronic circuitry and associated controls. The main objective of this dissertation is to contribute knowledge toward the design of small PM wind generators, which includes the application of soft magnetic composite (SMC) materials to the design of these machines.

A problem is first addressed of adapting a PM wind generator, intended for high-speed operation with a small 3-bladed wind turbine, for low speed operation with a new multi-blade, high solidity turbine. An analytical model is formulated and then used to examine the effect of several changes to the design of the wind generator. A new specification of the redesigned machine is then set. The overall performance of the redesigned machine coupled to the new turbine is shown to be satisfactory.

An alternative method of operation of a PM wind generator is considered. The method considers variable speed operation of a turbine at a tip speed ratio, which ensures that maximum shaft torque is captured. The performance of a small PM wind generator operated at maximum power capture is compared to that of maximum torque capture.

A design procedure is outlined for a PM wind generator, which optimises its performance over a wide operating wind speed range. An optimum permutation of
generator design variables is selected by an optimisation routine. An optimised design of a 1kW PM wind generator is produced with a relatively flat efficiency curve over a wide operating wind speed range. This is in contrast to a commercial system that was investigated, which was characterised by a rather low efficiency at rated wind speed and also by a larger drop in efficiency over a wide range of wind speeds.

The design and prototyping of a Soft Magnetic Composite (SMC)-based axial-flux PM wind generator with a composite SMC/steel stator core is investigated. The support structure of the wind generator is designed for a new turbine with contra-rotating blades. A chemical processing technique is investigated for treating the smear of the iron particles on the machined surfaces of the SMC core. The PM wind generator was prototyped and extensively tested in the laboratory.
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</thead>
<tbody>
<tr>
<td>AEO</td>
<td>Annual energy output</td>
</tr>
<tr>
<td>AFPM</td>
<td>Axial-flux permanent magnet</td>
</tr>
<tr>
<td>AWG</td>
<td>American wire gauge</td>
</tr>
<tr>
<td>BEM</td>
<td>Blade element momentum</td>
</tr>
<tr>
<td>d-axis</td>
<td>Direct axis (rotor pole axis)</td>
</tr>
<tr>
<td>ed</td>
<td>Electrical degrees</td>
</tr>
<tr>
<td>EDM</td>
<td>Electrostatic discharge machining</td>
</tr>
<tr>
<td>FE</td>
<td>Finite element</td>
</tr>
<tr>
<td>FOC</td>
<td>Field orientated control</td>
</tr>
<tr>
<td>GA</td>
<td>Genetic Algorithm</td>
</tr>
<tr>
<td>HAWT</td>
<td>Horizontal-axis wind turbine</td>
</tr>
<tr>
<td>HAWTs</td>
<td>Horizontal-axis wind turbines</td>
</tr>
<tr>
<td>NdFeB</td>
<td>Neodymium iron boron</td>
</tr>
<tr>
<td>PBIL</td>
<td>Population-based incremental learning</td>
</tr>
<tr>
<td>PM</td>
<td>Permanent magnet</td>
</tr>
<tr>
<td>PMs</td>
<td>Permanent magnets</td>
</tr>
<tr>
<td>PMSM</td>
<td>Permanent magnet synchronous</td>
</tr>
<tr>
<td>q-axis</td>
<td>Quadrature axis (rotor inter-pole axis)</td>
</tr>
<tr>
<td>RFPM</td>
<td>Radial-flux permanent magnet</td>
</tr>
<tr>
<td>SEL</td>
<td>Specific electric loading</td>
</tr>
<tr>
<td>SMC</td>
<td>Soft Magnetic Composite</td>
</tr>
<tr>
<td>SMCs</td>
<td>Soft Magnetic Composites</td>
</tr>
<tr>
<td>WECS</td>
<td>Wind energy conversion system</td>
</tr>
<tr>
<td>WECSs</td>
<td>Wind energy conversion systems</td>
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</table>
## List of Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Unit</th>
<th>Definition</th>
</tr>
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<tbody>
<tr>
<td>$a$</td>
<td></td>
<td>Number of parallel paths</td>
</tr>
<tr>
<td>$A_{\text{cond}}$</td>
<td>$m^2$</td>
<td>Area of copper portion of stator conductor</td>
</tr>
<tr>
<td>$A_g$</td>
<td>$m^2$</td>
<td>Airgap area</td>
</tr>
<tr>
<td>$A_m$</td>
<td>$m^2$</td>
<td>Magnet area</td>
</tr>
<tr>
<td>$B_g$</td>
<td>$T$</td>
<td>Plateau value of airgap flux density</td>
</tr>
<tr>
<td>$B_{1\text{max}}$</td>
<td>$T$</td>
<td>Peak of fundamental component of airgap flux density</td>
</tr>
<tr>
<td>$B_{\text{max}}$</td>
<td>$T$</td>
<td>Maximum flux density</td>
</tr>
<tr>
<td>$B_r$</td>
<td>$T$</td>
<td>Remanent flux density of PMs</td>
</tr>
<tr>
<td>$B_{\text{sat}}$</td>
<td>$T$</td>
<td>Saturation flux density</td>
</tr>
<tr>
<td>$B_{\text{sat, rotor}}$</td>
<td>$T$</td>
<td>Saturation flux density of rotor core material</td>
</tr>
<tr>
<td>$B_{\text{sat, stator}}$</td>
<td>$T$</td>
<td>Saturation flux density of stator core material</td>
</tr>
<tr>
<td>$B_{1\text{max}}$</td>
<td>$T$</td>
<td>Peak flux density in stator tooth</td>
</tr>
<tr>
<td>$B_{y\text{max}}$</td>
<td>$T$</td>
<td>Peak flux density in stator yoke</td>
</tr>
<tr>
<td>$\cos\phi$</td>
<td></td>
<td>Rated power factor</td>
</tr>
<tr>
<td>$C_P$</td>
<td></td>
<td>Power coefficient or Aerodynamic efficiency</td>
</tr>
<tr>
<td>$C_{P\text{max}}$</td>
<td></td>
<td>Maximum power coefficient</td>
</tr>
<tr>
<td>$C_I$</td>
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<td>Torque coefficient</td>
</tr>
<tr>
<td>$C_{I\text{max}}$</td>
<td></td>
<td>Maximum torque coefficient</td>
</tr>
<tr>
<td>$C_\phi$</td>
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<td>Flux focusing factor</td>
</tr>
<tr>
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<td>$m$</td>
<td>Airgap diameter</td>
</tr>
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<td>$m$</td>
<td>Average airgap diameter of an AFPM stator core</td>
</tr>
<tr>
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<td>$m$</td>
<td>Inner diameter of AFPM stator core</td>
</tr>
<tr>
<td>$D_o$</td>
<td>$m$</td>
<td>Outer diameter of AFPM stator core</td>
</tr>
<tr>
<td>Symbol</td>
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<tr>
<td>$E_f$</td>
<td>$V$</td>
<td>- Excitation voltage per-phase</td>
</tr>
<tr>
<td>$f$</td>
<td>$Hz$</td>
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<tr>
<td>$h$</td>
<td></td>
<td>- Harmonic number</td>
</tr>
<tr>
<td>$h_{ry}$</td>
<td>$m$</td>
<td>- Height of rotor yoke</td>
</tr>
<tr>
<td>$h_s$</td>
<td>$m$</td>
<td>- Height of stator slot</td>
</tr>
<tr>
<td>$h_{sy}$</td>
<td>$m$</td>
<td>- Height of stator yoke</td>
</tr>
<tr>
<td>$I_a$</td>
<td>$A$</td>
<td>- Armature current per-phase</td>
</tr>
<tr>
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<td>$A$</td>
<td>- $d$-axis component of stator current</td>
</tr>
<tr>
<td>$i_q$</td>
<td>$A$</td>
<td>- $q$-axis component of stator current</td>
</tr>
<tr>
<td>$J$</td>
<td>$A/mm^2$</td>
<td>- Current density</td>
</tr>
<tr>
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<td></td>
<td>- Diameter coefficient of AFPM machine ($D_y/D_i$)</td>
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<td></td>
<td>- Eddy current loss constant</td>
</tr>
<tr>
<td>$k_h$</td>
<td></td>
<td>- Hysteresis loss constant</td>
</tr>
<tr>
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<td>- Form factor of excitation field</td>
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<td>- Aspect ratio coefficient ($L/D$)</td>
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<td>- Stacking factor of stator laminations</td>
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<tr>
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<td>- Axial length of stator core</td>
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<tr>
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<td>$m$</td>
<td>- Length of end winding</td>
</tr>
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<td>$m$</td>
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<td>- Total length of a phase winding</td>
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<tr>
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<td>$m$</td>
<td>- Axial length of stator core</td>
</tr>
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<td>- Effective axial length of an AFPM stator core</td>
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<tr>
<td>Symbol</td>
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</tr>
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<td>--------</td>
<td>----------</td>
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</tr>
<tr>
<td>$L_i$</td>
<td>$H$</td>
<td>- Leakage inductance</td>
</tr>
<tr>
<td>$L_m$</td>
<td>$H$</td>
<td>- Magnetising inductance</td>
</tr>
<tr>
<td>$L_q$</td>
<td>$H$</td>
<td>- $q$-axis synchronous inductance</td>
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<td>$H$</td>
<td>- Synchronous inductance</td>
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<tr>
<td>$m_{ac}$</td>
<td>$kg$</td>
<td>- Mass of active materials</td>
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<td>$kg$</td>
<td>- Mass of rotor core</td>
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<td>- Hysteresis loss coefficient</td>
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<tr>
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<td>- Number of winding layers</td>
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<td>turns</td>
<td>- Number of turns per phase</td>
</tr>
<tr>
<td>$N_r$</td>
<td>$rpm$</td>
<td>- Rotor shaft speed</td>
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<td>$n_r$</td>
<td>$rev/sec$</td>
<td>- Rotational speed</td>
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<tr>
<td>$N_t$</td>
<td>turns</td>
<td>- Number of turns per tooth</td>
</tr>
<tr>
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<td>- Pole pairs</td>
</tr>
<tr>
<td>$p_{ct}$</td>
<td>$W/m^3$</td>
<td>- Average eddy current loss density in stator tooth</td>
</tr>
<tr>
<td>$p_{cy}$</td>
<td>$W/m^3$</td>
<td>- Average eddy current loss density in stator yoke</td>
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<tr>
<td>$p_{ht}$</td>
<td>$W/m^3$</td>
<td>- Hysteresis loss density in stator tooth</td>
</tr>
<tr>
<td>$p_{hy}$</td>
<td>$W/m^3$</td>
<td>- Hysteresis loss density in stator yoke</td>
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<td>$P_{rt}$</td>
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<td>- Normalised rotor leakage permeance</td>
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<td>- Permeance coefficient</td>
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<td>- Total core loss</td>
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<td>$P_{Cu}$</td>
<td>$W$</td>
<td>- Copper loss</td>
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<tr>
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<td>- Friction and windage loss</td>
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<td>- Generator output power</td>
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<td>$W$</td>
<td>- Rotational loss</td>
</tr>
<tr>
<td>$P_{shaft}$</td>
<td>$W$</td>
<td>- Turbine / generator shaft power</td>
</tr>
<tr>
<td>$P_{windage}$</td>
<td>$W$</td>
<td>- Windage loss</td>
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<tr>
<td>$q$</td>
<td></td>
<td>- Slots/pole/phase</td>
</tr>
<tr>
<td>$R$</td>
<td>$m$</td>
<td>- Radius of turbine blades</td>
</tr>
<tr>
<td>$R_a$</td>
<td>$\Omega$</td>
<td>- Armature winding resistance per-phase</td>
</tr>
<tr>
<td>$r_s$</td>
<td>$\Omega$</td>
<td>- Armature winding resistance per-phase</td>
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<tr>
<td>Symbol</td>
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<td>---------</td>
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</tr>
<tr>
<td>$S$</td>
<td></td>
<td>Number of slots</td>
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<td>$VA$</td>
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</tr>
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<td>$A/m$</td>
<td>Specific electric loading (peak)</td>
</tr>
<tr>
<td>$SMI_{pk}$</td>
<td>$T$</td>
<td>Specific magnetic loading (peak)</td>
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<td>$Nm$</td>
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</tr>
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<td>$v_d$</td>
<td>$V$</td>
<td>$d$-axis component of stator voltage</td>
</tr>
<tr>
<td>$v_q$</td>
<td>$V$</td>
<td>$q$-axis component of stator voltage</td>
</tr>
<tr>
<td>$V_a$</td>
<td>$V$</td>
<td>Armature / terminal voltage per-phase</td>
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<tr>
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<td>$m^3$</td>
<td>Volume of PM material</td>
</tr>
<tr>
<td>$V_{rc}$</td>
<td>$m^3$</td>
<td>Volume of rotor core material</td>
</tr>
<tr>
<td>$V_{teeth}$</td>
<td>$m^3$</td>
<td>Volume of stator teeth</td>
</tr>
<tr>
<td>$V_{yoke}$</td>
<td>$m^3$</td>
<td>Volume of stator yoke</td>
</tr>
<tr>
<td>$w_o$</td>
<td>$m$</td>
<td>Width of stator slot opening</td>
</tr>
<tr>
<td>$w_s$</td>
<td>$m$</td>
<td>Width of a stator slot</td>
</tr>
<tr>
<td>$w_t$</td>
<td>$m$</td>
<td>Width of a stator tooth</td>
</tr>
<tr>
<td>$X_s$</td>
<td>$\Omega$</td>
<td>Synchronous reactance</td>
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<thead>
<tr>
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<th>Definition</th>
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<tbody>
<tr>
<td>$a$</td>
<td>Pole-arc to pole-pitch ratio</td>
</tr>
<tr>
<td>$\beta$</td>
<td>Pitch angle of turbine blades</td>
</tr>
<tr>
<td>$\varepsilon$</td>
<td>Ratio of rated excitation voltage to terminal voltage</td>
</tr>
<tr>
<td>$\Phi_p$</td>
<td>$Wb$ Flux per pole</td>
</tr>
<tr>
<td>$\eta_{gen}$</td>
<td>Efficiency of generator</td>
</tr>
<tr>
<td>$\mu_o$</td>
<td>Permeability of air</td>
</tr>
<tr>
<td>$\mu_r$</td>
<td>Relative permeability</td>
</tr>
<tr>
<td>$\rho_{air}$</td>
<td>$kg/m^3$ Density of air at the turbine height</td>
</tr>
<tr>
<td>$\rho_{pm}$</td>
<td>$kg/m^3$ Density of PM material</td>
</tr>
<tr>
<td>$\rho_{rc}$</td>
<td>$kg/m^3$ Density of rotor core material</td>
</tr>
<tr>
<td>Symbol</td>
<td>Unit</td>
</tr>
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</tr>
<tr>
<td>$\tau_c$</td>
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</tr>
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</tr>
<tr>
<td>$\tau_s$</td>
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<tr>
<td>$\sigma$</td>
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<tr>
<td>$\sigma_{Cu}$</td>
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<tr>
<td>$\lambda_s$</td>
<td>Wb-turns</td>
</tr>
<tr>
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<td>Wb-turns</td>
</tr>
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<td>Wb-turns</td>
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<tr>
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<tr>
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</tr>
<tr>
<td>$\omega_r$</td>
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Chapter 1

Introduction

1.1. Background

Due to a global concern about the rapid depletion of natural energy resources, renewable energy sources, such as wind, solar, biomass, geothermal and tidal have received renewed interest in recent years. Considerable research effort has therefore concentrated on developing medium to large-scale, commercial wind energy conversion systems, capable of interfacing directly to utility grids. Consequently, the literature on wind energy conversion is rich with research conducted on these systems. The published research includes the design, control and transient performance analysis of wind energy conversion systems, as well as the optimum sizing, costing and siting of these systems. Typical ratings of these systems range between 50 kW to several megawatts.

On the contrary, research and development efforts on small wind energy conversion systems (WECSs) have lagged behind. This is mainly due to the limited commercial potential envisaged by large corporations, as these systems are generally intended for end-user and rural applications, where they are interfaced to isolated loads or weak electrical grids. Since these applications are prevalent throughout Africa and in other
developing countries, a need therefore exists to develop low cost, robust, small wind energy conversion systems, capable of harnessing wind energy in areas with a low wind potential. This dissertation responds to this need by addressing issues relating to the design of small permanent magnet (PM) wind generators.
1.2. Literature Review

Wind energy conversion systems are classified here according to the generator technology employed in the system. In general, PM wind generators are used in variable speed, direct-drive WECSs, whilst induction generators are used in constant / semi-constant speed, geared systems. The power electronic converter topologies and control strategies associated with each of the generator technologies differ. An overview of selected literature available, mainly on PM wind generator technologies, is presented here. Aspects relating to the power electronic converters and control of PM wind generators are also discussed briefly.

The design configurations for PM wind generators found in the literature are mainly that of unconventional machine types. The main reason for this is the high number of poles required by low speed, directly coupled PM wind generators, in order to produce power at a frequency close to that of the utility grid.

The design considerations of low speed PM synchronous generators, intended for direct coupling to wind turbines, are presented in [1.1]. Detailed considerations leading to the optimum design of a 400 kW PM synchronous wind generator is presented. Four machine configurations are investigated. These include radial flux, axial flux, transverse flux and claw pole PM synchronous generator configurations. Moreover, two radially magnetised rotor configurations are considered. These include a surface PM design using rare earth PMs and a buried PM rotor design using ferrite PMs. Detailed comparisons are made between the resulting airgap flux density, copper loss, total magnet mass and material cost of the generator configurations. Based on the comparisons, the radial flux machine with buried ferrite PMs was chosen as a suitable configuration for the 400 kW PM synchronous wind generator. A design summary of the 400kW PM synchronous wind generator is presented. The generator uses a conventional continuous-core stator with a buried ferrite PM rotor design. Tapered pole pieces are used to separate the ferrite PMs as well as to direct flux toward the airgap. Moreover, small ferrite PMs are embedded beneath the tapered
pole pieces to prevent flux leakage toward the axis of the rotor, thereby contributing to the total airgap flux.

The modular design of a radially magnetised PM wind generator is presented in [1.2]. The machine rotor is assembled from several "rotor modules", joined at the $d$-axis. Each module uses a standard tangentially magnetised ferrite magnet block, with tapered pole pieces fixed on either side. The stator is assembled using several "stator modules", each consisting of simple E-cores carrying a single rectangular coil. A discontinuous stator is formed by the gaps between the E-cores, which can be adjusted to form a generator of the required diameter. The modular structure makes it possible to construct multi-pole PM wind generators for a range of power ratings by using the standard rotor and stator modules. A detailed comparison is made between the design summaries of a 400kW modular PM generator and that of a 400kW conventional, continuous stator-core, PM generator presented in [1.1]. Based on the comparisons, the modular design is shown to be easy to assemble, possesses low leakage reactances and also attenuates sub-harmonic components of the airgap flux. It is concluded that the modular design using readily available components reduces design and construction effort, whilst creating a PM wind generator with low reactances and high efficiency. However, additional loss mechanisms specific to the modular design, are yet to be identified, analysed and controlled.

The design and finite element analysis of an outer rotor PM wind generator is presented in [1.3]. The generator is intended for low speed, direct-drive, stand-alone wind energy conversion applications. Direct-drive wind generators require a large number of poles and therefore a large diameter at high cost in order to produce electricity at the normal operating frequency range (30-80Hz) of these turbines [1.3]. The outer-rotor construction offers an increased diameter of the rotor periphery, thereby enabling it to economically accommodate the high number of poles necessary for direct-drive applications. The important design principles associated with the outer-rotor construction are discussed together with the critical design constraints. These include the temperature rise and margin to permanent demagnetisation of the PMs. A simple magnetic equivalent circuit approach is used for the initial design and optimisation of the machine. A detailed finite element analysis is then applied to the
initial design in order to validate the design methodologies to predict the performance of the machine more accurately. Comprehensive experimental tests were conducted to verify the characteristics of a prototype generator. The open circuit voltage was found to be non-sinusoidal. This was attributed to the fully pitched concentrated coils used in the stator phase windings. The terminal voltage waveform of the machine did however become more sinusoidal as the machine was loaded with a resistive load. The efficiency of the machine was measured at 86% when supplying 20kW to a resistive load at 170rpm.

The average efficiency of three 500kW wind generator systems is compared in [1.4] and [1.5]. The systems include a conventional grid-connected 4-pole induction generator equipped with a gear, a variable speed synchronous generator equipped with a gear and a frequency converter, and a directly-driven variable speed generator equipped with a frequency converter. It was shown that a variable speed generator system can, on average, be as efficient as the constant speed system, even though its efficiency at rated load is much lower. This is due to the reduction in core losses of the generator and rotational losses of the gear and generator at low speeds in a variable speed system. It was also found that the direct-driven variable speed system was more efficient than the conventional gear and generator. This is due to the elimination of the gearbox losses and that a large part of the generator losses at rated load are copper losses, which are reduced at low wind speeds.

The modular design of an axial-flux PM synchronous wind generator is presented in [1.7]. The modular design offers similar advantages as that presented in [1.2]. In addition, generator modules can be stacked in an axial direction in order to increase the power rating of the PM wind generator system. The PM rotor is constructed by mounting axially magnetised rotor poles on a nonmagnetic disk. The rotor poles are constructed by sandwiching axially magnetised PMs between two rotor core stacks. The airgap flux density can be varied by altering the effective magnet area. This is achieved by altering the ratio of the magnetic surface area to the pole surface area. Double-sided stators are used in the PM wind generator for easy stacking to achieve increased power ratings. The stator is concentrically wound, similar to a toroid. This configuration simplifies the assembly of the stator and the insertion of the stator
windings into slots. It was concluded that the mechanical design of the machine presented in this paper was not optimised.

Another concentrically wound axial flux machine called a “Torus” is presented in [1.8] and [1.9]. The stator of this machine consists of a strip-wound stator core which carries a slotless torroidal winding. The rotor comprises two discs carrying axially magnetised PMs. High magnetic loading is achieved with the use of NdFeB PMs on the rotor. Furthermore, high electric loading is made possible through good cooling of the stator winding provided by the rotor discs, which act naturally as fans. In [1.9], the design, construction and testing of a 5kW, 200rpm “Torus” PM wind generator is discussed. It was found that the attractive force between rotor PMs and the stator core was excessive. This required special care during assembly of the machine. The excitation voltage induced in a stator phase winding was found to be very close to sinusoidal. This result was different to the trapezoidal emf waveform induced in the phase windings of the “Torus” machine in [1.8]. The difference was attributed to the trapezoidal PMs with constant angular width used in [1.8] compared to the variable angular width of the PMs used in [1.9].

The potential application of several wind generator topologies to direct-drive wind turbines was investigated in [1.10]. A comparison of 60 different machine prototypes was presented. The comparison was based on the torque density and cost/torque ratio of the different machine topologies. It was found that radial-flux PM machines with ferrite PMs in flux concentrating designs are not superior to radial-flux designs with surface mounted PMs. Axial-flux machines with a torus stator core have double the torque density of equivalent radial-flux designs. However, the large PM thickness required in the torus design results in double the cost/torque ratio of and equivalent radial-flux PM machine. A radial-flux machine with surface PMs can be built with twice the torque density and half the cost/torque ratio by using the transverse-flux PM (TFPM) machine topology. Switched-reluctance machine designs resulted in a torque density and cost/torque ratio equivalent to that of radial-flux PM machine designs with surface PMs. However, switched-reluctance machines can be built with 50% higher torque density than radial-flux machines, but at the expense of 4 times the cost/torque ratio. An axial-flux machine with interior PMs (AFIPM) showed good
characteristics which were comparable to those of a transverse-flux PM machine design. A transverse vernier individual hybrid reluctance machine (TVIHRM) topology did not provide good torque density. The study presented in [1.10] concluded that the TFPM and the AFIPM topologies should be considered to meet the demands of applying PM wind generators to direct-drive wind turbine applications.

An axial-flux PM (AFPM) machine with slots was compared to a radial-flux PM (RFPM) for the application as direct-drive wind generators in [1.11]. Optimisation of the two machine types was performed with respect to cost/torque. The optimisation for maximum torque/volume was also investigated. It was shown that the RFPM machine had lower cost/torque and lower torque/volume than the AFPM machine design with slots. The effect of the diameter ratio was investigated for AFPM machines. It was found that a diameter ratio of 0.88 results in an AFPM machine with the lowest cost/torque. However, the end windings created significant losses for high diameter ratios. A good compromise between high efficiency and low cost/torque was achieved with a ratio of 0.78. A comparison between the optimal cost/torque of a RFPM machine and that of an AFPM machine with slots was also performed. It was found that an AFPM machine with a diameter ratio of 0.78 had a cost/torque of about 22% higher than that of a RFPM machine of the same diameter and rated torque. The cost/torque of an AFPM machine with a diameter ratio of 0.88 was about 12% higher than that of a RFPM machine. The torque/volume of the two machine types was also compared. It was found that the torque/volume of an AFPM machine was 2 - 5 times higher than that of a RFPM machine. This was mainly attributed to the shorter axial length of an AFPM machine topology compared to that of an equivalent RFPM machine design. It was finally concluded that a RFPM machine topology still appears as a better choice for direct-drive wind generators, than an AFPM machine with slots.

An axial-flux PM wind generator with a toroidal stator core is examined in [1.12]. The mechanical and electromagnetic designs are discussed and experimental results are presented from 5kW and 10kW prototypes. A 100kW generator is also designed and analysed. It was concluded that special attention should be given to the mounting of magnets, the stator core and to the selection of materials for the support structure.
Chapter 1: Introduction

The analysis and performance of a three-phase generator with an inset NdFeB magnet rotor is presented in [1.13]. The inverse saliency ($L_d < L_q$) present in this machine due to the rotor configuration, results in an improved voltage characteristic when the generator supplies an isolated, unity power factor load. An analytical technique is used to show the existence of two values of load current at which zero voltage regulation is achieved. The effect of the armature resistance, saliency ratio, and operating speed on the achievement of zero voltage regulation is investigated. Finite-element analysis is used to determine the generator parameters. The analytical analyses are validated by experiments conducted on a 2.5-kVA prototype generator.

The design, analysis and performance of an axial flux PM machine with an ironless stator is presented in [1.14],[1.15]. The coreless machine topology has a large effective airgap length and no core losses. A multi-variable optimisation algorithm that uses a lumped parameter magnetic circuit model and finite element analysis was used with to determine an optimised machine design. A 3.5 kW machine was prototyped and tested in motor and generator modes of operation. The airgap flux density waveform was slightly distorted by armature reaction. However, the armature reaction effect only caused a small reduction in the average torque. The prototype operated with low noise, zero cogging torque and reduced copper losses for a certain power level at the expense of a large magnet volume.

The design and analysis of a coreless axial-flux PM generator is presented in [1.16],[1.17]. A multi-dimensional optimisation procedure is introduced, which uses a combination of analytical and finite element techniques to design the machine. Two optimisation algorithms were investigated. These include: Powell's method and the population-based incremental learning (PBIL) algorithm. The PBIL algorithm found slightly better designs in all cases. Good designs with lower eddy-current losses, high efficiency, high power-to-mass ratio, and low cost were obtained by minimising the PM material. A 150kW prototype was successfully built and tested. The low phase inductance of the coreless design resulted in an almost linear variation of the terminal voltage with load current. Good agreement was achieved between predicted and measured results and the stator voltage and current waveforms were close to sinusoidal when supplying a resistive load.
Chapter 1: Introduction

A method of estimating the eddy current losses in a coreless axial-flux PM generator is presented in [1.18]. Closed-form analytical eddy current loss formulae are used together with a two dimensional FE analysis to estimate the losses. A multilayer and multi-slice technique is devised to account for three dimensional effects and non-sinusoidal field distributions in an axial-flux PM machine. Experimental tests conducted on a coreless machine confirmed the accuracy of the proposed method.

An extensive comparison of PM wind generator topologies was presented in [1.19],[1.20],[1.21]. Seven PM wind generator configurations were designed with power ratings of: 1, 10, 20, 50, 100, 150 and 200kW. The designs were optimised by means of a multi-objective machine design optimisation routine. The machine topologies considered in the study were: an inner and outer rotor radial-flux configuration, a double stator and double rotor axial-flux configuration, a single stator and single rotor axial-flux configurations and a torus configuration. The comparisons were made on the basis of torque density, active material mass, outer radius, axial length, volume and efficiency. The double-rotor axial-flux configuration was shown to have the highest torque density and therefore chosen for prototyping. A new Soft Magnetic Composite (SMC) material was used for the stator core of the prototype of the PM wind generator [1.19],[1.22]. The measured efficiency of the prototype was relatively low and the design suffered from high cogging torque.

PM wind generators are designed for direct coupling to a wind turbine and therefore eliminate the need for a gearbox in the WECS. However, a power electronic converter is required in order to interface these machines with the utility supply. Options for interfacing variable speed PM wind generators to the utility grid are presented in [1.23]. With a PM generator directly coupled to the wind turbine, its rotational speed and hence output voltage and frequency is dependent on wind speed and electrical loading. Three methods are proposed for interfacing a variable speed PM wind generator to the utility grid. These include a VSI with sinusoidal PWM switching, a DC/DC converter interposed between the rectifier and a VSI and a line-commutated SCR-CSI. The principal features of the three interface options are summarised, together with techniques for controlling real and reactive power flow between the PM
wind generator and the utility grid. Control techniques are also discussed for extracting the maximum power from the wind generator at varying wind speeds.

Current commercial power electronic converters for small PM wind generators allow these machines to be interfaced directly with AC utility grids or autonomous DC/AC electrical grids. The utility grid interface can be implemented with or without battery storage. Here, the generator output is generally rectified by means of a three-phase diode bridge, then boosted and regulated to a suitable DC voltage by means of a boost converter and finally inverted to an AC voltage which conforms to the utility grid. Grid-tied systems of this type are becoming increasingly popular as urban turbines. Battery storage can be eliminated from these grid-tied systems if backup power is not required in the event of a loss of the grid coinciding with a period of low wind speed. The control of these systems features maximum power point tracking and hence maximum utilisation of prevailing winds.

In the autonomous DC/AC grid interface system, the generator output is rectified, then boosted to a DC voltage level required to maintain the charge on batteries and finally inverted to AC. Most commercial systems make use of a dump load to limit the power from the generator during high operating wind speed conditions.

Commercial power electronic converters are available for the afore-mentioned small wind energy conversion systems from the following manufacturers:

Magnetek (http://www.alternative-energies.com/default.htm),
Xantrex (http://www.xantrex.com/),
Sungrow (http://www.sungrow.cn/)
1.3. Research Questions and Objectives

The main objective of this dissertation is to contribute knowledge toward the design of small PM wind generators, which includes the application of soft magnetic composite (SMC) materials to the design of these machines.

This objective is achieved through detailed consideration of the following research questions associated with this dissertation:

- What are the current technological trends in small wind energy conversion systems?

- How can the design of a small wind generator be adapted for operation with a new low-speed, multi-blade, low solidity wind turbine?

- Which control strategies will ensure maximum utilisation of prevailing winds, whilst maintaining good overall system performance?

- How can the design of a small wind generator be optimised to ensure maximum utilisation of prevailing winds?

- Can Soft Magnetic Composite (SMC) materials be successfully applied to the design of small PM wind generators?

- How can a small wind generator be designed for a new turbine with counter-rotating blades?
Chapter 1: Introduction

The secondary objectives of this dissertation include the following:

- Identifying the current technological trends for electric drives of small wind energy conversion systems. More specifically, the common generator and accompanying power electronic converter configurations are identified, together with aspects of their control.

- Analysing the effect of changes to the design of a small PM wind generator and then adapting it for operation with a new low-speed, multi-blade, high solidity wind turbine.

- Investigating an alternative method of operation of a PM wind generator at a tip speed ratio which ensures that maximum shaft torque is captured as opposed to maximum power.

- Investigating a design procedure for a PM wind generator, for optimal performance over a wide range of operating speeds and loads.

- Improving on the design of a PM wind generator, by using a new Soft Magnetic Composite (SMC) material for its stator core.

- Assessing a new PM wind generator topology for operation with a new turbine with contra-rotating blades.
1.4. Dissertation Overview and Contributions

The research methods associated with this thesis are mainly analytical and experimental. The performance and design of PM wind generators are modelled analytically. The models are validated experimentally and through simulation with a Finite Element software package. The models are used to analyse the performance and to optimise the design of PM wind generators. A prototype of an SMC-based wind generator was built and thoroughly tested in a laboratory.

The specific contributions made by this thesis include the following:

- The problem was addressed of adapting a PM wind generator, intended for high-speed operation with a small 3-bladed wind turbine, for low speed operation with a new multi-blade, high solidity turbine. The new wind generator was required to deliver the same rated power as the original machine, but at 45% of the original shaft speed, under rated wind speed conditions. This requirement translates to a 122% increase in the developed torque and hence a 122% increase in the rotor volume \(D^2l\) of the machine. A complete redesign of the machine would ordinarily be necessary to meet these stringent requirements. However, this research offered an alternative approach to a complete redesign of the machine. It addressed the problem of adapting an existing wind generator to meet the new requirements, through carefully analysing the effects of several minor changes to the machine design, before selecting a new design specification for the machine. In so doing, the new machine fully exploited existing WECS manufacturing infrastructure and components, in an effort to reduce the energy costs associated with the generators.

- The presence of parasitic torques in PM wind generators results in a reduction of the overall response time of the Wind Energy Conversion System (WECS), as the turbine shaft torque is required to overcome the parasitic torques in addition to the electromagnetic torque of the generator. This effect is
Chapter 1: Introduction

exacerbated at low speed operation, which is typical for these direct-coupled WECS, where the shaft torque reduces to levels comparable with the parasitic torques. A method of increasing the shaft torque of the turbine was investigated, in an effort to improve the response time of the WECS. This was achieved through an alternative method of operation of the turbine at maximum power capture. The new method considers variable speed operation of the turbine at a tip speed ratio, which ensures that maximum shaft torque is captured. Moreover, this research compared the performance of a small PM wind generator operated at maximum power capture to that of maximum torque capture. The losses, efficiency and annual energy output of the generator was determined and compared for the different operating regions of a turbine.

- A direct-drive WECS with fixed-pitch blades is typically operated at variable speeds, which offers the advantage of maximising the energy capture from prevailing winds. However, a consequence of variable speed operation of a WECS is that the generator does not operate for substantial periods at its rated operating point (i.e. rated speed and load). With conventional machine design procedures producing high absolute efficiencies at the rated operating point, the energy capture of a variable speed WECS is therefore naturally reduced due to low generator efficiency at operation away from the designed operating point. A design procedure is investigated for a permanent-magnet (PM) wind generator, which optimises its performance over a wide range of operating speeds and loads. This was achieved by using an algorithm to find the combination of generator design variables, which optimises its performance when operated through the normal operating regions of a variable speed wind turbine.

- The design and prototyping of an axial-flux PM wind generator with a composite SMC/steel stator core is presented. The generator is intended for a new turbine with contra-rotating blades. The machine topology has SMC teeth fitted into a laminated silicon steel stator yoke. A simple pressed fit and wedge/retaining ring arrangement is proposed for fixing the SMC teeth into
the yoke. The machine is sized based on wind speed requirements and SMC core samples available for the research. Preformed coils are used to form a concentrated, non-overlapping stator winding, which are inserted from the stator yoke side. Consequently, the slot openings are small, thereby reducing harmonics associated with the slot openings. The SMC cores are machined by means of a cost effective and easily accessible process using an end-mill. A discussion is provided of the detrimental effect of this machining process on the SMC core. However, a chemical processing technique is investigated for treating the smear of the iron particles on the machined surfaces of the SMC core. Microscopic images are shown to illustrate the successful elimination of the smear on the machined surfaces. Assembly of the prototype is discussed in detail and experimental results are provided.
1.5. Scope and Limitations

This dissertation is limited to the electromagnetic design and analysis of small PM wind generators. The range of small PM machines under consideration is confined to: $50\text{W} - 20\text{kW}$. These machines are generally not mounted in a nacelle and are exposed to the environment. Thermal design aspects were therefore considered to be less important provided that the electric loadings of the machines are reasonable.

1.6. Structure

An analytical model of a radial-flux PM wind generator is formulated in chapter 2. The problem of adapting a PM wind generator for a multi-blade, high solidity turbine is addressed in chapter 3. Here, the model formulated in chapter 2 is used to evaluate the effect of design changes on the performance of the wind generator. The operation of a turbine at a tip speed ratio, which ensures that maximum shaft torque is captured, is addressed in chapter 4. The design procedure for a PM wind generator which optimises its performance over a wide range is presented in chapter 5. The design and prototyping of an axial-flux PM wind generator with a composite SMC/steel stator core is presented in chapter 6. Conclusions and recommendations are finally drawn in chapter 7. The author's publications are then listed in appendix A.
1.7. References


Chapter 2

Analytical model of a PM wind generator

2.1. Overview

An analytical model of a PM wind generator is formulated in this chapter. The model links the mechanical design specification of the machine to its equivalent circuit parameters, terminal voltage characteristic and losses. Experimental validation of the model is provided.
2.2. Introduction

An analytical model of a small PM wind generator is presented in this section. The model relates the mechanical design specifications of the machine to its electrical equivalent circuit parameters and performance, as shown in Fig. 2.1. It neglects saturation of the magnetic circuit, which is not considerable in the machine under investigation; i.e. a radial-flux machine with surface mounted magnets. The analytical model is used in conjunction with the terminal voltage characteristic and operating power capability of a PM wind generator, to evaluate its performance in subsequent chapters. Experimental validation of the model is provided by considering a 3.5kW, 8 pole, three-phase PM synchronous machine.

Fig. 2.1. Link between analytical model, design specifications and equivalent circuit
2.3. Analytical Model of a Radial-Flux PM Machine

The analytical model of a radial-flux PM synchronous machine is formulated here. The machine has radially magnetised NdFeB PMs mounted on the surface of a solid mild-steel rotor core. The resulting airgap flux density produced by the PMs is approximately rectangular in shape. The machine stator has a distributed, double-layer three-phase winding accommodated in semi-closed oval slots.

2.3.1. Excitation Voltage

The rms value of the fundamental component of the excitation voltage induced in a phase winding of the machine can be expressed as [2.4]:

\[ E_f = \frac{2\pi}{\sqrt{2}} fN_{ph} K_{w1} \phi_p \]  \hspace{1cm} (2-1)

where \( K_{w1} \) is the fundamental harmonic winding factor, \( \phi_p \) is the flux per pole due to the fundamental space harmonic component of the excitation flux density distribution, \( f \) is the frequency and \( N_{ph} \) is the number of turns per phase.

The fundamental harmonic winding factor of a three-phase machine can be written in terms of the fundamental distribution and pitch factors as [2.4]:

\[ K_{w1} = K_{d1} \cdot K_{p1} = \frac{\sin(\pi \cdot 6)}{q \sin(\pi / 6) q} \cdot \sin \left( \frac{\tau_c}{2} \right) \]  \hspace{1cm} (2-2)

where \( q \) is the number of slots/pole/phase, i.e. \( q = S/2p/3 \) and \( \tau_c \) is the coil pitch.

The flux per pole can be expressed as [2.4]:

\[ \phi_p = B_{l_{max}} l D / p \]  \hspace{1cm} (2-3)

where \( B_{l_{max}} \) is the peak value of the fundamental space harmonic component of the excitation flux density distribution.
Furthermore, $B_{\text{imax}}$ can be related to the plateau value $B_p$, of the rectangular airgap flux density distribution produced by the PMs, as follows [2.2]:

$$B_{\text{imax}} = k_f B_p$$  \hspace{1cm} (2-4)

where $k_f$ is the form factor of the excitation field.

In the case of a single smooth PM per pole, $k_f$ can be expressed as [2.2]:

$$k_f = \frac{4}{\pi} \sin\left(\frac{\alpha \pi}{2}\right)$$  \hspace{1cm} (2-5)

where $\alpha$ is the pole arc to pole pitch ratio of the magnets.

The plateau value of the excitation flux density distribution can be related to the remanent flux density and the relative permeability of the PMs by the following expression [2.1],[2.3]:

$$B_p = \frac{l_m / \mu_r}{l_m / \mu_r \cdot \sqrt{C_p + K_c l_s (1 + p_{T})}} B_r$$  \hspace{1cm} (2-6)

where $C_p$ is the flux focusing factor, $K_c$ is Carter's coefficient and $p_{T}$ is the normalised rotor leakage permeance. The range of values for $p_{T}$ is typically 0.05 - 0.2 [2.1].

The flux-focusing factor is defined as the ratio of the PM pole area to that of the airgap across which the excitation flux traverses, i.e. $C_p = A_e / A_s$. Now, with the PMs mounted on the rotor surface, the outer PM pole area is larger than the inner area. An average PM pole area can therefore be used for simplicity, resulting in [2.1]:

$$A_e = \frac{\alpha \pi l_e}{2p} (D - 2l_s - l_m)$$  \hspace{1cm} (2-7)

The airgap area across which the excitation flux per pole traverses would be larger than that of the PM pole area. This is due to fringing of the excitation flux on the edges of the PM pole. A simple approximation to the airgap area, which accounts for this fringing effect, can be expressed as [2.1]:

23
\[ A_s = \left[ \frac{\alpha \pi}{2p} (D - l_s) + 2l_s \right] (l + 2l_s) \]  

(2-8)

Carter’s coefficient is introduced in (2-6) to modify the mechanical airgap clearance \( l_g \), to account for slotting of the stator. The presence of stator slots causes the flux crossing the airgap to contract toward the stator teeth, thereby increasing the airgap reluctance [2.4]. This is accounted for by a lengthened airgap, obtained by means of Carter’s coefficient, which can be expressed as [2.4]:

\[ K_c = \frac{\tau_s}{\tau_s - \gamma w_o} \]  

(2-9)

where \( \tau_s = \pi D/S \) is the stator slot pitch, \( w_o = h_{1a} \) is the width of the stator slot opening and \( \gamma \) is a function, dependent on the width of the slot opening and the mechanical airgap clearance.

The function \( \gamma \) can be expressed as [2.2]:

\[ \gamma = \frac{4}{\pi} \left[ \frac{w_o}{2l_s} \text{arctan} \left( \frac{w_o}{2l_s} \right) - \ln \sqrt{1 + \left( \frac{w_o}{2l_s} \right)^2} \right] \]  

(2-10)

2.3.2. Synchronous Reactance

The effective airgap in a PM machine with magnets mounted on the rotor surface can be considered constant and relatively large. This is due to the relative permeability of the PM material being close to unity. The \( d \) and \( q \)-axis synchronous reactances are consequently identical in this machine. The synchronous reactance of the machine can be written in terms of the magnetising \( (X_m) \) and leakage \( (X_l) \) reactances, as:

\[ X_s = X_m + X_l \]  

(2-11)
The magnetising reactance can be expressed as [2.1]:

\[ X_m = \frac{6 \mu_0 DfK w_p^2 N_{ph}^2}{P^3 (K_A l_s + l_s / \mu_r)} \]  

(2-12)

where the term \( K_A l_s + l_s / \mu_r \) in (2-12) represents the effective airgap length in the path of the magnetising flux. This includes the mechanical airgap clearance modified by Carter's coefficient to account for slotting and the radial thickness of the PMs.

The leakage reactance can be written in terms of the specific permeance coefficients associated with the dominant leakage flux paths of the stator, i.e. the slot, tooth-top and winding overhang leakage flux paths, as [2.3],[2.4]:

\[ X_l = 4 \pi \mu_0 f \frac{N_{ph}^2 I}{pq} (\lambda_{slot} + \lambda_{tooth-top} + \lambda_{overhang}) \]  

(2-13)

The specific permeance coefficient associated with the slot leakage flux path for the semi-closed oval slots of the machine under investigation can be expressed as [2.5]:

\[ \lambda_{slot} = 0.1424 + \frac{2h_{11}}{3(b_{11} + b_{12})} + \frac{2h_{12}}{b_{12} + h_{13}} + \frac{2h_{13}}{b_{13} + h_{14}} + \frac{h_{14}}{b_{14}} \]  

(2-14)

The specific permeance coefficient associated with the tooth-top leakage flux path can be expressed as [2.2]:

\[ \lambda_{tooth-top} = \frac{5l_s/w_o}{5 + 4l_s/w_o} \]  

(2-15)

The specific permeance coefficient associated with the winding overhang can be expressed empirically as [2.5]:

\[ \lambda_{overhang} = 0.47q \frac{l_{end}}{l} - 0.3q \frac{r_p}{l} \]  

(2-16)

where \( l_{end} \) is the length of a single end connection of the armature winding and
\[ \tau_p = \frac{\pi D}{2p} \] is the pole pitch.

The length of a single end connection can be expressed empirically as \([2.2]\):

\[ l_{\text{end}} = (0.083p + 1.217) \cdot \frac{pD + h}{2p} + 0.02 \] (2-17)

where \( h \) is the height of a stator tooth.

2.3.3. Armature Resistance

The per-phase resistance of the stator winding can be estimated on the basis of the total length of a phase winding. This can be expressed as \([2.2]\):

\[ l_{\text{ph-winding}} = 2(l + l_{\text{end}}) \cdot N_{\text{ph}} \] (2-18)

Furthermore, the per-phase resistance of the stator winding, neglecting the skin effect, can be expressed as \([2.2]\):

\[ R_o = \frac{l_{\text{ph-winding}}}{a \cdot \sigma_{\text{cu}} \cdot A_{\text{winding}}} \] (2-19)

2.3.4. Machine Losses and Performance

The efficiency of the PM synchronous machine operated as a generator can be expressed as \( \eta = \frac{P_L}{P_{\text{shaf}}} \), where \( P_{\text{shaf}} \) is the input mechanical power applied to the shaft of the machine and \( P_L \) is the total real power delivered to its load. The input mechanical and load powers can further be related by:

\[ P_{\text{shaf}} = P_L + P_{\text{Cu}} + P_{\text{rot}} + P_{\text{core}} \] (2-20)

where \( P_{\text{Cu}} \) is the total stator copper losses, \( P_{\text{rot}} \) is the total rotational losses and \( P_{\text{core}} \) is
the total core losses in the machine.

The total real power delivered to a load on the generator can be expressed as [2.4]:

$$ P_L = 3V_s I_s \cos \phi $$

(2-21)

The total stator copper losses can be expressed as:

$$ P_{Cu} = 3I_s^2 R_u $$

(2-22)

The total rotational losses in the machine consist of friction losses in the bearings $P_{friction}$ and windage losses $P_{windage}$. The total rotational losses can therefore be written in terms of its component losses as $P_{rot} = P_{friction} + P_{windage}$. Moreover, the friction losses can be expressed as [2.2]:

$$ P_{friction} = k_f m_r \frac{60f}{p} \cdot 10^{-4} $$

(2-23)

where the factor $k_f$ is typically in the range 1-3 and $m_r$ is the mass of the rotor.

The mass of the rotor can be estimated in terms of the mass densities of the rotor core material $\rho_{rc}$ and that of the PM material $\rho_{pm}$, as $m_r = \rho_{rc} V_{rc} + \rho_{pm} V_{pm}$. The rotor core and PM material volumes ($V_{rc}$ and $V_{pm}$) can further be expressed as:

$$ V_{rc} = \frac{\pi l}{4} \left(D - 2l_e - 2l_u\right)^2 $$

$$ V_{pm} = \alpha \pi l \left(D - 2l_e - l_u\right) l_u $$

(2-24)

Furthermore, the windage losses can be approximated by the following expression [2.2]:

$$ P_{windage} = 2 \left(D - 2l_e\right)^2 l \left(\frac{60f}{p}\right)^2 \cdot 10^{-4} $$

(2-25)
Chapter 2: Analytical model of a PM wind generator

The total core losses in the machine can be estimated on the basis of the hysteresis loss densities in the stator teeth and yoke \( (p_{ht}, p_{hy}) \), and the average eddy current loss densities in the stator teeth and yoke \( (\overline{p}_e, \overline{p}_o) \). The total core losses can therefore be expressed as \([2.3],[2.6]\):

\[
P_{\text{core}} = V_{\text{teeth}} \left( p_{ht} + \overline{p}_e \right) + V_{\text{yoke}} \left( p_{hy} + \overline{p}_o \right) \tag{2-26}
\]

where \( V_{\text{teeth}} \) and \( V_{\text{yoke}} \) are the volumes of the stator teeth and yoke, respectively.

The stator teeth and yoke volumes in a small machine with parallel-sided teeth can be estimated as follows \([2.3],[2.6]\):

\[
V_{\text{teeth}} = \pi lDh_s/\tau_s \quad V_{\text{yoke}} = \pi l(D + 2h_s + h_y)h_y \tag{2-27}
\]

The hysteresis loss densities in the stator teeth and yoke can be expressed as \([2.3],[2.6]\):

\[
p_{ht} = k_h\omega_s B_{\text{max}}^n \\
p_{hy} = k_h\omega_s B_{\text{y,max}}^n \tag{2-28}
\]

where \( k_h \) and \( n \) are hysteresis loss constants for the stator silicon sheet steel, \( \omega_s = 2\pi f \), \( B_{\text{max}} \) and \( B_{\text{y,\text{max}}} \) are the peak flux densities in the stator teeth and yoke, respectively.

The peak flux densities in the stator teeth and yoke can be related to the plateau value of the rectangular excitation flux density waveform as follows \([2.3],[2.6]\):

\[
B_{\text{max}} = B_s \tau_s/w_s \\
B_{y,\text{max}} = B_s \alpha \tau_p/2h_y \tag{2-29}
\]
Chapter 2: Analytical model of a PM wind generator

The average eddy current loss densities in the stator teeth and yoke can be expressed as [2.3],[2.6]:

\[
\begin{align*}
\overline{P_n} &= \frac{2k_e D}{\pi p_w} \left( \omega, B_{r\text{max}} \right)^2 \\
\overline{P_y} &= \frac{8k_e}{\alpha \pi} \left( \omega, B_{y\text{max}} \right)^2
\end{align*}
\]  

(2-30)

where \( k_e \) is the eddy current loss constant for the stator silicon sheet steel.
2.4. Experimental Verification of the Analytical Model

The analytical model of the PM synchronous machine is verified in this section before it can be used to investigate the effect of design changes on the performance of the machine. This is achieved by comparing the calculated equivalent circuit parameters and power losses of a test machine, to the values measured by laboratory experiments.

The design specifications of the test machine used to verify the analytical model are summarised in Table 2.3 of the Appendix. The test machine did however have 6 discrete 4x10x50mm NdFeB PMs per pole with a 1mm spacing between PMs, instead of a single smooth PM assumed in the analytical model above. The form factor of the excitation field (2-5), was thus modified to account for the discrete PMs per pole [2.2], [2.5]. The resulting rms value of the fundamental excitation voltage calculated for this machine was compared with that predicted by a Finite Element Analysis (FEA) and the measured voltage at various speeds. This is illustrated in Fig. 2.2.

The calculated and measured flux per pole, armature resistance and excitation voltage at 50Hz is shown in Table 2.1.

![Fig. 2.2. Comparison of measured and calculated rms value of fundamental excitation voltage at various rotor speeds (N_r)](chart.png)
The variation of calculated and measured core and rotational losses with speed is illustrated in Fig. 2.3.

From the results presented in this section it can be seen that good agreement exists between the calculated and measured equivalent circuit parameters of the test machine, thereby verifying the analytical model of the PM Synchronous machine presented in the previous section.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Measured</th>
<th>Analytical</th>
<th>FEA</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\phi_p [\text{wb}]$</td>
<td>0.002264</td>
<td>0.002293</td>
<td>0.002303</td>
</tr>
<tr>
<td>$E_f [\text{V}]$</td>
<td>258.77</td>
<td>262.18</td>
<td>263.13</td>
</tr>
<tr>
<td>$R_a [\Omega]$</td>
<td>1.317</td>
<td>1.571</td>
<td>-</td>
</tr>
</tbody>
</table>

**Fig. 2.3.** Variation of core and rotational losses with speed
2.5. Terminal Voltage Characteristic and Operating Conditions of a Small PM Wind Generator

In this section, the equivalent circuit parameters of a small PM wind generator, which are obtained from its analytical model, are used in conjunction with its terminal voltage characteristic and load power requirement, to determine the operating armature current of the machine. The current obtained in this manner is then used to evaluate the performance of the generator at various speeds.

In general, small PM wind generators are connected to power electronic converters of varying sophistication. These may vary from simple rectifiers to converters with elaborate control strategies, capable of full four-quadrant operation. In this chapter, it is assumed that the generator is connected to a power electronic converter, which operates as an active rectifier. The armature current of the generator is thus assumed sinusoidal and controlled by the converter. Moreover, the perceived load power factor at the terminals of the machine is also controlled by means of the converter. Under steady-state conditions, the converter can therefore be fully characterised by the generator armature current \( I_\alpha \omega \phi \) demanded by a fictitious load \( Z_L \), connected to each phase of the generator. This configuration resembles that of a synchronous machine connected to an isolated load. The steady-state terminal voltage characteristic of an isolated PM synchronous generator is therefore analysed in this section together with its operating power capability.

2.5.1. Terminal Voltage Characteristic

The dependence of the terminal voltage characteristic of an isolated PM synchronous generator on its equivalent circuit parameters will be highlighted in this section. The steady-state terminal characteristic of the generator can be determined by considering its per-phase equivalent circuit and phasor diagram illustrated in Fig. 2.4 and Fig. 2.5, respectively. The figures relate to a machine with negligible saliency, operating at a leading power factor and constant speed.
Chapter 2: Analytical model of a PM wind generator

Fig. 2.4. Per-phase equivalent circuit of a synchronous generator

Fig. 2.5. Phasor diagram of synchronous generator operating at leading power factor

With the aid of the phasor diagram in Fig. 2.5, the magnitude of the excitation voltage phasor can be expressed as:

\[ E_f^2 = (V_a + I_a R_a \cos \phi - I_a X_a \sin \phi)^2 + (I_a R_a \sin \phi + I_a X_a \cos \phi)^2 \]  \hspace{1cm} (2-31)

The terminal voltage of an isolated PM synchronous generator operating at a leading power factor can be written in terms of its equivalent circuit parameters by solving for \( V_a \) from (2-31). This results in:

\[ V_a = \sqrt{E_f^2 - (I_a X_a \cos \phi + I_a R_a \sin \phi)^2 + I_a X_a \sin \phi - I_a R_a \cos \phi} \]  \hspace{1cm} (2-32)

33
Furthermore, (2-32) is applicable for unity power factor operation and can be made applicable for lagging power factor operation by simply substituting negative power factor angles for lagging power factors. Equation (2-32) can be used to plot a family of curves to illustrate the variation of the generator terminal voltage with equivalent circuit parameters. Such curves showing the variation of terminal voltage with armature current are illustrated in Fig. 2.6, for a machine operating at various load power factors and rated speed.

![Graph showing terminal voltage characteristic of an isolated PM Synchronous generator at various load power factors and rated speed](image)

Fig. 2.6. Terminal voltage characteristic of an isolated PM Synchronous generator at various load power factors and rated speed

The relative slopes of the curves in Fig. 2.6 can be understood intuitively by considering an alternative expression for the terminal voltage of the generator. By inspection of the per-phase equivalent circuit of the machine, as illustrated in Fig. 2.4, the terminal voltage can alternatively be expressed as a function of the load impedance $Z_L$, as:

$$V_s = \frac{|Z_L|}{|R_s + jX_s + Z_L|} E_f$$  

(2-33)

where the load impedance $Z_L = R_L \pm jX_L$, has a positive reactive component for inductive loads and a negative reactive component for capacitive loads.
Equation (2-33) can be rewritten as:

\[ V_a = \sqrt{\frac{R^2 + X^2}{(R_a + R_L)^2 + (X_a \pm X_L)^2}} \cdot E_f \]  

(2-34)

Dividing both the numerator and denominator of (2-34) by \( X_L \), results in:

\[ V_a = \sqrt{\frac{\left( \frac{R_a}{X_L} \right)^2 + 1}{\left( \frac{R_a + R_L}{X_L} \right)^2 + \left( \frac{X_a}{X_L} \pm 1 \right)^2}} \cdot E_f \]  

(2-35)

With the armature resistance neglected for the purpose of this explanation, (2-35) can further be simplified as follows:

\[ V_a = \sqrt{\frac{\sigma^2 + 1}{\sigma^2 + (\xi \pm 1)^2}} \cdot E_f \]  

(2-36)

where \( \sigma = \frac{R_a}{X_L} \) and \( \xi = \frac{X_a}{X_L} \).

The ratio \( \sigma \), in (2-36) is related directly to the load power factor, since \( \sigma \) varies in the range \( 0 \leq \sigma \leq 1 \) as the load impedance varies from being purely resistive to purely reactive. Consequently, the ratio \( \sigma \) remains constant when the machine is operating at a constant load power factor and moreover, whilst the armature current is increased. An increase in armature current at constant load power factor can be considered as due to a reduction in the magnitude of the load impedance \( Z_L \). This corresponds to an increase in the ratio \( \xi \). The terminal characteristic of the generator under constant power factor operation is thus solely dependent on the term \( (\xi \pm 1)^2 \), in (2-36). The relative magnitudes of \( X_L \) and \( X_L' \) forming \( \xi \), can now be used to explain the terminal characteristic illustrated in Fig. 2.6. This is summarized in Table 2.2.

From Table 2.2, it can be seen that the terminal voltage always decreases with load when operating at constant unity or lagging load power factors. On the contrary, the
terminal voltage may initially increase with load, depending on the value of \( \xi \), after which it will decrease to zero with a further increase in load, for constant leading power factor operation. This agrees with the curves in Fig. 2.6.

The terminal characteristic of the generator at various operating speeds can also be determined using (2-32). The variation of \( V_a \) with \( i_a \), at speeds 60% and 140% of rated speed, are illustrated in Fig. 2.7 for the same machine as above.

As expected, the curves in Fig. 2.7 resemble those presented in Fig. 2.6 and can also be explained using Table 2.2.

**Table 2.2**

**Effect of Load Current and Power Factor on the Terminal Voltage of the Machine**

<table>
<thead>
<tr>
<th>( \omega \geq \sigma \geq 0 )</th>
<th>( X_1 \geq X_2 \geq 0 )</th>
<th>( X_1 &gt; X_2 \geq 0 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>( 0 \leq \xi \leq 1 )</td>
<td>( E_r = V_e = \sqrt{\frac{T^2 + 1}{\sigma^2 + 4}} \cdot E_f )</td>
<td>( E_r = V_e = \sqrt{\frac{T^2 + 1}{\sigma^2 + 4}} \cdot E_f \leq 0 )</td>
</tr>
<tr>
<td>( 1 \leq \xi + 1 \leq 2 )</td>
<td>( \rightarrow V_e ) decreases with load</td>
<td>( \rightarrow V_e ) decreases with load</td>
</tr>
<tr>
<td>( 1 \leq (\xi - 1)^2 \leq 4 )</td>
<td>( E_r = V_e = \sqrt{\frac{T^2 + 1}{\sigma^2 + 4}} \cdot E_f )</td>
<td>( E_r &lt; V_e \leq 0 )</td>
</tr>
<tr>
<td>( \omega &gt; \sigma \geq 0 )</td>
<td>( 1 \leq \xi \leq \omega )</td>
<td>( 0 &lt; \xi - 1 \leq \omega )</td>
</tr>
<tr>
<td>( -1 \leq \xi - 1 \leq 0 )</td>
<td>( 0 &lt; \xi - 1 \leq \omega )</td>
<td>( 0 &lt; (\xi - 1)^2 \leq \omega )</td>
</tr>
<tr>
<td>( 1 \geq (\xi - 1)^2 \geq 0 )</td>
<td>( E_r \leq V_e = \sqrt{\frac{T^2 + 1}{\sigma^2 + 4}} \cdot E_f )</td>
<td>( E_r &lt; V_e &lt; 0 )</td>
</tr>
<tr>
<td>( \rightarrow V_e ) increases with load</td>
<td>( \rightarrow V_e ) decreases with load</td>
<td></td>
</tr>
</tbody>
</table>
2.5.2. Operating Conditions

In this section, the terminal voltage characteristic and power capability of the generator are used to explore its steady-state operating conditions. Recall that an expression for the total real power delivered to a load on the generator was given by (2-21). This can be used to write an alternate expression for the terminal voltage of an isolated PM synchronous generator. Thus, by solving for $V_a$, (2-21) can be rewritten as a function of $I_a$ and $P_L$, as:

$$V_a = \frac{P_L}{3\cos\phi} \cdot \frac{1}{I_a} \quad (2-37)$$

Equation (2-37) can be used to plot the $V_a$ vs $I_a$ variation required in order for the generator to deliver $P_L$ (kW) to its load, at a specific power factor. The constant per-phase apparent power (VA) hyperbolas obtained in this manner are illustrated in Fig. 2.8, for the same machine as above. The curves in Fig. 2.8 represent rated real power supplied to the load, over the same range of power factors.

An apparent power hyperbola is illustrated in Fig. 2.9, together with the terminal voltage characteristic of the generator at various operating speeds. The terminal
characteristic relates to operation of the generator at a constant load power factor of 0.76 leading, over a range of speeds from 60% to 120% of its rated speed.

Fig. 2.8. Terminal voltage and current variation required to deliver rated real power to a load, at various power factors.

Fig. 2.9. Constant per-phase apparent power hyperbola with terminal voltage characteristic of generator at various speeds.
With reference to Fig. 2.9, point A defines the terminal voltage and current at which the machine will operate when required to deliver rated power at rated speed to an isolated load at a power factor of 0.76 leading. Moreover, the intersections between the terminal voltage characteristic curves and the constant per-phase apparent power hyperbola determines the operating points of the generator when required to deliver this power to a load at various speeds.

The operating points can be determined by equating the expressions for each of the curves. This results in a 4th order polynomial of the form:

\[ ax^4 + bx^2 + c = 0 \]  

(2-38)

where:

\[ a = 9(R_x^2 + X_i^2)\cos^2 \phi, \]

\[ b = 6E \cos \phi (R_x \cos \phi - X_i \sin \phi) - 9E^2 \cos^2 \phi, \]

\[ c = P_e^2. \]

The smallest positive real root of (2-38) defines the armature current that the generator will operate at when delivering the required real power to its load at the specified speeds and power factor. It should be noted that the terminal voltage characteristic curves do not intersect the constant per-phase apparent power curve for speeds lower than approximately 0.8pu. The roots obtained for (2-38) will thus be complex for these low speeds, thus indicating that the generator will be incapable of delivering the required real power to an isolated load at speeds lower than approximately 0.8pu.

The operating currents obtained in this manner can be used to evaluate the performance of the generator when delivering the required real power to its load at various speeds. Furthermore, it can be used to evaluate the performance of the generator when design changes are investigated in order to restore the output power capability of the generator at low operating speeds.
2.6. Conclusions

An analytical model of a radial-flux PM wind generator was formulated in this chapter. The model relates the mechanical design specifications of the machine to its electrical equivalent circuit parameters and performance. Experimental validation of the model was provided by considering a 3.5kW, 8 pole, three-phase PM synchronous machine. The model will be used in subsequent chapters to evaluate the performance of a small PM wind generator.
2.7. References


2.8. Appendix

The nominal design specifications of the machine under consideration are summarised in Table 2.3 below.

<table>
<thead>
<tr>
<th>Design specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P$ - Rated Power</td>
<td>3.5kW</td>
</tr>
<tr>
<td>$p$ - Number of pole pairs</td>
<td>4</td>
</tr>
<tr>
<td>$l$ - Effective length of stator core</td>
<td>0.0475$m$</td>
</tr>
<tr>
<td>$D$ - Stator inner diameter</td>
<td>0.2472$m$</td>
</tr>
<tr>
<td>$l_g$ - Mechanical airgap clearance</td>
<td>0.0008$m$</td>
</tr>
<tr>
<td>$N_{ph}$ - Number of turns per phase</td>
<td>528</td>
</tr>
<tr>
<td>$a$ - Number of parallel paths</td>
<td>1</td>
</tr>
<tr>
<td>$\sigma_{Cu}$ - Conductivity of Cu at $25^\circ$C</td>
<td>$57 \cdot 10^4 S/m$</td>
</tr>
<tr>
<td>$A_{cond}$ - Cross-sectional area of conductors</td>
<td>$\pi \cdot 10^{-4} m^2$</td>
</tr>
<tr>
<td>$S$ - Number of stator slots</td>
<td>36</td>
</tr>
<tr>
<td>$\tau_c$ - Stator coil pitch</td>
<td>180 $^\circ ed$</td>
</tr>
<tr>
<td>$\alpha$ - Pole-arc to pole-pitch ratio</td>
<td>0.696</td>
</tr>
<tr>
<td>$w_t$ - Width of a stator tooth</td>
<td>0.009$m$</td>
</tr>
<tr>
<td>$h_t$ - Height of stator tooth</td>
<td>0.0387$m$</td>
</tr>
<tr>
<td>$h_{11}$ - Height of conductor portion of slot</td>
<td>0.026$m$</td>
</tr>
<tr>
<td>$h_{12}$ - Height of slot above conductors</td>
<td>0$m$</td>
</tr>
<tr>
<td>$h_{13}$ - Height of tapered portion of slot</td>
<td>0.0026$m$</td>
</tr>
<tr>
<td>$h_{14}$ - Height of stator slot opening</td>
<td>0.0012$m$</td>
</tr>
<tr>
<td>$b_{11}$ - Width of oval portion of stator slot</td>
<td>0.0178$m$</td>
</tr>
<tr>
<td>$b_{12}$ - Width of slot above conductors</td>
<td>0.0132$m$</td>
</tr>
<tr>
<td>$b_{13}$ - Width of tapered portion of slot</td>
<td>0.0132$m$</td>
</tr>
</tbody>
</table>
### Chapter 2: Analytical model of a PM wind generator

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$b_{14}$</td>
<td>Width of stator slot opening</td>
<td>0.0036m</td>
</tr>
<tr>
<td>$h_s$</td>
<td>Height of stator slot</td>
<td>0.0387m</td>
</tr>
<tr>
<td>$h_y$</td>
<td>Height of stator yoke</td>
<td>0.0504m</td>
</tr>
<tr>
<td>$l_m$</td>
<td>Radial thickness of PM</td>
<td>0.004m</td>
</tr>
<tr>
<td>$B_r$</td>
<td>Remanent flux density of PM</td>
<td>1.06T</td>
</tr>
<tr>
<td>$\mu_r$</td>
<td>Relative permeability of PM</td>
<td>1.0042</td>
</tr>
<tr>
<td>$\mu_0$</td>
<td>Permeability of free air</td>
<td>$4\pi \cdot 10^{-7}$</td>
</tr>
<tr>
<td>$\rho_{pm}$</td>
<td>Mass density of PM material</td>
<td>7500kg/m$^3$</td>
</tr>
<tr>
<td>$\rho_{rc}$</td>
<td>Mass density of rotor core material</td>
<td>7860kg/m$^3$</td>
</tr>
</tbody>
</table>
Chapter 3

On adapting a small PM wind generator for a multi-blade, high solidity wind turbine

3.1. Overview

This chapter explores the design space that exists between multi-blade, high-solidity water-pumping turbines and modern high-speed 2 and 3-bladed Horizontal Axis Wind Turbines (HAWTs). In particular, it compares the features and performance of a small 12-bladed, high solidity HAWT to that of a modern 3-bladed HAWT. It also outlines a procedure for adapting a small PM wind generator, intended for high-speed operation with a 3-bladed HAWT, for low speed operation with a 12-bladed, high solidity HAWT. This is achieved through a detailed analysis of the effects of several
minor changes to the nominal design of the machine. The redesigned machine is shown to be capable of delivering rated power at the reduced speed required by the 12-bladed HAWT, whilst operating at a good efficiency. The overall system performance of the 12-bladed HAWT coupled to the redesigned wind generator is shown to be satisfactory. Experimental validation is provided.
3.2. Introduction

Small wind turbines have largely adopted the 3-bladed, low solidity design philosophy of large utility-scale wind turbines. Increasing the number of blades has been shown theoretically to increase the aerodynamic efficiency of a wind turbine. An increase in aerodynamic efficiency could have the potential to decrease the overall cost of energy from a small wind-energy conversion system (WECS). This chapter examines the design space that exists between multi-blade, high-solidity water-pumping turbines and modern high speed 2 and 3-bladed Horizontal Axis Wind Turbines (HAWTs). Furthermore, it compares the features and performance of a small 12-bladed, high solidity HAWT to that of a modern 3-bladed one. The 12-bladed turbine operates at a lower tip speed ratio, thereby providing quieter operation, fewer balancing and vibration issues, less blade erosion and easier starting characteristics.

This chapter also addresses the problem of adapting a PM wind generator, intended for high-speed operation with a small 3-bladed HAWT, for low speed operation with a small multi-blade, high solidity HAWT. The new wind generator is required to deliver the same rated power as the original machine, but at 45% of the original shaft speed, under rated wind speed conditions. This requirement translates to a 122% increase in the developed torque and hence a 122% increase in the rotor volume ($D^2l$) of the machine. A complete redesign of the machine would ordinarily be necessary to meet these stringent requirements. However, this chapter offers an alternative approach to a complete redesign of the machine. It addresses the problem of adapting an existing wind generator to meet the new requirements, through carefully analysing the effects of several minor changes to the machine design, before selecting a new design specification for the machine. In so doing, the new machine fully exploits existing WECS manufacturing infrastructure and components, in an effort to reduce the energy costs associated with the generators.

The performance and equivalent circuit parameters of the machine are assessed by means of the comprehensive analytical model of a radial-flux PM synchronous machine (PMSM) presented in Chapter 2. The accuracy of the model has been verified experimentally and through a finite element analysis (FEA). The analytical
model is used in conjunction with the terminal voltage characteristic and operating power capability of a PMSM, to evaluate the effects of the design changes on the machine's performance.
3.3. Multi-Blade, High Solidity Wind Turbines

The design space for wind turbine rotors, in particular Horizontal Axis Wind Turbines (HAWTs), is quite large. Solidity ($\sigma$ - the ratio of total rotor blade area to swept area), blade number ($B$), airfoil characteristics, and tip speed ratio ($\lambda$) are all factors that determine the aerodynamic performance of the rotor. Reference [3.1] presented a comparison of approximate turbine Power Coefficient ($C_P$) vs tip speed ratio for a wide range of machines, including HAWTs and Vertical Axis Wind Turbines (VAWTs) and is shown in Fig. 3.1. It can be seen that the highest overall $C_P$ curve is for a 2-bladed high speed rotor with tip speed ratios in the range of $\lambda=4-7$. Most modern 3-bladed HAWTs have similar $C_P$ characteristics, and are designed with low solidities, typically of 7% or less. The American multi-blade turbine illustrated in Fig. 3.1, exhibits generally poor $C_P$ performance at a lower tip-speed ratio range than the high speed 2 and 3-bladed HAWTs. Multi-blade turbines typically have high solidities and use aerodynamic drag to develop high torques for mechanical water-pumping applications [3.2]. The four-bladed Dutch turbine occupies a low-performance space between the HAWT types, likely because they did not employ the airfoil or rotor design methodologies of modern HAWTs.

![Diagram](image_url)

**Fig. 3.1.** $C_P$ vs $\lambda$ for various types of wind turbines [3.1]
A large gap can be seen to exist in the performance and operating range between the multi-blade and low blade number turbines. Moreover, the ideal efficiency curve presented in Fig. 3.1, which is based on general momentum theory, indicates that it is theoretically possible for a properly designed rotor to exhibit as high or higher \( C_P \) characteristics than the current high-speed turbines, at approximately half of the tip speed ratios. A study was therefore conducted to explore the design space that exists between high-solidity water-pumping turbines and the high-speed 2 and 3-bladed turbines [3.3], [3.4]. In particular, the study focussed on using the same design principles used in the high-speed turbine designs, to investigate the impact of solidity and turbine blade number on the performance of a small HAWT.

### 3.3.1. Effect of Blade Number & Solidity on HAWT Performance

A Blade Element Momentum (BEM) based optimum design routine from [3.5] was first used to investigate the impact of solidity and turbine blade number on the performance of a small HAWT with optimally designed blades. Fig. 3.2 shows maximum power coefficient \( (C_{P_{\text{max}}} = C_P(\lambda_{\text{opt}})) \), vs tip speed ratio at \( C_{P_{\text{max}}} (\lambda_{\text{opt}}) \), for each of the optimum designs. The analysis verified that a 3-bladed turbine, designed for a high tip speed ratio of \( \lambda=5–7 \), should have a solidity of 5% to 7%. Moreover, high tip speed ratios were shown to be more optimal for a 3-bladed rotor. As shown for the 6 and 12-bladed cases in Fig. 3.2, increasing the blade number at the high design point \( \lambda \) would increase \( C_{P_{\text{max}}} \), with a diminishing return. Moreover, decreasing the design point \( \lambda \) would further increase \( C_{P_{\text{max}}} \). The highest \( C_{P_{\text{max}}} \) for the cases studied, was found to be 0.523 at \( \lambda_{\text{opt}}=3.75 \), with \( \sigma=14\% \) and \( B=12 \). It should be noted that these results are based on an idealised blade element technique [3.3].

Optimum designs use complicated non-linear twist and chord distributions, but small HAWTs are often constructed with non-twisted, constant chord blades for ease of manufacture. Blades are then pitched at an angle (\( \beta \)) that gives best overall performance. Fig. 3.3 shows a comparison of maximum \( C_P \) versus solidity for a series of rotors with non-twisted, constant chord blades. The curves in Fig. 3.3 were
Chapter 3: On adapting a small PM wind generator for a multi-blade, high solidity wind turbine

obtained from a BEM analysis technique described in [3.3], and are shown for comparison. Unlike the optimum designs in Fig. 3.2, constant-chord, untwisted blades were observed to produce highest points of maximum $C_p$ at a higher solidity than the optimum design solution in both the 3 and 12-bladed cases. For $B=12$, the highest $C_{p\text{max}}$ was 0.44 at a solidity of $\sigma=25\%$ and a pitch angle of $\beta=12^\circ$. Although the difference in maximum $C_p$ is small between solidities, Fig. 3.4 shows that the tip speed ratio decreases significantly with increasing solidity, almost independently of blade number.

Wind tunnel experiments were conducted on a scale model HAWT with flat plate, constant-chord, non-twisted blades, to experimentally verify the trends in $C_p$ vs solidity and blade number [3.3], [3.4]. The experimental data confirmed that increasing solidity increased $C_p$, whilst decreasing $\lambda$ at the point of maximum $C_p$. The experiments also indicated that changes in blade number had little effect on the $C_p$ characteristics at a given solidity. This was however mainly attributed to the low Reynolds number range of operation and the thickness-to-chord ratio of the flat plate turbine blades used in the experiments [3.3].

Fig. 3.2. Effect of turbine blade number ($B$) and solidity ($\sigma$) on $C_{p\text{max}}$ and $\lambda_{opt}$, for a small HAWT with optimally designed rotor blades [3.3], [3.4]

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University of Cape Town
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Fig. 3.3. Effect of turbine blade number (B), blade pitch angle (β) and solidity (σ) on $C_{P,max}$ of a small HAWT [3.3], [3.4]

Fig. 3.4. Effect of turbine blade number (B) and solidity (σ) on tip speed ratio at $C_{P,max}$ ($\lambda_{opt}$) for of a small HAWT [3.3], [3.4]
3.3.2. Operation of Small Multi-Blade, High Solidity HAWTs

With reference to Fig. 3.3 and Fig. 3.4, the highest $C_{p_{\text{max}}}$ for a small multi-blade, high solidity HAWT was 0.44. This was achieved by a 12-bladed HAWT, with a solidity of $\sigma=25\%$, at a tip speed ratio of $\lambda_{\text{opt}}=2.7$. In contrast with the 12-bladed turbine, a 3-bladed turbine with low solidity of $\sigma=5\%$, which is typical for these HAWTs, achieves a $C_{p_{\text{max}}}=0.39$, at $\lambda_{\text{opt}}=6$. Thus, for the same turbine blade diameter and wind speed conditions, the 12-bladed turbine will capture approximately 13\% more energy than the 3-bladed turbine when each are operated at the respective tip speed ratios which ensures maximum energy capture. The increased energy capture of the 12-bladed turbine is however achieved at 45\% of the operating speed range of the 3-bladed turbine.

For the same output power rating, a generator coupled to the 12-bladed turbine will therefore be required to develop 122\% more torque, compared to when coupled to the 3-bladed turbine. This requirement can be met by a proportional increase in rotor volume ($D^2l$), which would ordinarily require a complete redesign of the machine. However, the alternative approach considered in this chapter relates to adapting the nominal design of the wind generator to meet the new requirement. This is achieved through carefully analysing the effects of several minor changes to the nominal design, before selecting a new design specification for the machine.

The generator is a 3.5kW, 8 pole, 750rpm, three-phase PMSM. The output power rating of the machine is derated for operation as a wind generator coupled directly to the afore-mentioned 3-bladed HAWT. It is assumed that the 3-bladed turbine has a 3.29m blade diameter and achieves its maximum $C_p$ of 0.39 at a tip speed ratio of 6. Moreover, the rated wind speed is 10m/s. Using the definition for the tip speed ratio [3.6], the reduced operating speed of the PM machine as a wind generator is calculated as 348rpm. The output power rating of the machine at this speed is then calculated as 1.625kW. In coupling this generator to the 12-bladed HAWT, it is still required to deliver 1.625kW, but at a reduced speed of 157rpm at rated wind speed.
3.4. Effects of Design Changes on the Performance of the PM Wind Generator

The 3.5kW, 750rpm PMSM under investigation is intended for operation as a wind generator, directly coupled to the small 3-bladed HAWT. The machine is consequently derated to 1.625kW, at 348rpm. In coupling this machine to a multi-blade, high solidity HAWT, it is required to deliver 1.625kW to its load, but at 157rpm under rated wind speed conditions.

The output power capability of the PM wind generator at this low speed can be ensured through several design changes. In particular, the effects of changes to $N_{ph}$, $I$, $a$, $B_r$ and $A_{cond}$ are investigated. Changes to these design parameters are considered minor, since they avoid the redesign and remanufacture of stator lamination stampings, as would be required by a change in $D$. The design changes are evaluated on the basis of the overall efficiency of the wind generator, its equivalent circuit parameters, the mass of active materials used and the flux density levels in the main parts of the machine. The effect of each design change is analysed independently, with all other parameters at nominal values.

3.4.1. Change in the Number of Turns Per Phase ($N_{ph}$)

The effect of this design change on the efficiency of the generator is illustrated in Fig. 3.5. The figure shows the variation of generator efficiency with speed, as the number of turns per phase is increased from 60% to 180% of its nominal value, in 20% intervals. The dotted line represents the variation in generator efficiency, when all its design specifications are at nominal values.

It can be seen that the wind generator is capable of delivering rated power (1.625kW) to its load at lower speeds as the number of turns per phase is increased. Moreover, the efficiency of the wind generator improves. The effect of this design change is summarised in Table 3.1. The specific electric loading ($SEL$) is the rms value of the stator peripheral current density and $J$ is the stator conductor current density [3.8].
Fig. 3.5. Effect of a change in $N_{ph}$ on the wind generator efficiency

**TABLE 3.1**

**COMPARISON OF WIND GENERATOR PERFORMANCE AT FULL-LOAD, FOR CHANGE IN $N_{ph}$, AT RATED AND REDUCED SPEED OPERATION**

<table>
<thead>
<tr>
<th>Speed</th>
<th>Performance</th>
<th>Change in $N_{ph}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>348 rpm</td>
<td>$\eta [%]$</td>
<td>1x</td>
</tr>
<tr>
<td></td>
<td>91.96</td>
<td>93.69</td>
</tr>
<tr>
<td></td>
<td>$SEL \ [A-Cond./m]$</td>
<td>19,478</td>
</tr>
<tr>
<td></td>
<td>$J \ [A/mm^2]$</td>
<td>1.52</td>
</tr>
<tr>
<td>157 rpm</td>
<td>$\eta [%]$</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>$SEL \ [A-Cond./m]$</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>$J \ [A/mm^2]$</td>
<td>-</td>
</tr>
</tbody>
</table>

The relatively high efficiency at rated speed (348rpm) in Table 3.1 is a direct result of the low operating current density due to oversized conductors, and the low armature current required due to the large excitation voltage induced in the stator winding by its many turns per phase. Increasing $N_{ph}$ makes operation at 157rpm possible, whilst maintaining the relatively high efficiency and low current density. The $SEL$ does however increase at this low operating speed.
The effect of a change in $N_{ph}$ on the equivalent circuit parameters of the generator are illustrated in Fig. 3.6. It can be seen that the magnetising and leakage reactances increase significantly, as these are proportional to the square of $N_{ph}$. A 40% increase in $N_{ph}$ results in a 96% increase in the reactances. Furthermore, the armature resistance increases linearly as the total length of copper increases with $N_{ph}$. The slot-fill factor increases proportionately with this design change. In assessing the machine under consideration, it was observed that the stator slots would only be able to accommodate a 10% increase in the slot-fill factor.

The mass of the copper used in the generator windings increases proportionately with $N_{ph}$. The copper mass increases by 60%, from 23.63kg to 37.81kg, as $N_{ph}$ is increased from its nominal value (528 turns) by 60%. The mass of the stator core, rotor core and PM material used in the machine remains constant as the number of turns is increased. The power to weight ratio of the generator is therefore not decreased significantly as the number of turns per phase is increased.

The flux density levels in the main parts of the machine remain within acceptable limits as $N_{ph}$ is increased. The peak value in the stator teeth and yoke remain constant at 1.80T and 0.504T, whilst the plateau and peak value of the fundamental airgap flux density are 0.756T and 0.851T.

![Fig. 3.6. Effect of a change in $N_{ph}$ on equivalent circuit parameters at 348rpm](image-url)
3.4.2. Change in the Axial Length \((l)\)

The effect of this design change on the efficiency of the wind generator is illustrated in Fig. 3.7. The curves show the effect on generator efficiency as the axial length is increased from 60% to 180% of the nominal value, in 20% intervals.

By comparison with the change in \(N_{ph}\), it can be seen from Table 3.2 that the change in \(l\) causes the machine to operate at higher efficiency and lower, more acceptable \(SEL\). The magnetising reactance of the generator increases proportionately with the change in \(l\), as expected from its expression in Chapter 2. The armature resistance also increases as the total length of armature winding increases with axial length. The effect of this design change on the mass of active materials used in the wind generator is illustrated in Fig. 3.8.

It can be seen from the figure that the total mass of the machine increases significantly by 56.83%, from 67.07kg to 105.18kg, as \(l\) is increased from its nominal value by 80%. Consequently, the power-to-weight ratio of the wind generator drops by 36.24%, from 24.23W/kg to 15.45W/kg. The flux density levels in the main parts of the machine remain similar to those quoted in the previous section.

Fig. 3.7. Effect of a change in \(l\) on the wind generator efficiency
Table 3.2
Comparison of Wind Generator Performance at Full-Load (1.625 kW), for Several Design Changes, at Reduced Speed Operation (157 rpm)

<table>
<thead>
<tr>
<th>Design parameter</th>
<th>Design change</th>
<th>Full-load performance at 157 rpm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\eta$ [%]</td>
<td>$SEL$ [A-Cond./m]</td>
</tr>
<tr>
<td>$N_{ph}$</td>
<td>1.4x</td>
<td>77.76</td>
</tr>
<tr>
<td></td>
<td>1.6x</td>
<td>81.75</td>
</tr>
<tr>
<td>$l$</td>
<td>1.2x</td>
<td>78.41</td>
</tr>
<tr>
<td></td>
<td>1.4x</td>
<td>83.74</td>
</tr>
<tr>
<td></td>
<td>1.6x</td>
<td>86.69</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>1.2x</td>
<td>72.49</td>
</tr>
<tr>
<td></td>
<td>1.3x</td>
<td>74.82</td>
</tr>
<tr>
<td>$B_r$</td>
<td>1.1x</td>
<td>73.36</td>
</tr>
<tr>
<td></td>
<td>1.2x</td>
<td>78.76</td>
</tr>
<tr>
<td></td>
<td>1.4x</td>
<td>83.74</td>
</tr>
<tr>
<td>$A_{cond}$</td>
<td>1.4</td>
<td>77.76</td>
</tr>
<tr>
<td></td>
<td>1.6</td>
<td>81.75</td>
</tr>
</tbody>
</table>

Fig. 3.8. Effect of a change in $l$ on the mass of active materials
3.4.3. Change in the Pole-arc to Pole-pitch Ratio ($\alpha$)

The effect of this design change on the full-load performance of the generator at 157rpm is summarised in Table 3.2. The equivalent circuit parameters of the generator are unaffected by this design change. The mass of the stator core, rotor core and copper used in the machine is also not affected. However, the mass of the PM material increases proportionately with $\alpha$, from 0.753kg to 0.979kg.

The effect of a change in $\alpha$ on the flux density levels in the main parts of the machine is illustrated in Fig. 3.9. It can be seen that $B_{y_{\text{max}}}$ increases from 0.50T to 0.66T and $B_{1_{\text{max}}}$ increases from 0.85T to 0.95T, as $\alpha$ is increased by 30%. The peak flux density in the stator teeth is also quite high, by design. These flux density levels are however within the allowable range for small PM machines [3.9], [3.10].

![Diagram](image_url)

Fig. 3.9. Effect of a change in $\alpha$ on the flux density levels in the generator
3.4.4. Change in the Remanent Flux Density of the PMs ($B_r$)

The effect of this design change on the performance of the generator is summarised in Table 3.2. The equivalent circuit parameters and mass of the generator are unaffected by this design change. The effect of this design change on flux density levels is shown in Fig. 3.10. It can be seen that the peak flux density in the stator teeth increases from 1.80T to 2.53T as the remanent flux density of the PMs is increased by 40%. This peak value of the tooth flux density is well above the normal operating range and will cause saturation of the stator teeth.

![Graph showing change in flux density](image)

Fig. 3.10. Effect of a change in $B_r$ on the flux density levels in the machine

3.4.5. Change in Cross-section of Stator Conductors ($A_{cond}$)

The effect of this design change on the performance of the wind generator at 157rpm is summarised in Table 3.2. The reactances of the generator are unaffected by this design change. The stator winding resistance decreases, with an increase in the cross-sectional area of the conductors. The mass of copper increases proportionately by 60% from 23.63kg to 37.81kg, as $A_{cond}$ increases by 60% from its nominal value. The slot-fill factor also increases proportionately with $A_{cond}$. 
3.5. Redesign for Multi-Blade, High Solidity HAWT

New specifications for several design parameters of the PM wind generator are discussed here. This is achieved by combining the previous changes to produce a new machine design. The new PM wind generator design obtained in this manner would therefore be capable of delivering rated power (1.625kW) at the reduced speed (157rpm) required by the multi-blade, high solidity HAWT.

3.5.1. Machine Redesign Procedure

An increase in $N_{ph}$ and $A_{cond}$ results in satisfactory full-load performance at low speeds. However, the copper mass and the slot-fill factor increases proportionately with each of these design changes. Moreover, the actual stator slots can only accommodate a 10% increase in the slot-fill factor. The reactances of the machine also increase significantly, with the square of the number of turns.

The constraint on increasing the axial length of the machine is the reduction in the power to weight ratio of the machine. Similarly, the constraint on increasing $\alpha$ and $B_r$ is the increase in flux density levels in the main parts of the machine. In particular, $B_{\text{max}}$ increases significantly beyond its operating range.

In redesigning the machine, a 40% increase in $N_{ph}$ and a 30% reduction in $A_{cond}$ results in a 10% net increase in the slot-fill factor and copper mass. The reduction in $A_{cond}$ can be tolerated due to the relatively low operating current density of the machine. However, in order to limit the reactances of the machine, $N_{ph}$ was reduced by 30% and $A_{cond}$ increased by 40% instead. A 10% net increase in the slot-fill factor and the copper mass was therefore still maintained. Furthermore, $B_r$ was only increased by 10% in order to limit $B_{\text{max}}$ to values within the operating range for small PM machines. The pole-arc to pole-pitch ratio ($\alpha$) was increased by 30%, to a value of 0.905. This increase in $\alpha$ results in a PM pole-arc of 164°, which borders on the limit of the range for small PM synchronous machines [3.10].
Since the redesigned wind generator is required to deliver rated power at 45% of its rated speed when coupled to the multi-bladed, high solidity HAWT, its developed torque would increase by 122%. This would ordinarily necessitate a proportional increase in the rotor volume ($D^2l$) of the machine [3.8]. However, an increase in rotor volume can only be achieved through an increase in $l$, since changing $D$ would require the remanufacture of stator lamination stampings. The axial length was increased by 40%, in order to increase the current loadings ($SEL$ and $J$) of the machine, which were low in the nominal design.

3.5.2. Performance of New PM Wind Generator

The nominal and new design specifications of the wind generator are compared in Table 3.3. It can be seen that the flux density levels in the main parts of the redesigned machine are within acceptable limits. The $SEL$ of the new wind generator is acceptable, but $J$ is still quite low. This is primarily due to the large cross-sectional area of the stator conductors used in the new design. The total mass of the new machine has however increased by 28%, thereby reducing its power-to-weight ratio by 22%.

The efficiency of the PM wind generator with nominal and new design specifications is compared in Fig. 3.11. It can be seen that the redesigned wind generator is capable of delivering rated power (1.625kW) to its load at the low speed (157rpm) required by the multi-blade, high solidity HAWT. Moreover, this is achieved at an efficiency of 87.84%, whilst the machine with nominal design specifications is incapable of delivering rated power at this speed.

The overall system performance of the 3 and 12-bladed HAWTs coupled to wind generators with nominal and new design specifications are compared in Table 3.4. It can be seen that although the 12-bladed turbine captures 12.8% more energy at rated wind speed, the overall system efficiency ($C_{P_{max}} \times \eta$) only increases by 7.5%. This is primarily due to the drop in efficiency of the new generator design, which was constrained by practical issues relating to the nominal machine design.
### Table 3.3

**Comparison of Nominal and New Design Specs of Wind Generator**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Nominal Specification</th>
<th>New Specification</th>
</tr>
</thead>
<tbody>
<tr>
<td>$N_{ph}$ [turns]</td>
<td>528</td>
<td>370</td>
</tr>
<tr>
<td>$l$ [m]</td>
<td>0.0475</td>
<td>0.0665</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>0.696</td>
<td>0.905</td>
</tr>
<tr>
<td>$B_r$ [T]</td>
<td>1.06</td>
<td>1.17</td>
</tr>
<tr>
<td>$A_{cond}$ [mm$^2$]</td>
<td>3.142</td>
<td>4.398</td>
</tr>
<tr>
<td>$R_a$</td>
<td>1.57</td>
<td>0.84</td>
</tr>
<tr>
<td>$L_1$ [mH]</td>
<td>21.4</td>
<td>12.17</td>
</tr>
<tr>
<td>$L_m$ [mH]</td>
<td>44.6</td>
<td>30.57</td>
</tr>
<tr>
<td>$L_2$ [mH]</td>
<td>66.0</td>
<td>42.74</td>
</tr>
<tr>
<td>$B_g$ [T]</td>
<td>0.75</td>
<td>0.84</td>
</tr>
<tr>
<td>$B_{1 \text{ max}}$ [T]</td>
<td>0.85</td>
<td>1.05</td>
</tr>
<tr>
<td>$B_{t \text{ max}}$ [T]</td>
<td>1.80</td>
<td>2.01</td>
</tr>
<tr>
<td>$B_{y \text{ max}}$ [T]</td>
<td>0.50</td>
<td>0.73</td>
</tr>
<tr>
<td>$SEL$ [A-Cond./m]</td>
<td>19,478</td>
<td>25,578</td>
</tr>
<tr>
<td>$J$ [A/mm$^2$]</td>
<td>1.52</td>
<td>2.037</td>
</tr>
<tr>
<td><strong>Total mass</strong> [kg]</td>
<td>67.07</td>
<td>85.93</td>
</tr>
<tr>
<td><strong>Power/weight</strong> [W/kg]</td>
<td>24.23</td>
<td>18.91</td>
</tr>
</tbody>
</table>
Chapter 3: On adapting a small PM wind generator for a multi-blade, high solidity wind turbine

Fig. 3.11. Comparison of efficiency of the PM wind generator with nominal and new design specifications, at full-load (1.625kW)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>3-bladed HAWT</th>
<th>12-bladed HAWT</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma$ [%]</td>
<td>5</td>
<td>25</td>
</tr>
<tr>
<td>$\lambda_{opt}$</td>
<td>6</td>
<td>2.7</td>
</tr>
<tr>
<td>$C_{P_{max}}$</td>
<td>0.39</td>
<td>0.44</td>
</tr>
<tr>
<td>Blade diameter [m]</td>
<td>3.29</td>
<td>3.29</td>
</tr>
<tr>
<td>Rated generator power [kW]</td>
<td>1.625</td>
<td>1.625</td>
</tr>
<tr>
<td>Rated speed [rpm]</td>
<td>348</td>
<td>157</td>
</tr>
<tr>
<td>Generator efficiency ($\eta$) [%]</td>
<td>91.96</td>
<td>87.84</td>
</tr>
<tr>
<td>$C_{P_{max}} \times \eta$</td>
<td>0.359</td>
<td>0.386</td>
</tr>
</tbody>
</table>

TABLE 3.4
SYSTEM PERFORMANCE OF 3 AND 12-BLADED HAWTS COUPLED TO WIND GENERATOR WITH NOMINAL AND NEW DESIGN SPECIFICATIONS
3.6. Conclusions

This chapter explored the design space that exists between multi-blade, high-solidity turbines and modern high speed 2 and 3-bladed HAWTs. Furthermore, it compared the performance of a small 12-bladed, high solidity HAWT to that of a modern 3-bladed design. It also outlined a procedure for adapting a small PM wind generator, intended for high speed operation with a 3-bladed HAWT, for low speed operation with a 12-bladed, high solidity HAWT. The redesigned machine was shown to be capable of delivering rated power at the reduced speed required by the multi-blade, high solidity HAWT, whilst operating at a good efficiency. The overall system performance of the 12-bladed, high solidity HAWT coupled to the redesigned wind generator was satisfactory.
3.7. References


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Chapter 4

Comparison of maximum power & torque operation of a PM wind generator

4.1. Overview

In general, three operating regions are defined in relation to the power coefficient curve of a variable speed wind turbine. These include variable speed operation at a tip speed ratio which ensures maximum power capture, constant speed operation at the turbine speed limit, and variable speed operation at the power limit of the power electronic converter. This chapter considers an alternative to the operation at maximum power capture. It considers variable speed operation at a tip speed ratio, which ensures that maximum shaft torque is captured. Moreover, it compares the performance of a small PM wind generator operated at maximum power capture to that of maximum torque capture.
4.2. Introduction

Direct-drive wind energy conversion systems (WECS) are receiving increasing attention due to their inherent efficiency. By eliminating the need for a gearbox between the wind turbine and generator, these systems are less expensive and also require less maintenance. The efficiency of these systems is further enhanced with the use of custom-built Permanent Magnet (PM) generators. These generators are however inherently characterised by the presence of parasitic torque components. These include cogging torques due to the PM excitation and harmonic torques due to a non-sinusoidal airgap magnetic field distribution [4.1].

The presence of these parasitic torques results in a reduction of the overall response time of the WECS, as the turbine shaft torque is required to overcome the parasitic torques in addition to the electromagnetic torque of the generator. This effect is exacerbated at low speed operation, which is typical for these direct-coupled WECS, where the shaft torque reduces to levels comparable with the parasitic torques.

This chapter considers a method of increasing the shaft torque of the turbine, in an effort to improve the response time of the WECS. It considers an alternative to the operation of the turbine at maximum power capture. It considers variable speed operation at a tip speed ratio, which ensures that maximum shaft torque is captured. Moreover, it compares the performance of a small PM wind generator operated at maximum power capture to that of maximum torque capture. The losses, efficiency and annual energy output of the generator is determined and compared for the different operating regions.
4.3. Analytical Model of the Wind Turbine

The wind turbine under investigation is a commercial 3-bladed horizontal-axis turbine, with a blade diameter of 3.6m. The turbine is intended for direct-coupling to a 1kW PM wind generator. The power captured by a wind turbine from the incident wind can be expressed as [4.2]:

\[ P_{\text{shaft}} = \frac{1}{2} \rho_{\text{air}} \pi R^2 U^3 C_P \quad (4-1) \]

where \( \rho_{\text{air}} \) is the density of air at the height of the turbine hub and \( C_P \) is the dimensionless power coefficient of the turbine.

The power coefficient of the turbine is a function of the tip speed ratio (\( \lambda \)) at which the turbine is operating at. This ratio is defined as the ratio of blade tip speed to wind speed, and can be expressed as:

\[ \lambda = \frac{\omega R}{U} \quad (4-2) \]

Equations (4-1) and (4-2) can be used to determine an expression for the torque applied to the turbine shaft, which can be expressed as [4.3]:

\[ T_{\text{shaft}} = \frac{P_{\text{shaft}}}{\omega} = \frac{1}{2} \rho_{\text{air}} \pi R^2 U^3 \frac{C_T}{\lambda} \quad (4-3) \]

where \( C_T = \frac{C_P}{\lambda} \) is the torque coefficient of the turbine.

The power coefficient of the turbine under investigation in this chapter was approximated by a 5th order polynomial of the form [4.3]:

\[ C_P(\lambda) = a_0 + a_1 \lambda + a_2 \lambda^2 + a_3 \lambda^3 + a_4 \lambda^4 + a_5 \lambda^5 \quad (4-4) \]

where \( a_0, a_2, a_4 = 0, a_1 = 0.00333, a_3 = 0.00433, \) and \( a_5 = -7.26e-5 \).
Chapter 4: Comparison of maximum power and torque operation of a PM wind generator

The power and torque coefficients for the turbine under investigation are shown in Fig. 4.1.

The turbine power and shaft torque can be written in alternate forms as functions of its shaft speed and tip speed ratio. Thus, using (4-1)-(4-3), these can be expressed as:

\[ P_{\text{shaft}} = \frac{1}{2} \rho \alpha \pi R^3 \omega^3 \frac{C_p}{\lambda^3} \]  \hspace{1cm} (4-5)

\[ T_{\text{shaft}} = \frac{1}{2} \rho \alpha \pi R^3 \omega^3 \frac{C_T}{\lambda^3} \]  \hspace{1cm} (4-6)

Moreover, (4-5) and (4-6) can be used to plot the turbine power and shaft torque as a function of shaft speed, at various wind speeds. This is illustrated in Fig. 4.2.
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Fig. 4.1. Power and torque coefficients for the turbine under investigation

Fig. 4.2. Turbine power and shaft torque as a function of shaft speed, at various wind speeds
4.4. Wind Turbine Operating Regime

With reference to Fig. 4.1 and (4-1)-(4-2), it can be seen that the aerodynamic efficiency of a wind turbine is determined by its operating tip speed ratio ($\lambda$). In general, three regions of operating tip speed ratio are defined for the operation of variable speed wind turbines. These regions are defined in relation to the power coefficient curve and include [4.4],[4.5],[4.6]:

- Region I, where $\lambda = \lambda_{rpm}, C_T(\lambda_{rpm}) = C_{T_{max}}$
- Region II, where $\lambda < \lambda_{rpm}, C_T(\lambda) < C_{T_{max}}$
- Region III, where $\lambda > \lambda_{rpm}, C_T(\lambda) < C_{T_{max}}$

Operation in region I is achieved at low wind speeds by allowing the turbine shaft speed to vary with the wind speed, thus maintaining a constant tip speed ratio. Maximum aerodynamic efficiency is maintained throughout this region, since the turbine always operates at a tip speed ratio (\lambda_{rpm}) that ensures maximum power capture $C_T(\lambda_{rpm}) = C_{T_{max}}$.

Region II operation is entered under moderate wind conditions, when the turbine speed limit is reached. The shaft speed is maintained constant throughout this region by reducing the tip speed ratio and hence the aerodynamic efficiency, with further increases in wind speed.

Region III is entered under high wind conditions, when the electrical power limit of the generator or power electronic converter is reached. The electrical output power of the generator is maintained constant throughout this region by further reducing the tip speed ratio and hence the power captured by the turbine, as the wind speed increases. The aerodynamic efficiency is deliberately exacerbated in regions II and III in order to limit the power captured by the turbine in moderate and high wind conditions.

A new operating region can be defined as an obvious alternative to the region I operating mode. This region is defined in relation to the torque coefficient curve in
Fig. 4.1, as:

- Region I', where $\lambda = \lambda_{\text{max}} \cdot c_r(\lambda_{\text{ref}}) - c_{\text{ref}}$.

Operation in region I', ensures that maximum shaft torque is captured at all wind speeds until the turbine speed limit is reached. Operation in this region is particularly advantageous when the turbine is coupled to a PM generator, where parasitic cogging and harmonic torques are present. The increased shaft torque captured by the turbine provides additional driving torque required to overcome the parasitic torques present in a direct-coupled PM generator, thereby improving the overall response time of the WECS.

The variation in the power captured by the turbine and shaft torque is illustrated in Fig. 4.3 and Fig. 4.4 respectively, for the four regions of operation. The power and torque are plotted as a function of turbine shaft speed at various wind speeds.

With reference to Fig. 4.3, it can be seen that the power captured by the turbine when operating in region I, always corresponds to the peak power available at all wind speeds. Operation in region I', however results in the turbine capturing less power than the maximum available at all wind speeds. Furthermore, the turbine reaches its speed limit and hence enters region II, at a higher wind speed ($U_{a'}$) than when operating in region I ($U_a$). In region II, the turbine power increases with wind speed, whilst operating at its speed limit. Region III is entered at $U_b$, and the turbine power continues to increase in order to compensate for a drop in the generator efficiency, as the electrical output power of the generator is maintained constant.

With reference to Fig. 4.4, it can be seen that the shaft torque captured by the turbine when operating in region I', always corresponds to the peak torque available at all wind speeds. Operation in region I, however results in the turbine capturing less torque than the maximum available at all wind speeds.
The difference in power and shaft torque captured in regions I and I' is clearly illustrated in Fig. 4.5 and Fig. 4.6 below, which shows their variation with wind speed. It is evident that the power captured in region I always exceeds that of region I' for all wind speeds. Similarly, the shaft torque captured in region I' always exceeds that of region I for all wind speeds.
Fig. 4.5. Turbine power captured vs wind speed for all operating regions

Fig. 4.6. Turbine shaft torque vs wind speed for all operating regions
Chapter 4: Comparison of maximum power and torque operation of a PM wind generator

A relationship between the turbine shaft speed and wind speed can be obtained from the definition of the tip speed ratio (4-2). This can be expressed as:

$$\omega_r = \frac{\omega U}{R}$$

(4-7)

Thus, by substituting the wind speed and tip speed ratio range for each region of turbine operation, the relation between turbine speed and wind can be plotted for each of the four regions of operation. This is shown in Fig. 4.7. The turbine speed limit is clearly illustrated, together with the wind speeds $U_e$ and $U_e'$ at which it is reached in regions I and I'.

![Fig. 4.7. Turbine shaft speed vs wind speed for all operating regions](image-url)
4.5. PM Generator Control and Performance

Operation of a wind turbine through each of the four operating regions can be achieved by controlling the generator to which it is coupled. The wind turbine under investigation is coupled to a PM synchronous generator. Aspects relating to the control of the generator are presented in this section. Moreover, its performance is simulated and compared for the various regions of turbine operation.

The machine considered in this investigation is a commercial 3-phase, 1kW, 30 pole PM synchronous wind generator with an outer rotor construction. The machine has radially magnetised ferrite PMs mounted on the inner surface of a mild steel rotor core. The per-phase armature resistance \( R_a \) is 0.39 \( \Omega \) and the flux linkage of the PMs \( \lambda_{pm} \) is 0.1122 Wb-turns.

4.5.1. Generator Control

It is assumed that the PM generator is connected to a 3-phase voltage source converter operating in rectifier mode. The machine is controlled by a Field Oriented Control (FOC) loop, implemented in the rotor reference frame. The speed reference for the FOC loop is generated as a function of wind speed from \( (4-7) \) and Fig. 4.7. The \( q \)-axis current reference is determined from the electromagnetic and applied shaft torques, in order to assess the machine losses in the subsequent section.

The steady-state electromagnetic torque developed by a PM synchronous machine operating as a generator can be expressed in terms of rotor reference frame currents \( i_d \) and \( i_q \), as \( (4.7) \):

\[
T_e = \frac{3}{2} p \left( \lambda_d i_q - \lambda_q i_d \right)
\]  

(4-8)

where \( \lambda_d = \lambda_{d,cm} - L_d i_d \) and \( \lambda_q = -L_q i_q \) are the total flux linkages of the \( d \) and \( q \)-axis stator coils, in the rotor reference frame model of the machine. Moreover \( I_d \) and \( I_q \) are the \( d \) and \( q \)-axis synchronous inductances of the machine.
The effective airgap in a PM machine with magnets mounted on the rotor surface can be considered constant and relatively large. This is due to the relative permeability of the PM material being close to unity. The \( d \) and \( q \)-axis synchronous inductances are consequently identical and relatively small in magnitude. Saliency and armature reaction is therefore negligible in this machine configuration.

With negligible saliency present in a surface-mounted PM rotor construction (\( L_d = L_q \)), maximum torque can be developed per ampere of stator current if \( i_q \) is maintained to be zero [4.8]. The expression for the torque developed by the PM generator under investigation thus reduces to:

\[
\tau_e = \frac{3}{2} p L_{pm} i_q
\]  

The \( q \)-axis current reference for the FOC loop can be determined by equating the expressions for the generator shaft torque (4-3), (4-6) and the electromagnetic torque (4-9). The \( q \)-axis current can therefore be expressed as a function of wind speed (4-10) or turbine shaft speed (4-11), as:

\[
i_q = \frac{1}{3} \frac{\rho_{\omega d} \pi R^4}{p \lambda_{pm}} C_r U^2
\]  

\[
i_q = \frac{1}{3} \frac{\rho_{\omega d} \pi R^4}{p \lambda_{pm}} \frac{C_r}{\omega^2} \theta^2
\]

**4.5.2. Generator losses**

The losses in the PM generator were assessed in an effort to accurately predict the performance of the WECS when operated in each of the four regions of operation. The dominant losses considered were the copper and rotational losses.
The rotational losses include core losses and friction & windage losses. The rotational losses are speed dependent and can be fully characterised at no-load. This is due to the presence of core losses even at no-load, which result from the rapid change in flux density in the stator teeth and yoke, as the edges of the PMs rotate through the airgap [4.9].

The rotational losses of the generator were measured, and are plotted as a function of speed in Fig. 4.8. A best-fit quadratic approximation to the experimental data is also illustrated in Fig. 4.8.

![Graph showing rotational losses of generator vs speed](image)

**Fig. 4.8. Rotational losses of generator vs speed**

A generalised expression for the rotational losses of the machine as a function of speed can therefore be written as:

\[ P_{rot}(N_r) = 0.00012N_r^2 + 0.07507N_r + 0.11593 \]  \hspace{1cm} (4-12)

The stator copper losses of the generator can be written in terms of the \( q \)-axis current as:

\[ P_{Cu} = \frac{3}{2} i_q^2 R_n \]  \hspace{1cm} (4-13)
The variation of the generator copper and rotational losses with wind speed and turbine shaft speed is shown in Fig. 4.9 and Fig. 4.10 respectively, for the four regions of turbine operation.

**Fig. 4.9.** Generator copper and rotational losses vs wind speed for all operating regions

**Fig. 4.10.** Generator copper and rotational losses vs shaft speed for all operating regions
4.5.3. Generator Efficiency

The efficiency of the PM generator can be expressed as \( \eta = \frac{P_{\text{gen}}}{P_{\text{shaft}}} \), where \( P_{\text{shaft}} \) is the input mechanical power applied to the shaft of the generator by the turbine and \( P_{\text{gen}} \) is the real power output of the generator. The input mechanical and output electrical power of the generator can further be related by:

\[
P_{\text{gen}} = P_{\text{shaft}} - P_{\text{e}} - P_{\text{m}}
\]  

Equation (4-14) is applicable for steady-state conditions, as the rotational energy stored in the drive train of the WECS has been omitted. Steady-state conditions are of interest in order to simplify the performance analysis of the machine.

The input mechanical and estimated output electrical power of the PM generator are plotted in Fig. 4.11 as a function of wind speed, for the four regions of turbine operation. The generator power limit is also clearly illustrated in the figure.

![Diagram](image)

Fig. 4.11. Input shaft and output electrical power of the PM generator

With reference to Fig. 4.11, it can be seen that the output electrical power of the generator is maintained constant in region III, whilst the power captured by the turbine is increased. This is required in order to compensate for the drop in generator
efficiency in region III, which is clearly illustrated in Fig. 4.12.

The variation of the estimated generator efficiency with wind speed and turbine shaft speed is shown in Fig. 4.12 and Fig. 4.13 respectively, for the four regions of turbine operation.

Fig. 4.12. Generator efficiency vs wind speed for all operating regions

Fig. 4.13. Generator efficiency vs shaft speed for all operating regions
4.6. Annual Energy Capture

In general, the wind speed information for an area can be modeled by a statistical Weibull or Rayleigh distribution [4.2],[4.5]. These distributions report on the frequency of occurrence of wind speeds in the area over a period of time and are useful in comparing the wind potential of several areas. For the purposes of this chapter, wind speed information is modeled by a Rayleigh distribution. Moreover, the annual energy capture of the WECS under investigation is assessed for areas with Rayleigh wind speed distributions, but with various average wind speeds.

For a Rayleigh distribution of wind speeds, the likelihood of a prevailing wind having a particular speed \((U_i)\) can be expressed by the probability density function \(f(U_i)\), as [4.2],[4.5]:

\[
f(U_i) = \frac{U_i}{\bar{U}^2} e^{-\frac{U_i^2}{\bar{U}^2}}
\]  

(4-15)

where \(\bar{U}\) is the average wind speed for the area.

The total number of hours per year for a particular wind speed \((U_i)\) can be determined by simply multiplying the probability of its occurrence by the duration of one year. This can be expressed as [4.5]:

\[
H(U_i) = 365 \times 24 \times f(U_i) \Delta U
\]  

(4-16)

The energy contribution at each wind speed can be determined by the product of the power output of the WECS at a specific wind speed and the duration that the wind speed occurs annually. The annual energy output (AEO) of the WECS can be determined by summing the incremental energy contributions at each wind speed. This can be expressed as [4.5]:

\[
AEO = \sum_{i=1}^{n} P(U_i) \cdot H(U_i)
\]  

(4-17)

where \(P(U_i)\) is the power output of the WECS at wind speed \(U_i\).
The ideal energy capture of the WECS, which excludes the generator losses, is compared to that including the generator losses. For this comparison, the expressions used for $P_{\text{shaft}}$ and $P_{\text{gen}}$ to obtain Fig. 4.11, are substituted in turn for $P(U_a)$ in (4-17), thereby resulting in the ideal and real energy capture of the WECS. The comparison is made for operation in regions I, II and III, for areas with various average wind speeds and is illustrated in Fig. 4.14.

With reference to Fig. 4.14, the total AEO represents the sum total of energies from the three regions of operation at each average wind speed. The AEO from region III exceeds that obtained from region II at high average wind speeds. This is due to an increased frequency of occurrence of winds at higher speeds for a Rayleigh distribution with a higher mean. This in turn causes the WECS to operate more often in its power limited region (III), thus resulting in a greater energy yield at higher average wind speeds.

The real energy capture of the WECS is shown in Fig. 4.15, for each of the four regions of turbine operation. With reference to Fig. 4.15, the energy capture in region II is compared to that of a new region (II'). Region II' is defined as the speed limited region of turbine operation following the transition from region I' to region II, i.e. the region between wind speeds $U_a'$ and $U_b$, in Fig. 4.11.

With reference to Fig. 4.15, the energy capture in region I' always exceeds that of region I. This is because the WECS exits region I' at a higher wind speed ($U_a'$) than it does region I ($U_a$), and consequently captures power at higher wind speeds in region I'. This however results in a lower energy yield in region II' compared to region II. This is due to the additional energy captured by the WECS in region II, between wind speeds $U_a$ and $U_a'$.

The total annual energy capture when the WECS is operated in regions I, II and III throughout its wind speed range, always exceeds the total energy capture when it is operated in regions I, II' and III, as illustrated in Fig. 4.15. This is expected since the output electrical power of the generator, when operated in region I, always exceeds that of region I', as shown in Fig. 4.11.
Fig. 4.14. Comparison of ideal and real AEO for WECS operation in regions I, II and III at various average wind speeds.

Fig. 4.15. Comparison of AEO of PM generator at various average wind speeds, for operation in regions I, II, and III.
4.7. Comparison of WECS Operation in Regions I and I'

A comparison is made between the operating parameters of the WECS when it is operated in regions I and I'. More specifically, the comparison is made by plotting the change in operating parameters between the two regions of operation. The percent change in shaft torque, efficiency, shaft power and generator output power is illustrated in Fig. 4.16 and Fig. 4.17, as a function of wind speed and turbine shaft speed, respectively.

With reference to in Fig. 4.16 and Fig. 4.17, it can be seen that operation in region I' results in a 4.3% increase in the turbine shaft torque compared to that of region I. The increased driving torque is thus available at the shaft of the PM generator throughout the wind/shaft speed range in region I', to overcome the parasitic torques inherently present in a PM machine. Operation in region I' does however result in a 5.1% drop in the power captured by the turbine throughout its wind/shaft speed range in region I', when compared to that of region I. The increase in shaft torque and consequent decrease in power captured in region I' is of course related to the fact that the turbine operates at \( \lambda = \lambda_{\text{opt}} \cdot C_T(\lambda_{\text{opt}}) - C_{T_{\text{max}}} \), as shown in Fig. 4.1.

An initial 1.1% drop in the output electrical power of the generator is caused by the 5.1% drop in the power captured by the turbine. Moreover, this results in an initial 4.2% increase in the efficiency of the WECS at low wind/shaft speeds in region I'. A sustained drop in the output electrical power of the generator is experienced throughout region I. This results mainly from an increase in the stator copper losses caused by an increase in the \( q \)-axis current when operating at increased shaft torque throughout region I'. The efficiency of the WECS drops by 1% at the end of region I'.

The percent change in the real energy capture of the WECS at various average wind speeds is illustrated in Fig. 4.18. With reference to Fig. 4.18, it can be seen that the energy capture of the WECS initially drops by 1.7% when operated in region I', thereafter increasing steadily with average wind speed. The energy capture increases by 36.5% in areas with an average wind speed of 8m/s when operated in region I'.
compared to region I. The change in energy capture for turbine operation in region II' and II is also illustrated in the figure. Furthermore, the total energy capture drops by between 5% and 2.6% over the range of average wind speeds when the turbine is operated through regions I, II' and III compared to when it is operated through regions I, II and III.

Fig. 4.16. Percentage change in operating parameters vs wind speed, for WECS operation in region II' as compared to that of region I

Fig. 4.17. Percentage change in operating parameters vs shaft speed, for WECS operation in region II' as compared to that of region I
Fig. 4.18. Percentage change in AEO of WECS for operation in region I' as compared to that of region I.
4.8. Conclusions

Operation of the turbine in region I' (\( \lambda = \lambda_{p_{\text{max}}}, C_f(\lambda_{p_{\text{max}}}) = C_{T_{\text{max}}} \)) results in increased shaft torque throughout the wind/shaft speed range when compared to its operation in region I (\( \lambda = \lambda_{p_{\text{max}}}, C_f(\lambda_{p_{\text{max}}}) = C_{P_{\text{max}}} \)). The increased driving torque is thus available at the shaft of the PM generator to overcome the parasitic torques inherently present in a PM machine. Furthermore, the electrical energy captured by the WECS when operated in region I' increases with the average wind speed of the area in which it is located. Operation in region I' also results in increased efficiency of the WECS in low wind/shaft speed conditions. These benefits are however obtained at the expense of a reduction in the output electrical power and hence the total energy captured by the WECS.
4.9. References


Chapter 5

Design of a PM wind generator, optimised over a wide operating range

5.1 Overview

This chapter outlines the design procedure for a permanent-magnet (PM) wind generator, for optimal performance over a wide operating range (i.e. a range of speeds and loads). This is achieved by using an algorithm to find the combination of generator design variables, which optimises its performance when operated through the normal operating regions of a variable speed wind turbine. In so doing, the efficiency vs. speed curve of the generator is shaped to produce a high average efficiency over the operating range of the turbine.
5.2 Introduction

A direct-drive WECS with fixed-pitch blades is typically operated at variable speeds, which offers the advantage of maximising the energy capture from prevailing winds. However, a consequence of variable speed operation of a WECS is that the generator does not operate for substantial periods at its rated operating point (i.e. rated speed and load). With conventional machine design procedures producing high absolute efficiencies at the rated operating point, the energy capture of a variable speed WECS is therefore naturally reduced due to low generator efficiency at operation away from the designed operating point.

In this chapter, the design procedure is outlined for a permanent-magnet (PM) wind generator, which optimises its performance over a wide range of operating speeds and loads. This is achieved by using an algorithm to find the combination of generator design variables, which optimises its performance when operated through the normal operating regions of a variable speed wind turbine. In so doing, the efficiency vs. speed curve of the generator is shaped to produce a high average efficiency over the operating range of the turbine.

In designing the PM wind generator, this chapter starts by sizing the turbine rotor blades and shaft speed for a range of WECS output powers and rated wind speeds. The sizing of radial-flux and axial-flux PM wind generators are then addressed. The normal operating regions of a variable speed WECS is then considered. The optimisation procedure for the wind generator is then discussed. The design routine is then applied to a 1kW radial-flux PM wind generator design.
5.3 Turbine Blade Sizing and Shaft Speed

The rotor blade diameter and operating shaft speed of a small direct-drive WECS is dependent on its output electrical power and rated wind speed requirements. As an example, a high-power WECS operating in a high wind speed region could require smaller blades than a low-power WECS operating in a low wind speed region. It is therefore important that due consideration be given to the sizing of the turbine blades for any small wind generator design. The functional relationship between the parameters which determine the turbine blade sizing and shaft speed will be discussed further in this section.

The power captured by a wind turbine from the incident wind can be expressed as [5.1]:

\[ P_{\text{out}} = \frac{1}{2} \rho_{\text{air}} \pi R^2 U^3 C_p \]  

(5-1)

where \( \rho_{\text{air}} \) is the density of air at the height of the turbine hub and \( C_p \) is the dimensionless power coefficient or aerodynamic efficiency of the turbine.

The power coefficient of the turbine is a function of the tip speed ratio (\( \lambda \)) at which the turbine is operating. This ratio is defined as the ratio of blade tip speed to wind speed, and can be expressed as:

\[ \lambda = \frac{\omega R}{U} \]  

(5-2)

Equations (5-1) and (5-2) can be used to determine an expression for the torque applied to the turbine shaft, which can be expressed as [5.1]:

\[ T_{\text{shaft}} = \frac{P_{\text{out}}}{\omega_{\text{r}}} = \frac{1}{2} \rho_{\text{air}} \pi R^2 U^3 C_T \]  

(5-3)

where \( C_T = \frac{C_p}{\lambda} \) is the torque coefficient of the turbine.
The power and torque coefficients for a typical 3-bladed Horizontal Axis Wind Turbine (HAWT) are shown in Fig. 5.1.

In general, most modern 3-bladed HAWTs are designed to operate at maximum aerodynamic efficiency at tip speed ratios between $\lambda_{P_{\text{opt}}} = 5-7$ [5.2]. Furthermore, the maximum aerodynamic efficiencies ($C_{P_{\text{max}}}$) typically vary between 0.35 and 0.45 [5.2].

The output electrical power of a wind generator ($P_{\text{gen}}$) can be written in terms of its efficiency ($\eta_{\text{gen}}$) and shaft power, as:

$$P_{\text{gen}} = \eta_{\text{gen}} P_{\text{shaft}}$$

(5-4)

Combining equations (5-1) and (5-4), facilitates the sizing of a typical HAWT. In particular, the radius of the turbine blades can be determined as a function of the rated output electrical power of the WECS and its operating wind speed. This can be expressed as:

$$R = \sqrt{\frac{P_{\text{gen}}}{\frac{1}{2} \rho_{\text{air}} \pi U^3 C_p \eta_{\text{gen}}}}$$

(5-5)
Chapter 5: Design of a PM wind generator, optimised over a wide operating range

The turbine blade sizing requirements for various WECS output powers and rated wind speeds are illustrated in Fig. 5.2. The curves relate to small 3-bladed HAWTs with $C_{P_{\text{max}}}=0.4$, $\lambda_{\text{opt}}=6$ and $\eta_{\text{gen}}=80\%$.

Equations (5-1), (5-2) and (5-4) can be combined in a similar manner to determine the operating shaft speed as a function of the WECS output power and rated wind speeds. This can be expressed as:

$$\omega = \sqrt{\frac{V_2 \rho \pi \lambda^2 U^2 C_{p} \eta_{\text{gen}}}{P_{\text{gen}}}}$$

The curves showing the turbine shaft speeds for various WECS output powers and rated wind speeds are illustrated in Fig. 5.3, for the same 3-bladed HAWTs with $C_{P_{\text{max}}}=0.4$, $\lambda_{\text{opt}}=6$ and $\eta_{\text{gen}}=80\%$.

Fig. 5.2 and Fig. 5.3 can be used as a guideline when designing small direct-drive WECSs with varying output power and rated wind speed requirements. As an example, assume that a small WECS with an output power of 1 kW at a rated wind speed of 8 m/s is of interest. It can be seen from Fig. 5.2 that this WECS will require a HAWT with a radius of 1.75 m. Furthermore, it can be seen from Fig. 5.3 that the rated speed of the WECS will be 250 rpm. The rated speed and output power of the WECS can now be used to facilitate the design of the PM wind generator.
Fig. 5.2. Turbine blade radius as a function of generator output power, at various rated wind speeds.

Fig. 5.3. Shaft speed as a function of generator output power, at various rated wind speeds.


5.4 PM Wind Generator Sizing

The sizing and main dimensions of radial and axial-flux PM wind generators are determined in this section. The radial-flux machines under investigation in this chapter have radially magnetised NdFeB PMs mounted on the surface of a solid mild-steel rotor core. The stator core consists of silicon sheet steel laminations with open rectangular slots. The axial-flux machines have axially magnetised PMs mounted on the surface of two mild-steel disc rotor cores. The stator consists of a tape-wound silicon steel core with open rectangular slots at each end of the core. The stator is mounted between the rotor discs. A distributed, three-phase winding is considered for all machines.

The relationship between the main dimensions in the radial and axial-flux machines are illustrated Fig. 5.4.

![Diagram of radial and axial-flux machines](image)

Fig. 5.4. Relationship between main dimensions in the radial and axial-flux machines [5.3], [5.4]

A simple transformation can be used to relate the dimensions of the axial-flux machine topology to those of the conventional radial-flux topology. This facilitates the use of traditional radial-flux machine design procedures for an axial-flux machine design. The transformation can be expressed as [5.3],[5.4],[5.7]:

\[
D \Rightarrow D_{ax} = \frac{(D_r + D_j)}{2} \\
L \Rightarrow L_c = \frac{(D_r - D_j)}{2}
\]

(5.7)
where \( D \) and \( L \) is the airgap diameter and axial length of a radial-flux machine; \( D_o, \)
\( D_i, \) and \( D_{ave} \) are the outer, inner, and average diameters, respectively of an axial-flux
machine; \( L_e \) is the effective length of the stator core in an axial-flux machine.

\subsection*{5.4.1 Radial-Flux PM Wind Generator Sizing}

The apparent power of a radial-flux machine at the airgap can be written in terms of the
main dimensions of the machine as [5.6],[5.7]:

\[
S_p = \frac{1}{2} \pi \cdot K_{wl} \cdot D^2 \cdot L \cdot n_s \cdot S_{ML pk} \cdot S_{EL pk}
\]  

(5-8)

where \( K_{wl} \) is the fundamental winding factor, \( n_s \) is the rotational speed in rev/sec,
\( S_{ML pk} \) and \( S_{EL pk} \) are the peak values of the specific magnetic and electric loadings
of the machine.

The output electrical power of a radial-flux generator can be related to its apparent
airgap power by the following expression [5.7]:

\[
P_{gen} = 3 V_e I_e \cos \phi = \frac{3 E_e}{E_t} I_e \cos \phi = \frac{1}{\eta} S_p \cos \phi
\]

(5-9)

where \( \eta = E_e / E_t \) is ratio of rated excitation voltage to terminal voltage of the
machine.

The product \( D^2 L \), which is proportional to the rotor volume of a radial-flux machine,
determines its output torque capability [5.5]. This product can be determined by
combining (5-8) and (5-9), and can be expressed as:

\[
D^2 L = \frac{8 P_{gen}}{0.5 \pi \cdot K_{wl} \cdot n_s \cdot S_{ML pk} \cdot S_{EL pk} \cdot \cos \phi}
\]

(5-10)

where \( P_{gen} \) is the output electrical power of the generator, \( K_{wl} \) is the fundamental
winding factor, \( n_s \) is the rotational speed in rev/sec, \( S_{ML pk} \) and \( S_{EL pk} \) are the peak
values of the specific magnetic and electric loadings of the machine.

With the \( D^2 L \) product determined from (5-10), the relative apportionment of \( D \) and \( L \)
in a conventional radial-flux machine design is purely based upon practical requirements of the machine application [5.7], [5.11]. This is facilitated by the choice of a suitable aspect ratio coefficient, defined as \( K_r = L/D \), for each application. Typical values of \( K_r \) reported in the literature vary widely from 0.14 to 0.5 for direct-drive PM wind generator applications [5.12][5.15][5.20][5.21].

In choosing the airgap diameter in a radial-flux PM wind generator design, due consideration should be given to the number of poles required and hence the resulting pole pitch of a design. The diameter should however be restricted in order to limit the proportion of inactive copper in the overhang [5.6].

The airgap diameter of generators for various WECS output powers and rated wind speeds are illustrated in Fig. 5.5.

![Airgap diameter of PM wind generators as a function of generator output power, at various rated wind speeds](image)

**Fig. 5.5.** Airgap diameter of PM wind generators as a function of generator output power, at various rated wind speeds
5.4.2 Axial-Flux PM Wind Generator Sizing

The outer diameter of an axial-flux machine is the most important dimension, as it determines the rated output power of the machine. Maximum power is developed by an axial-flux PM machine if the diameter ratio \( k_d = D_j/D_i \), is set to 1.73 \([5.3]\). The equivalent \( D^2 L \) product in an axial flux machine can be expressed in terms of the diameter ratio \( k_d \), as \([5.7]\):

\[
D_{av}^2 l_e = \frac{1}{8} \left( 1 + \frac{1}{k_d} \right) \left( 1 - \frac{1}{k_d^2} \right) D_o^3 = K_1 D_o^3
\]

(5-11)

where \( K_1 = 0.131 \) for maximum power to be developed by an axial-flux PM machine.

An equation similar to (5-10) can be written for an axial-flux PM generator in terms of \( D_{ave} \) and \( L_e \). Furthermore, with the aid of (5-11), an expression for the outer diameter of an axial-flux PM generator can be written as:

\[
D_o = \sqrt[3]{\frac{e P_{max}}{\pi \cdot K_w \cdot K_d \cdot n_i \cdot SML_{st} \cdot SEL_{ph} \cdot \cos \phi}}
\]

(5-12)

Equation (5-12) is applicable to a double-airgap axial-flux PM generator. For a single-airgap machine, the result from (5-12) should be multiplied by \( \sqrt{2} \).
5.5 Magnetic Circuit Design

The magnetic circuit design of the PM wind generator is considered in this section. In particular, the required number of generator poles, PM material sizing, and stator and rotor core dimensions are discussed.

5.5.1 Airgap flux density

The airgap flux density produced by the PMs in the wind generator configurations are considered to be approximately rectangular in shape. The $S_{ML\text{pk}}$ is defined as the peak value of the fundamental space harmonic component of the airgap flux density distribution $B_{L_{\text{max}}}$. It can be related to the plateau value $B_{E}$, and the average value $B_{\text{ave}}$, of the rectangular airgap flux density distribution, as follows [5.7]:

$$B_{\text{max}} = k_{f}B_{E} = k_{f} \cdot \sqrt{\frac{\alpha}{\alpha}} \cdot B_{\text{ave}}$$  \hspace{1cm} (5-13)

where $k_{f}$ is the form factor of the excitation field and $\alpha$ is the pole arc to pole pitch ($\tau_{P}$) ratio of the PMs.

In the case of a single smooth PM per pole, $k_{f}$ can be expressed as [5.7]:

$$k_{f} = \frac{4}{\pi} \sin \left( \frac{\alpha \pi}{2} \right)$$  \hspace{1cm} (5-14)

The airgap flux density level in a PM machine is constrained by saturation of the stator teeth [5.8]. The $S_{ML\text{pk}}$ of the machine considered was therefore chosen to be 0.9T. This flux density level provides a good compromise between adequate torque production and saturation of the magnetic circuit of PM machines [5.5].

In conventional PM machine designs with only a few pole pairs, a pole pitch is generally much larger than a stator slot pitch. The ratio $\alpha$ can therefore be selected independently, as it does not affect the saturation of the stator teeth. This may not be the case in PM wind generator designs with many pole pairs, where a pole pitch may be comparable to a slot pitch. The prescribed range for $\alpha$ is: 0.67-0.77 [5.5],[5.8].
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5.5.2 Number of Generator Poles

The operating frequency range for small PM wind generators is reported as typically: 30-80Hz [5.12] and 10-70Hz [5.13]. Thus, for the purposes of this chapter, the nominal frequency of the generator at rated wind speed is chosen to be within the range 45-65Hz. The required number of generator poles for various WECS output powers and rated wind speeds are illustrated in Fig. 5.6. The figure is applicable to both radial and axial-flux PM wind generators.

Fig. 5.6. Number of poles for generators operating at a nominal frequency of 60Hz, as a function of generator output power, at various rated wind speeds

5.5.3 PM Material Sizing

The operating point of a magnetic circuit can be determined by considering the demagnetisation curve of the PM material and the load-line representing the magnetic circuit. Furthermore, the slope of the open circuit load-line, defined as the permeance coefficient (PC), provides a measure of the withstand capability of the PMs to demagnetisation by external applied magnetic fields. In PM machines with surface mounted PMs, adequate margin against demagnetisation is ensured with a PC of 6 or more [5.5],[5.14]. The length of the PM material required for a particular machine
coefficient \((PC)\), provides a measure of the withstand capability of the PMs to
demagnetisation by external applied magnetic fields. In PM machines with surface
mounted PMs, adequate margin against demagnetisation is ensured with a \(PC\) of 6 or
more \([5.5],[5.14]\). The length of the PM material required for a particular machine
design can be expressed in terms of the \(PC\) as \([5.5]\):

\[
I_n = (PC - \rho_n \mu_n) \cdot C_\phi K_c I_s \tag{5-15}
\]

where \(C_\phi\) is the flux focusing factor, \(K_c\) is Carter's coefficient and \(\rho_n\) is the normalised
rotor leakage permeance. The range of values for \(\rho_n\) is typically 0.05 - 0.2 \([5.5]\).

It can be seen that the length of the PM material required to provide adequate
excitation in a PM machine is largely dependant on the airgap length in the machine.

The airgap length in the PM wind generator design considered in this chapter was
chosen to be 1\% of \(D\) \([5.15]\). A larger airgap length may however be required
practically, in axial-flux PM machine designs in order to reduce the axial forces
between stator and rotor structures.

### 5.5.4 Stator and Rotor Sizing

The steel yokes of the stator and rotor cores provide the return paths for flux between
poles. Saturation of these flux paths are avoided if the respective yoke cross-sectional
areas are adequate. For a given axial length of a radial-flux machine, this is ensured by
choosing the yoke heights as follows \([5.11],[5.14]\):

\[
h_y > \frac{\alpha \pi D}{4p K_s} \cdot \frac{B_s}{B_{sat_stator}} \tag{5-16}
\]

\[
h_r > \frac{\alpha \pi (D - 2l_y)}{4p} \cdot \frac{B_s}{B_{satRotor}} \tag{5-17}
\]

where \(K_s\) is the stacking factor of the stator laminations, \(h_y\) and \(h_r\) are the heights of
the stator and rotor yokes respectively, \(B_{sat_stator}\) and \(B_{satRotor}\) are the saturation flux
densities of stator and rotor steels.
The stator yoke thickness in an axial-flux PM machine design can be determined using (5-16), by substituting for $D_{me}$ from (5-7). However, the rotor yoke thickness of an axial-flux machine can be determined as follows:

$$h_{ry} > \frac{\alpha \pi D_{me} B_s}{4p B_{sat, max}}$$

(5-18)

The values of $h_{sy}$ and $h_{ry}$ determined for an axial-flux machine are the yoke thicknesses of a single stator and single rotor module, respectively. The stator yoke thickness in a double-airgap machine with two rotor discs would be $2h_{sy}$, if the excitation flux is required to travel circumferentially through the stator in order to link with adjacent poles on the rotor discs. This occurs when PMs on the rotor discs are arranged such that poles with opposite magnetisation directions face each other. This configuration produces lower axial forces between rotor and stator modules compared to the configuration where the poles facing each other have the same magnetisation direction. The stator yoke can be completely eliminated in the latter configuration, since the flux travels axially between facing poles on the two rotor modules.

Saturation of the stator teeth is avoided if adequate tooth area is provided. For parallel-sided teeth and approximately rectangular stator slots in a radial-flux machine design, this is ensured by choosing widths of the stator teeth as follows:

$$w_t > \frac{\tau_s B_s}{K_s B_{sat, max}}$$

(5-19)

where $\tau_s = \pi D/S$ is the stator slot pitch.

The corresponding widths of the stator slots can be expressed as: $w_s = \tau_s - w_t$. In most machine applications, the ratio of slot width to slot pitch is usually within the range: $0.5 < w_s/\tau_s < 0.6$ [5.14]. However, if $w_s = w_t = 0.5\tau_s$, the product of specific electric and magnetic loadings of the machine is maximised, which in turn maximises the torque capability of the machine [5.8]. Thus, for PM wind generator designs with equal tooth
and slot widths, (5-19) establishes a constraint on the airgap flux density which will ensure that saturation of the stator teeth is avoided. This can be expressed as:

\[ B_g < \frac{1}{2} K_r B_{sat \_stator} \]  

(5-20)

In axial-flux PM machines designs, parallel-sided slots are easier to machine but result in tapered stator teeth. It is therefore important to ensure that the narrowest part of the stator teeth do not saturate to unacceptable levels. This is ensured by choosing the width of the stator teeth along the inner diameter \( w_{\text{st}, \text{in}} \), such that:

\[ w_{\text{st}, \text{in}} > \frac{r_{\text{st}, \text{in}}}{K_r} \frac{B_g}{B_{sat \_stator}} \]  

(5-21)

where \( r_{\text{st}, \text{in}} = \pi D_{\text{in}} / S \) is the stator slot pitch at \( D_{\text{in}} \).
5.6 Electrical Design

The specific electric loading of a machine is the circumferential current density of the stator. It is limited by the slot-fill factor, slot height and current density of stator conductors [5.5]. The range of peak specific electric loadings ($SEL_{pk}$) for small PM machines is typically: 10,000 - 40,000A/m [5.22].

The slot-fill factor ($K_s$) is defined as the ratio of copper to slot area and is usually in the range: 0.3-0.5 for small low-voltage machines [5.7],[5.8]. This range ensures that the stator cores can be easily wound.

The current density ($J$) of the stator conductors in low voltage machines range between: 3-8A/mm² [5.6]. Current densities exceeding this range, up to a maximum of 10A/mm², may be tolerable in small PM wind generator designs provided that the insulation class of the windings is at least F, and that adequate cooling is provided [5.7].

The required height of the stator slots for the specific electric loading, slot-fill factor and current density chosen for a radial-flux machine with parallel-sided stator teeth can be expressed as:

$$h_s = \frac{SEL_{pk}}{\sqrt{2 \cdot J \cdot K_s \cdot (1 - w_l/r_s)}} \quad (5-22)$$
5.7 Validation of Turbine Sizing and Machine Design Equations

Validation of the turbine sizing and machine design equations is provided by comparing the main specifications of a commercial WECS to that of a system designed for the same wind speed (8m/s) and power rating (1kW) by means of the sizing and design equations presented in the previous sections. The designed system was not optimised for the purpose of the comparison to the commercial system. The commercial system under consideration here is similar to the one considered in chapter 4. Its generator is a 3-phase, 1kW, 30-pole PM synchronous machine with an outer rotor construction. The machine has radially magnetised ferrite PMs mounted on the inner surface of a mild steel rotor core. The per-phase armature resistance ($R_a$) is 0.4Ω and the flux linkage of the PMs ($\lambda_{PM}$) is 0.11Wb-turns. The rated frequency of the commercial system considered here is 50Hz and its rated speed is 200rpm. The tip speed ratio ($\lambda_{opt}$) for the commercial system is 4.5. This tip speed ratio has also been chosen for the designed WECS, for the purposes of the comparison. It should be noted that a more optimal tip speed ratio of 6, for a modern 3-bladed HAWT, was used to produce the guidelines illustrated in Figs. 5.2, 5.3, 5.5 and 5.6. The comparison of the main specifications of the designed WECS to that of the commercial system is illustrated in Table 5.1.

Validation of the analytical model used to predict the performance of PM wind generator designs was achieved through comparison with experimental results. This was presented in Chapter 2 where the analytical model was validated with the experimental results from a different machine to the commercial system under consideration above.

From the results presented in this section and in chapter 2, it can be seen that good agreement exists between the calculated and measured design specifications, equivalent circuit parameters and losses of actual machines. The PM wind generator sizing and design equations have therefore been validated together with the analytical
model presented in chapter 2.

The sizing and design equations and the analytical model will be used in subsequent sections of this chapter to optimise the design of a PM wind generator.

**Table 5.1**

Comparison of Main Specifications of a Designed WECS to That of a Commercial WECS

<table>
<thead>
<tr>
<th>Design Specification</th>
<th>Designed System</th>
<th>Commercial System</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated wind speed ((U))</td>
<td>8 m/s</td>
<td>8 m/s</td>
</tr>
<tr>
<td>Tip speed ratio ((\lambda_{rpm}))</td>
<td>4.5</td>
<td>4.5</td>
</tr>
<tr>
<td>Turbine blade diameter ((2R))</td>
<td>3.56 m</td>
<td>3.6 m</td>
</tr>
<tr>
<td>Rated speed ((N_r))</td>
<td>206 rpm</td>
<td>200 rpm</td>
</tr>
<tr>
<td>Airgap diameter ((D))</td>
<td>0.3158 m</td>
<td>0.3238 m</td>
</tr>
<tr>
<td>Axial length of stator core ((l))</td>
<td>0.0632 m</td>
<td>0.0647 m</td>
</tr>
<tr>
<td>No. of poles ((2p))</td>
<td>30</td>
<td>30</td>
</tr>
<tr>
<td>Length of magnets ((l_m))</td>
<td>0.0171 m</td>
<td>0.0201 m</td>
</tr>
<tr>
<td>Type of PMs</td>
<td>Ferrite</td>
<td>Ferrite</td>
</tr>
<tr>
<td>Height of stator yoke ((h_{sy}))</td>
<td>0.00512 m*</td>
<td>0.0212 m</td>
</tr>
<tr>
<td>Height of rotor yoke ((h_{rp}))</td>
<td>0.00434 m*</td>
<td>0.0495 m</td>
</tr>
<tr>
<td>Width of stator tooth ((w_t))</td>
<td>0.0034 m*</td>
<td>0.0042 m</td>
</tr>
<tr>
<td>Number of Stator Slots ((S))</td>
<td>90</td>
<td>90</td>
</tr>
<tr>
<td>Conductor diameter</td>
<td>0.00174 m</td>
<td>0.00134 m</td>
</tr>
</tbody>
</table>

* Note that these values are the minimum values tolerable for the machine design, as indicated by equations (5-16), (5-17) and (5-19).
5.8 PM Wind Generator Control and Performance

In general, three regions of operating tip speed ratio are defined for the operation of variable speed wind turbines. These regions are defined in relation to the power coefficient curve and include [5.16],[5.17],[5.18]:

- Region I, where \( \lambda = \lambda_{\text{opt}}, C_p(\lambda_{\text{opt}}) = C_{p_{\text{max}}} \)
- Region II, where \( \lambda < \lambda_{\text{opt}}, C_p(\lambda) < C_{p_{\text{max}}} \)
- Region III, where \( \lambda \ll \lambda_{\text{opt}}, C_p(\lambda) \ll C_{p_{\text{max}}} \)

Operation of a wind turbine through each of the three operating regions can be achieved by controlling the generator to which it is coupled. Aspects relating to the control of a PM wind generator and its performance are assessed in this section.

5.8.1 Generator Control

It is assumed that the PM wind generator is controlled by a Field Orientated Control (FOC) loop of a three-phase converter, implemented in the rotor reference frame. The speed reference for the FOC loop is generated as a function of wind speed from (5-2). The \( q \)-axis current reference is determined from the electromagnetic and applied shaft torques, in order to assess the machine losses. With negligible saliency present in a surface-mounted PM rotor construction \((L_d = L_q)\), maximum torque can be developed per ampere of stator current if \(i_d\) is maintained at zero [5.19]. The \( q \)-axis current can therefore be expressed as a function of wind speed, as:

\[
i_q = \frac{1}{3} \frac{p \rho_{\text{air}} \pi R^3}{p \lambda_{\text{opt}}^2} C_p U^2 \tag{5-23}
\]
5.8.2 Generator losses

The losses in the PM generator are assessed in an effort to accurately predict the performance of the WECS when operated in each of the three regions of operation. The dominant losses considered are the copper and rotational losses.

The rotational losses include core losses and friction & windage losses. The rotational losses are speed dependent and can be fully characterised at no-load. This is due to the presence of core losses even at no-load, which result from the rapid change in flux density in the stator teeth and yoke, as the edges of the PMs rotate through the airgap [5.8],[5.10]. The rotational losses of the generator are calculated using the models presented in [5.7],[5.8] and [5.10].

The stator copper losses can be written in terms of the $q$-axis current as:

$$P_{Cu} = \frac{3}{2} i_q^2 R_s$$  \hspace{1cm} (5-24)

The variation of the generator copper and rotational losses with wind speed and turbine shaft speed are determined for the three regions of turbine operation.

5.8.3 Generator Efficiency

The efficiency of the PM generator can be expressed as $\eta = P_{gen} / P_{shaft}$, where $P_{shaft}$ is the input mechanical power applied to the shaft of the generator by the turbine and $P_{gen}$ is the real power output of the generator. The input mechanical and output electrical power of the generator can further be related by:

$$P_{gen} = P_{shaft} - P_{Cu} - P_{rot}$$  \hspace{1cm} (5-25)

Equation (5-25) is applicable for steady-state conditions, as the rotational energy
stored in the drive train of the WECS has been omitted. Steady-state conditions are of interest in order to simplify the performance analysis of the machine.

5.8.4 Annual Energy Capture

In general, the wind speed information for an area can be modelled by a statistical Rayleigh distribution, as [5.1],[5.17]:

\[
f(U_i) = \frac{\pi U_i}{2 \bar{U}^2} e^{-\frac{U_i^2}{2 \bar{U}^2}}
\]

(5-26)

where \(\bar{U}\) is the average wind speed for the area and \(U_i\) is the likelihood of a prevailing wind having a particular speed.

The total number of hours per year for a particular wind speed \((U_i)\) can be determined by simply multiplying the probability of its occurrence by the duration of one year. This can be expressed as [5.17]:

\[
H(U_i) = 365 \times 24 \times f(U_i) \Delta U
\]

(5-27)

The energy contribution at each wind speed can be determined by the product of the power output of the WECS at a specific wind speed and the duration that the wind speed occurs annually. The annual energy output (AEO) of the WECS can be determined by summing the incremental energy contributions at each wind speed. This can be expressed as [5.17]:

\[
AEO = \sum_{i=1}^{n} P(U_i) \cdot H(U_i)
\]

(5-28)

where \(P(U_i)\) is the power output of the WECS at wind speed \(U_i\).

The energy capture of the WECS, in regions I, II and III can be now determined at various average wind speeds. The total AEO represents the sum total of energies from the three regions of operation at each average wind speed.
5.9 Optimisation of Wind Generator Design

A direct-drive WECS is operated at variable shaft speeds as the wind speed varies. This offers the advantage of maximising the energy capture from prevailing winds. However, a consequence of variable speed operation of a WECS is that the generator does not operate for substantial periods its rated operating point (i.e. rated speed and load). With conventional machine design procedures producing high absolute efficiencies at the rated operating point, a wind generator designed using this methodology will therefore result in a WECS with reduced energy capture. This is due to the low generator efficiency at operation away from the designed operating point.

The design procedure proposed in this chapter optimises the machine design over a wide range of operating speeds and loads. This is achieved by using an algorithm to find the combination of generator design variables, which optimises its performance when operated through the normal operating regions of a variable speed wind turbine.

5.9.1 Optimisation Algorithm

The Population Based Incremental Learning (PBIL) optimisation algorithm was used to optimise the generator design [5.23]. The PBIL algorithm is a stochastic non-linear programming technique that has many advantages over other stochastic optimisation techniques [5.7],[5.9]. It is essentially an abstraction of a classical Genetic Algorithm (GA) and therefore works in a similar manner to a GA [5.24].

Each design variable is coded as an n-bit binary number. The k design variables are then concatenated to form an n·k-bit binary string, which represents a trial solution vector. This can be expressed as:

$$|b_1b_2\cdots b_k|b_{k+1}b_{k+2}\cdots b_{2k}|\cdots|b_{k^2}b_{k^2+1}\cdots b_{kn}|$$

(5.29)

where b is a binary bit value.
During each generation, several pseudo-random trial solution vectors are generated in accordance with the population size and bias contained in a probability vector. The fitness of the trial solution vectors are evaluated simultaneously in each generation. The solution vector in each generation that results in the best fitness is allowed to influence the probability vector. The probability vector is adjusted in such a way that in subsequent generations trial solution vectors are generated in regions of the search space which produced previous good solutions. Each bit of the probability vector is updated based upon the update rule of competitive learning, which can be expressed as [5.23]:

\[ p_i = p_i \times (1.0 - LR) + LR \times v_i \]  

(5-30)

where \( p_i \) is the probability of generating a "1" in bit position \( i \), \( v_i \) is the \( i \)-th position in the solution vector which the probability vector is moved towards, and \( LR \) is the learning rate.

The generations therefore evolve with good solutions until future populations are saturated with good solutions. Premature convergence to a sub-optimal solution is prevented by the use of random sampling of the probability vector when generating trial solutions and by relaxing the bias of the probability vector toward a neutral value (0.5) after each generation [5.24],[5.25]. A global optimum solution is therefore likely to emerge during this search technique.

### 5.9.2 Design Variables

Ten design variables of the PM wind generator were selected to be optimised by the PBIL algorithm. Each design variable was coded as a 7-bit binary number. The design variables included: the remanent flux density of the PMs (\( B_r \)), the rated terminal voltage, frequency and power factor of the generator, the aspect ratio coefficient (\( K_L \)), the slot-fill factor (\( K_y \)), the pole-arc to pole-pitch ratio (\( a \)), the stator current density (\( J \)), the peak specific electric loading \( SEL_{pk} \) and the permeance coefficient (\( PC \)).
Suitable ranges were specified for the ten design variables based on design experience found in the literature. This was done in order to constrain the search to feasible regions in the design space. The ranges for the design variables are listed in Table 5.2. Trial wind generator designs are therefore restricted to permutations of the design variables within the specified ranges.

**Table 5.2**

**RANGES FOR PM WIND GENERATOR DESIGN VARIABLES**

<table>
<thead>
<tr>
<th>Design variable</th>
<th>Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>$V_e [V]$</td>
<td>180 – 260</td>
</tr>
<tr>
<td>$f [Hz]$</td>
<td>45 – 65</td>
</tr>
<tr>
<td>Power factor</td>
<td>0.7 – 0.9</td>
</tr>
<tr>
<td>$B_r [T]$</td>
<td>1.0 – 1.2</td>
</tr>
<tr>
<td>$K_L$</td>
<td>0.1 – 0.5</td>
</tr>
<tr>
<td>$K_d$</td>
<td>0.3 – 0.5</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>0.556 – 0.778</td>
</tr>
<tr>
<td>$SEL [A-Cond./m]$</td>
<td>10,000 – 40,000</td>
</tr>
<tr>
<td>$J [A/mm^2]$</td>
<td>3 – 8</td>
</tr>
<tr>
<td>$PC$</td>
<td>6 – 15</td>
</tr>
</tbody>
</table>

5.9.3 **Fitness Function**

The overall fitness of each trial wind generator design was evaluated by means of several single-valued objective functions. The overall fitness function was formulated by combining the objective functions with the aid of a goal programming technique and a weighted sum approach [5.26],[5.27]. The fitness function can be expressed in terms of a function to be minimised as:

$$F(\bar{x}) = \min \sum_{i=1}^{N} w_i |f_i(\bar{x}) - G_i|$$  \hspace{1cm} (5-31)

where $\bar{x} = [x_1, x_2, \ldots, x_N]^T$ is a vector of machine design variables and $w_i$ is an appropriate
Chapter 5: Design of a PM wind generator, optimised over a wide operating range

weighting for the \( i \)-th objective function \( f_i \), with a desired goal value of \( G_i \).

In optimising the design of a PM wind generator, it was desired to express the overall fitness of each design in terms of a function to be maximised. Equation (5-31) was therefore reformulated as:

\[
F(x) = \max \sum_{i=1}^{n} w_i \left( 1 - \frac{f_i(x) - G_i}{G_i} \right)
\]  

The overall fitness of each wind generator design is of course sensitive to the weightings selected in (5-32). For this reason, tests were conducted to investigate the effect of deliberately emphasising the importance of one single-valued objective function over the others in the overall fitness function. This was accomplished by appropriate selection of the weightings in (5-32). Suitable weightings were determined in this manner for the optimisation process. The single-valued objective functions used in the wind generator design included: 1/(mass of active materials), generator efficiency at rated wind speed, generator output power, phase current and voltage. An optimised wind generator design, which was obtained from the optimisation procedure, will be discussed in the next section.
5.10 Optimised PM Wind Generator Design

The optimisation routine and machine sizing equations discussed in the previous sections were used to design a 1kW radial-flux PM wind generator. The machine has NdFeB magnets mounted on the surface of low carbon steel rotor core and has a silicon steel stator core. The rated wind speed for the wind generator is 12$m/s$ and the tip speed ratio ($\lambda_{opt}$) is 6. The efficiency of the optimised design is compared to the commercial system discussed in section 5.7. The rated wind speed of the commercial system is 8$m/s$. Operation of both wind generators are considered in Region I only. Region I for the optimised wind generator design is over the range of wind speeds: 2.5 - 12$m/s$ and for the commercial system over the range: 2 - 8$m/s$.

In optimising the design of the PM wind generator, the following steps are taken: Several permutations of the design variables are used to size and design machines based on sections 5.3 – 5.6. The ranges for the design variables are illustrated in Table 5.2. The designs are then evaluated under operation with a turbine and Field Orientated Control (FOC), based on section 5.8. The optimisation routine then biases the selection of new design variables towards favourable wind generator designs. This iterative process continues until an optimised design is produced, which has minimum mass of active materials, maximum efficiency at rated wind speed and an output power of 1kW at rated wind speed.

The specifications of an optimised design are summarised in Table 5.3. The optimisation routine produced a design capable of delivering 1kW at rated wind speed and which had the lowest mass of active materials (7.30$kg$). The efficiency of the wind generator is reasonably high at rated wind speed (92.37%). The electric loading of 22,188$A/m$ and current density of 3.63$A/mm^2$ are reasonable. The gauge number for the conductors to be used in this machine is AWG-20. The variation of shaft power and generator output power with wind speed is shown in Fig. 5.7 for the optimised design. Furthermore, the variation of copper losses and rotational losses with wind speed is illustrated in Fig. 5.8. The asterisks in the figures show the operating points
**Table 5.3**

**Specifications of the Optimised PM Wind Generator Design**

<table>
<thead>
<tr>
<th>Design parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{\text{gen}}$ – Rated power</td>
<td>1 kW</td>
</tr>
<tr>
<td>$U$ – Rated wind speed</td>
<td>12 m/s</td>
</tr>
<tr>
<td>$\lambda_{\text{opt}}$ – Tip speed ratio</td>
<td>6</td>
</tr>
<tr>
<td>$R$ – Radius of turbine blades</td>
<td>0.9 m</td>
</tr>
<tr>
<td>$N_{\text{r}}$ – Rated shaft speed</td>
<td>720 rpm</td>
</tr>
<tr>
<td>$f$ – Rated frequency</td>
<td>60 Hz</td>
</tr>
<tr>
<td>$I_{\text{a}}$ – Rated current</td>
<td>1.55 A</td>
</tr>
<tr>
<td>$V_{\text{a}}$ – Rated voltage</td>
<td>240 V</td>
</tr>
<tr>
<td>$\eta$ – Rated power factor</td>
<td>0.9</td>
</tr>
<tr>
<td>$2p$ – Number of pole</td>
<td>10</td>
</tr>
<tr>
<td>$D$ – Airgap diameter</td>
<td>0.12 m</td>
</tr>
<tr>
<td>$l$ – Axial length of stator core</td>
<td>0.055 m</td>
</tr>
<tr>
<td>$S$ – Number of stator slots</td>
<td>30</td>
</tr>
<tr>
<td>$l_{\text{g}}$ – Mechanical airgap clearance</td>
<td>1.1 mm</td>
</tr>
<tr>
<td>$SEL_{pk}$ – Specific electric loading (peak)</td>
<td>22,188 A/m</td>
</tr>
<tr>
<td>$j$ – Current density</td>
<td>3.63 A/mm²</td>
</tr>
<tr>
<td>$G_{\text{a}}$ – Gauge of stator conductors</td>
<td>20 AWG</td>
</tr>
<tr>
<td>$N_{\text{ph}}$ – Number of turns per phase</td>
<td>635</td>
</tr>
<tr>
<td>$w_{S}$ – Width of a stator slot</td>
<td>0.0063 m</td>
</tr>
<tr>
<td>$h_{S}$ – Height of stator slot</td>
<td>0.0174 m</td>
</tr>
<tr>
<td>$h_{\text{gy}}$ – Height of stator yoke</td>
<td>0.0104 m</td>
</tr>
<tr>
<td>$h_{\text{ry}}$ – Height of rotor yoke</td>
<td>0.0089 m</td>
</tr>
<tr>
<td>$K_{\text{sf}}$ – Slot-fill factor</td>
<td>50%</td>
</tr>
<tr>
<td>$\tau_{\text{c}}$ – Stator coil pitch</td>
<td>180 °C</td>
</tr>
<tr>
<td>$SM_{\text{f-pk}}$ – Specific magnetic loading (peak)</td>
<td>0.92 T</td>
</tr>
<tr>
<td>$l_{\text{m}}$ – Axial thickness of PM</td>
<td>6.9 mm</td>
</tr>
</tbody>
</table>
Fig. 5.7. Shaft power and generator output power vs wind speed for optimised wind generator design.
Fig. 5.8. Copper losses and rotational losses vs wind speed for optimised wind generator design

The efficiency vs wind speed curves for the optimised wind generator design and the commercial system are illustrated in Fig. 5.9. The high efficiency of the optimised design at its rated wind speed (92.37%) is achieved with a relatively flat efficiency curve over a wide range of operating wind speeds. It can be seen that the efficiency of the optimised design drops by 0.58% over the wind speed range 12 - 8 m/s, and by 2.49% over the range 12 - 6 m/s. In contrast, it can be seen that the efficiency of the commercial system at its rated wind speed (8 m/s) is 88.49%. This efficiency is somewhat lower than that of the optimised design at its rated wind speed. Furthermore, the efficiency of the commercial system drops by 5.42% over the wind speed range 8 - 4 m/s.

The cumulative sum of annual wind speed durations are shown in Fig. 5.10 for average wind speeds of 12 and 8 m/s. A Raleigh distribution of wind speeds was used to obtain the cumulative durations shown in the figure. It can be seen that the optimised wind generator operates in its region 1 (2.5 - 12 m/s) for 4472.8 hours per
year and the commercial system operates in its region 1 (2 - 8m/s) for 4347.4 hours per year. The optimised wind generator design operates between the range of wind speeds 12 - 8m/s, for 2184 hours per year, which equates to 48.8% of its time in region 1. Thus, during approximately half of its operation in region 1, the optimised wind generator operates within 0.58% of its maximum efficiency. Furthermore, its operation between the range of wind speeds 12 - 6m/s, is for 3204 hours per year, which equates to 71.6% of its time in region 1. Thus, during 71.6% of the optimised wind generator’s operation in region 1 it operates within 2.49% of its maximum efficiency. In contrast, the commercial system operates between the range of wind speeds 8 - 4m/s, for 3204 hours per year, which equates to 73.7% of its time in region 1. Thus, during 73.7% of the commercial system’s operation in region 1 it operates within 5.42% of its maximum efficiency.

The energy capture of the commercial system within region 1 is exacerbated by its low efficiency at rated wind speed and also by the drop in its efficiency over the wind speed range 8 - 4m/s. In contrast to this, the high maximum efficiency of the optimised wind generator design, together with its relatively small drop in efficiency over a wide operating range (12 - 6m/s), will improve its energy capture within region 1. In particular, this characteristic improves the energy capture of the generator during fluctuations around the rated wind speed. The annual energy capture for the optimised wind generator design is $2.185 \times 10^6 \text{ Wh}$. 
Fig. 5.9. Efficiency vs wind speed curves for optimised and commercial generators.

Fig. 5.10. Cumulative sum of annual wind speed durations for 12 and 8 m/s average wind speeds.
5.11 Conclusions

This chapter outlined an optimisation design procedure for a PM wind generator, operating over a wide wind speed range. An optimal combination of generator design variables is selected by an optimisation routine, which minimises the mass of active materials, maximises the efficiency of the machine at rated wind speed and also ensures rated power delivery at rated wind speed. The optimisation routine evaluates several trial wind generator designs under operation with a turbine and uses this to guide the search for an optimised wind generator design. An optimised design of a 1kW PM wind generator was produced for a rated wind speed of 12m/s. A reasonably high efficiency of 92.37% is achieved with a relatively flat efficiency curve over a wide operating wind speed range. This is in contrast to a commercial system that was investigated, which was characterised by a rather low efficiency at rated wind speed and also by a larger drop in efficiency over a wide range of wind speeds. The efficiency of the optimised design drops by 0.58% of its maximum during approximately half of its operating time in region 1. Furthermore, its efficiency drops by 2.49% of its maximum during 71.6% of its operating time in region 1. This characteristic improves the energy capture of the generator during fluctuations around the rated wind speed.
5.12 References


Chapter 5: Design of a PM wind generator, optimised over a wide operating range


Chapter 6

Prototyping a composite SMC/Steel axial-flux PM wind generator

6.1 Overview

This chapter outlines the design and prototyping of an axial-flux PM wind generator with a composite SMC/steel stator core. The generator is intended for a new turbine with contra-rotating blades. The machine topology has SMC teeth fitted into a laminated silicon steel stator yoke. A simple pressed fit and wedge/retaining ring arrangement is proposed for fixing the SMC teeth into the yoke. The machine is sized based on wind speed requirements and SMC core samples available for the research. Preformed coils are used to form a concentrated, non-overlapping stator winding, which are inserted from the stator yoke side. Consequently, the slot openings are small, thereby reducing harmonics associated with the slot openings. The SMC cores are machined by means of a cost effective and easily accessible process using an end-
mill. A discussion is provided of the detrimental effect of this machining process on the SMC core. However, a chemical processing technique is investigated for treating the smear of the iron particles on the machined surfaces of the SMC core. Microscopic images are shown to illustrate the successful elimination of the smear on the machined surfaces. Assembly of the prototype is discussed in detail and experimental results are provided.
6.2 Introduction

Permanent-magnet (PM) machines are ideally suited for small wind energy conversion systems (WECSs), as they are inherently more efficient than wound-field machines. Moreover, PM machine rotors are easy to manufacture with the large number of poles required by low-speed, direct-drive WECSs. Axial-flux PM machines result in more compact designs with higher power densities than conventional radial flux PM machine types [6.1], [6.2]. Furthermore, the output power of an axial-flux machine is proportional to the cube of its outer diameter, thus implying a gradual increase in machine diameter as its power rating is increased [6.2]. The diameters of small axial-flux PM machines are therefore inherently large compared to radial-flux machines, thereby providing more space for the high number of poles required in wind generator designs. The volume savings provided by axial-flux PM machine designs also leads to more compact WECSs.

A potential drawback in axial-flux PM machine designs is the presence of high axial forces between rotor PMs and the stator ferromagnetic core. This is however more severe in medium and high power machines with large diameters and is therefore more tolerable in smaller machines. Another major drawback with axial-flux machines relates to the manufacture of the steel stator cores. These cores are formed by machining slots into a coiled-up steel lamination strip or by coiling a pre-punched lamination strip. The problems associated with each technique include short circuit paths created between laminations during the slot machining process and the difficulty involved in aligning the pre-punched slots when coiling the lamination strip [6.4].

Recent improvements in the low frequency characteristics of soft Magnetic Composite Materials (SMCs) have attracted wide-spread interest in the application of these materials to electrical machines. The SMC material together with the powder metallurgy compaction process offers unique advantages of enabling the formation of complex magnetic structures [6.3]. This is ideally suited to overcome the aforementioned drawback with the manufacture of axial-flux PM machine stator cores. Furthermore, the isotropic magnetic property of SMC materials provides opportunities
for 3-dimensional flux paths in magnetic components, and hence offers new possibilities for machine designers.

A new SMC-based axial-flux PM machine topology was recently presented in [6.4]. A single rotor, double stator axial-flux topology was implemented. Each stator module comprised of individually pressed SMC teeth held into position by a laminated steel stator yoke. Great care was taken to ensure a good joint between the stator teeth and yoke. This was ensured by a stator yoke formed from strip-punched laminations, formed into a circle to grip the teeth. The armature winding consisted of pre-formed concentrated coils, which were tightly wound and post-assembled onto the stator teeth. A high slot-fill and low ohmic losses were achieved with this armature winding configuration. The combined SMC/Steel core resulted in a high torque density. The complex gripping structure between the teeth and yoke in this design presents major difficulties in assembling the stator core and also in maintaining its form.

This chapter extends the concept of the machine topology presented in [6.4] to a direct-drive wind generator for a new turbine with contra-rotating blades. It outlines the design and prototyping of an axial-flux PM wind generator with SMC teeth and a silicon steel stator yoke. A simple press-fit and wedge/retaining ring arrangement is proposed for fixing the SMC teeth into the yoke. The wind generator is sized based on wind speed requirements and the SMC core samples available for this research. A concentrated winding configuration is inserted from the stator yoke side. This facilitates the use of small slot openings, thereby reducing harmonics associated with the slot openings. The SMC core is machined by means of a cost effective and easily accessible process using an end-mill. The detrimental effect of this machining process on the SMC core is discussed and chemical treatment of the smear of iron particles on the machined surfaces is investigated. Microscopic images are shown to illustrate the successful elimination of the smear on the machined surfaces. The support structure of the wind generator was designed with detailed consideration of its intended application with the contra-rotating turbine. Experimental results are provided to validate the performance of the wind generator.
The characteristics of SMC materials are first introduced in section II. The applicability of this material to the design of PM wind generators are then outlined in section III. The machine topology is then introduced in section IV. Aspects relating to its design and performance are then outlined in sections V, VI, VII and VIII. The application of the generator to the new turbine with contra-rotating blades is considered in section VII. Conclusions are drawn in section IX.
6.3 Characteristics of SMC Materials

Soft magnetic composite materials consist of iron powder particles that are coated with an insulating film coating, as illustrated in Fig. 6.1. The iron particles are electrically insulated from each other, which ensures a high electrical resistivity for SMC components. Magnetic structures are formed by compacting the powder into the desired shape. The compaction process introduces stresses within the lattice, which are subsequently relaxed by heat-treating the component at a sufficiently high temperature. The resistivity, mechanical and ferromagnetic properties of the SMC material depend on the size of the iron powder particles, density, insulation coating, compaction process and the heat treatment cycle. The properties of the SMC material can therefore be adapted to suit the requirements of a specific application.

![Iron powder particles with insulating film coating in SMC materials](Fig. 6.1)

The compaction process results in reduced wastage of materials required to produce intricate parts. Furthermore, good dimensional tolerances and smooth surface finishes are obtained with pressed SMC parts. This process also favours the modular construction of components, which is well suitable for mass production.

In machines with laminated silicon steel cores, good magnetic properties are achieved along planar flux paths. This results in a 2-dimensional magnetic circuit, which limits design possibilities. The insulated iron particles in SMC materials result in isotropic magnetic properties. Thus, three-dimensional flux paths are now possible in the design of magnetic components.
The saturation flux density and relative permeability of SMC materials is generally lower than that of silicon steel. The lower permeability can be attributed to the fact that flux in an SMC component has to constantly pass through the non-magnetic insulation between iron particles [6.3]. The lower saturation flux density and relative permeability of SMC materials is clearly illustrated in the comparison of the magnetisation curves of SMC materials and silicon steel shown in Fig. 6.2.

In the application of SMC materials to the design of electrical machines, the high resistivity of SMC materials result in lower eddy losses than with silicon steels. The hysteresis losses are however higher with SMC materials. This is mainly due to poor domain structure at low frequencies [6.5]. A comparison of the total core losses in silicon steel and SMC material is shown in Fig. 6.3, for several operating flux densities at 60Hz.
Fig. 6.2. Comparison of the magnetisation curves of SMC materials and silicon steel.

Fig. 6.3. Comparison of total core losses in silicon steel and SMC material, for several operating flux densities at 60Hz.
6.4 Application of SMC Materials to PM Wind Generator Design

As discussed in the previous section, SMC materials have lower relative permeability and higher core losses at low frequencies than silicon steel. The apparent drawback of higher core losses associated with SMC materials can be offset in direct-drive PM wind generator designs due to the inherently low operating speeds of these machines. Typical operating speeds of small PM wind generators are illustrated in Fig. 6.4 for various power ratings and rated wind speeds. These speeds are achieved at operating frequencies typically between 30 – 80Hz. The core loss at these operating frequencies is less a dominant contributor of the overall losses within a direct-drive PM wind generator, especially when compared to the stator copper losses. Thus, the higher core losses associated with SMC materials can be tolerated in the design of PM wind generators, since these losses represent a small part of the overall losses anyway. Furthermore, the increased core losses are outweighed by other advantages offered by SMC materials.

The apparent drawback of the lower relative permeability of SMC materials is offset in PM wind generator designs by the large effective airgap present due to the PMs mounted on the surface of the rotor. This design is therefore less sensitive to the low permeability of the SMC core due to the inherently high reluctance of the magnetic circuit.

The high number of poles required in small PM wind generators is illustrated in Fig. 6.5 for various power ratings and rated wind speeds. The number of poles is inversely proportional to the minimum stator and rotor yoke thickness required in a machine design in order to avoid saturation of the yokes. Thus, it can be seen that short yoke thicknesses are generally required in PM wind generators, which therefore result in shorter magnetic path lengths. This helps to compensate for the low relative permeability associated with SMC materials.
Chapter 6: Prototyping a composite SMC/Steel axial-flux PM wind generator

Fig. 6.4. Operating speeds for small direct drive WECSs of various powers ratings and rated wind speeds.

Fig. 6.5. Number poles for small direct-drive PM wind generators of various powers ratings and rated wind speeds.
The low relative permeability of SMC materials can be tolerated further by choosing a machine topology which inherently employs shorter magnetic flux paths. An axial-flux PM machine topology would be the most suitable candidate to satisfy this requirement. The rotor or stator yoke can be eliminated completely in axial-flux machine designs which have flux paths shown in Fig. 6.6. Here, the flux travels axially through the rotor or stator structure located in the centre without a yoke (blue), and returns through the yokes of the outer stator or rotor structures (beige).

![Fig. 6.6. Flux paths in axial-flux PM machine](image)

A major drawback with axial-flux machines relates to the manufacture of the steel stator cores. These cores are formed by machining slots into a coiled-up steel lamination strip or by coiling a pre-punched lamination strip. The problems associated with each technique include short circuit paths created between laminations during the slot machining process and the difficulty involved in aligning the pre-punched slots when coiling the lamination strip [6.6]. A pressed SMC core can provide a suitable alternative to the problems outlined above.

Copper losses can be reduced by having the coils preformed in a concentrated winding configuration. This results in phase windings with shorter end connections and hence lower resistances. The resulting electrical loading of a particular design can therefore be increased. Higher slot-fill factors are also achieved with this winding configuration [6.4],[6.6].
6.5 The SMC PM Wind Generator Topology

The topology of the SMC-based axial-flux PM wind generator is illustrated in Fig. 6.7. A circumferential cross-section of the machine is shown in the figure. The axial-flux machine has axially magnetised NdFeB PMs mounted on the surfaces of a mild-steel rotor disc. The rotor disc is mounted between two stator modules. Each stator core consists of tapered SMC teeth fitted into a laminated silicon steel yoke. The stator slots are rectangular in shape and have small (1mm) openings at the airgap. The small slot openings offer the advantage of reducing cogging torques due to slot openings and harmonic torques which are induced by the high frequency ripple in the airgap flux density waveform by the slot openings. The slots are open at yoke-end of the core to allow for easy insertion of the armature winding. A three-phase winding is inserted into the slots before the teeth are fitted into the yoke.

![Fig. 6.7. SMC PM wind generator topology](image)

The isotropic magnetic properties of the SMC material is fully utilised in this design. The flux between adjacent PMs crosses the rotor disc axially, then the airgap, and then travels axially along the SMC teeth. It then curves at the yoke-end of the teeth to enter the steel yoke. The flux travels circumferentially through the yoke, in the plane of the laminations and then returns along a similar path to another PM mounted on the rotor. The flux path is clearly illustrated in Fig. 5.7.
The presence of the steel stator yoke increases the effective permeance of the magnetic circuit, which would be lower if a pure SMC core were used. Previous work highlighted the need for steel in the magnetic circuit due to the low permeability of the SMC material [6.4], [6.7], [6.8].

The SMC stator teeth structure can be manufactured by pressing individual teeth or the entire structure, thereby circumventing the problems associated with slotted strip wound cores used in traditional axial-flux PM machine designs.
6.6 Generator Design

The sizing and main dimensions of the SMC-based axial-flux PM wind generator are determined in this section.

Sizing of the PM wind generator was based on SMC core samples provided by Hoganas and the wind speed requirement for the turbine. The core samples available for this research were cylindrical Somaloy 500 + 0.5% Kenolube cores with inner \((D_i)\) and outer \((D_o)\) diameters of 87.8mm and 122.2mm respectively. A diameter ratio \((k_d = D_o/D_i)\) of 1.39 is achieved and the average airgap diameter \((D_{ave})\) and effective length of a stator core \((L_e)\) is:

\[
D_{ave} = \frac{(D_o + D_i)}{2} = 0.105m
\]
\[
L_e = \frac{(D_o - D_i)}{2} = 0.017m
\]

The rated output power of a double airgap axial-flux PM wind generator can be written in terms of the main dimensions of the machine as [6.2],[6.9]:

\[
P_{gen} = \frac{1}{\varepsilon} \pi^2 \cdot K_{w1} \cdot D_{ave}^2 \cdot I_e \cdot \frac{f}{p} \cdot SML_{pk} \cdot SEL_{pk} \cdot \cos \phi
\]

where \(\varepsilon = E_f/V_e\), is ratio of rated excitation voltage to terminal voltage of the machine, \(K_{w1}\) is the fundamental winding factor, \(n_e\) is the rotational speed in rev/sec, \(SML_{pk}\) and \(SEL_{pk}\) are the peak values of the specific magnetic and electric loadings of the machine.

Based on the rated wind speed \(U\), maximum aerodynamic efficiency \((C_P)\) and optimum tip speed ratio \((\lambda)\) for the wind turbine, the power captured from the incident wind can be expressed as [6.10]:

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\[ P_{\text{shaft}} = \frac{1}{2} \rho_{\text{air}} \pi \frac{\lambda^2}{\omega^5} U^5 C_P \]  
\[ (6-3) \]
where \( \rho_{\text{air}} \) is the density of air at the height of the turbine hub and \( \omega \) is the rated shaft speed of the turbine in rad/sec.

By combining equations (6-2) and (6-3), an expression can be written for the required number of generator pole pairs, in terms of rated turbine and generator parameters. This can be expressed as:

\[ p = 2\pi f \cdot \sqrt{\frac{K_w D_{\text{ave}}^2 L_\ell \cdot SML_{\text{PA}} \cdot SEL_{\text{PA}} \cos \phi}{\varepsilon \eta_{\text{gen}} \rho_{\text{air}} \lambda^2 U^5 C_P}} \]  
\[ (6-4) \]

Once the number of pole pairs of the SMC-based PM wind generator is determined from (6-4), the rated power and shaft speed can be easily determined. For a rated wind speed of 8m/s, \( C_P=0.4 \), \( \lambda=6 \), \( f=50\text{Hz} \), and reasonable magnetic and electric loadings, the calculated machine ratings are: 14 poles, 220W and 428rpm.

The number of stator teeth is chosen to provide a good winding factor, minimise cogging torques and the harmonics in the stator MMF. A further consideration in choosing the number of teeth is the slot-pitch to pole-pitch ratio (\( \tau_s/\tau_p \)). A suitable ratio will ensure that excessive leakage between adjacent PM poles does not occur. The number of teeth selected was 15. This provided a good compromise between the afore-mentioned considerations. Furthermore, a fundamental winding factor (\( K_{w1} \)) of 0.951 could be achieved theoretically with 15 slots (S) and 14 poles (2p). The cogging torque frequency at rated speed for this design is 1.5kHz, thereby ensuring a low cogging torque amplitude.

In axial-flux PM machine designs, parallel-sided slots are used to accommodate the armature winding, but result in tapered stator teeth. The thickness of the SMC teeth was determined in order to avoid saturation of the narrowest portion of the teeth, which is located on the inner diameter of the SMC core. This is ensured by choosing
the width of the stator teeth along the inner diameter \( w_{t,\text{in}} \), such that:

\[
w_{t,\text{in}} > t_{s,\text{in}} \cdot \frac{B_g}{B_{\text{sat,SMC}}}
\]  

(6-5)

where \( t_{s,\text{in}} = \pi D_{\text{in}} / S \) is the stator slot pitch at \( D_{\text{in}} \), \( B_g \) is the plateau value of the airgap flux density and \( B_{\text{sat,SMC}} \) is the saturation flux density of the SMC teeth.

The slot opening width could be made as small as possible, since the windings are inserted from the stator yoke side of the teeth. However, a serious consideration in concentrated winding machine designs, where the slot and pole pitch lengths are comparable, is leakage between adjacent poles through a stator tooth. The stator slot openings should therefore be sufficient to prevent excessive leakage between PMs. A 1mm slot opening provided a good compromise.

The winding configuration suitable for the wind generator under consideration is a concentrated, double-layer winding. This winding configuration provides better utilisation of the copper, due to shorter end-connections, and also provides higher slot-fills, since the coils can be preformed and inserted from the store yoke side. The number of turns per phase \( (N_{ph}) \) for a concentrated, double-layer \( (N_t=2) \) winding can be calculated in terms of the number of turns per tooth \( (N_t) \), as follows [6.11]:

\[
N_{ph} = \frac{1}{3} \cdot \frac{n_t \cdot N_t \cdot S}{2}
\]  

(6-6)

The number of turns per tooth can be calculated from the electric loading of the machine, as:

\[
N_t = \frac{\pi D_{\text{ave}} \cdot SEL_{ph}}{\sqrt{2} I_{\phi} \cdot n_t \cdot S}
\]  

(6-7)

The number of turns per tooth was calculated as 90 for a rated current \( (I_{\phi}) \) of 3.3A and
The coil sides belonging to different coils are arranged such that they are positioned next to, as opposed to above, each other in a slot. This results in an average coil pitch ($t_c$) of less than a slot pitch ($t_s$) or pole pitch ($t_p$), thereby resulting in short-pitched coils. This short-pitched coil arrangement contributes significantly to suppressing the 3rd harmonic in the per-phase excitation voltage waveform of the machine. This is desirable in order to ensure a sinusoidal output voltage waveform for the application of the machine as a wind generator.

The emf phasors of the fundamental harmonic coil voltages (in per-unit) and the resultant phase voltages are shown in Fig. 6.8. A high fundamental harmonic distribution factor ($K_{dh}$) of 0.957 is achieved and can be clearly observed in the figure by comparing the magnitudes of the resultant phase voltages to that of the coil voltages.

The harmonic pitch ($K_{ph}$), distribution ($K_{dh}$) and winding ($K_{wh}$) factors of the SMC wind generator are shown in Table 6.1. It can be seen that the third harmonic is suppressed at the expense of a slightly lower fundamental harmonic winding factor of 0.852.

The design information of the SMC wind generator is summarised in Table 6.2.
Fig. 6.8. Emf phasors of fundamental harmonic coil voltages (in per-unit) and resultant phase voltage

**Table 6.1**

Harmonic Pitch ($K_{ph}$), Distribution ($K_{dh}$) and Winding ($K_{wh}$) Factors of the Stator Winding of the SMC Wind Generator, (where “$h$” is the Harmonic Number)

<table>
<thead>
<tr>
<th>$h$</th>
<th>$K_{ph}$</th>
<th>$K_{dh}$</th>
<th>$K_{wh}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.89094</td>
<td>0.95668</td>
<td>0.85234</td>
</tr>
<tr>
<td>3</td>
<td>0.156</td>
<td>0.64721</td>
<td>0.10096</td>
</tr>
<tr>
<td>5</td>
<td>0.70763</td>
<td>0.2</td>
<td>0.14153</td>
</tr>
<tr>
<td>7</td>
<td>0.98753</td>
<td>0.14945</td>
<td>0.14758</td>
</tr>
<tr>
<td>9</td>
<td>0.45281</td>
<td>0.24721</td>
<td>0.11194</td>
</tr>
</tbody>
</table>
### Table 6.2

**Design Information of SMC Axial-Flux PM Wind Generator**

<table>
<thead>
<tr>
<th>Design parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{\text{gen}}$</td>
<td>Rated power</td>
</tr>
<tr>
<td>$N_r$</td>
<td>Rated speed</td>
</tr>
<tr>
<td>$p$</td>
<td>Number of pole pairs</td>
</tr>
<tr>
<td>$V_a$</td>
<td>Rated phase voltage</td>
</tr>
<tr>
<td>$I_a$</td>
<td>Rated phase current</td>
</tr>
<tr>
<td>$D_{\text{ave}}$</td>
<td>Average airgap/stator diameter</td>
</tr>
<tr>
<td>$L_e$</td>
<td>Effective length of stator core</td>
</tr>
<tr>
<td>$S$</td>
<td>Number of stator slots</td>
</tr>
<tr>
<td>$l_s$</td>
<td>Mechanical airgap clearance</td>
</tr>
<tr>
<td>$SEL_{pk}$</td>
<td>Specific electric loading (peak)</td>
</tr>
<tr>
<td>$J$</td>
<td>Current density</td>
</tr>
<tr>
<td>$G_s$</td>
<td>Gauge of stator conductors</td>
</tr>
<tr>
<td>$N_{ph}$</td>
<td>Number of turns per phase</td>
</tr>
<tr>
<td>$w_s$</td>
<td>Width of a stator slot</td>
</tr>
<tr>
<td>$h_s$</td>
<td>Height of stator slot</td>
</tr>
<tr>
<td>$K_{sf}$</td>
<td>Slot-fill factor</td>
</tr>
<tr>
<td>$\tau_{c}$</td>
<td>Stator coil pitch</td>
</tr>
<tr>
<td>$\tau_{s}$</td>
<td>Stator slot pitch</td>
</tr>
<tr>
<td>$SMC_{pk}$</td>
<td>Specific magnetic loading (peak)</td>
</tr>
<tr>
<td>$l_m$</td>
<td>Axial thickness of PM</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>PM pole-arc to pole-pitch ratio</td>
</tr>
<tr>
<td>$B_r$</td>
<td>Remanent flux density of PMs</td>
</tr>
<tr>
<td>$H_c$</td>
<td>Coercive force of PMs</td>
</tr>
<tr>
<td>$\mu_r$</td>
<td>Relative permeability of PMs</td>
</tr>
</tbody>
</table>
6.7 Generator Analysis

Detailed Finite Element (FE) analyses were performed in order to validate the machine design before prototyping it. Some FE results are presented here. In order to facilitate a 2-D FE analysis of the axial-flux machine topology, a circumferential cross-section of the machine at its average diameter was simulated.

A serious consideration in concentrated winding machine designs, where the slot and pole pitch lengths are comparable, is leakage between adjacent poles through a stator tooth. The flux plots of this machine indicated some leakage of this type, as can be seen in Fig. 6.9. The figure shows an open circuit flux plot and flux density map of 7 poles of the (14 pole, 15 slot) PM wind generator. The two teeth (of each stator) located in the middle of the figure, are severely affected by this leakage. The coils associated with these teeth will therefore be severely under-fluxed, which will result in lower voltages induced in the coils. The flux per pole is 0.203mWb and the peak flux density in a stator tooth is 0.98T. The PM flux linkage with the turns of a coil in an under-fluxed tooth is 2.02mWb-turns compared to 18.9mWb-turns in a fully-fluxed tooth.

The open circuit airgap flux density waveform of the machine is shown in Fig. 6.10. The plateau value of the airgap flux density is 0.85T. The airgap flux density waveform appears trapezoidal and is hardly distorted by a harmonic due to the small (1mm) slot openings.

Several methods were considered for reducing the leakage between poles through the tips of the stator teeth. In one method, bevelled stator teeth were considered in the afore-mentioned 14 pole, 15 slot PM wind generator design. A flux plot and the airgap flux density waveform are shown Fig. 6.11 and Fig. 6.12, respectively. The method provided very little reduction in the leakage between poles, but resulted in significant distortion of the flux density waveform.
In another method, a reduction in the number of poles of the generator from 14 to 12 poles was considered. Here, the slot-pitch to pole-pitch ratio reduced slightly, which consequently reduced the leakage between adjacent poles through the edges of a stator tooth. This is illustrated in Fig. 6.13. The flux per pole increased to 0.274 mWb, thereby resulting in an increase in the peak flux density in a stator tooth to 1.27 T. The PM flux linkage with the turns of a coil in an under-fluxed tooth is 7.13 mWb-turns compared to 24.7 mWb-turns in a fully-fluxed tooth.

The airgap flux density waveform of the 12 pole machine is shown in Fig. 6.14. The plateau value of the airgap flux density remains unchanged. However, a distinct slot harmonic is clearly visible on flux density waveform.

The 12 pole wind generator design was only considered to investigate the leakage between poles. However, it did not present a feasible solution for the prototype, since the required number of poles (14) was based on the wind speed requirements for the SMC PM wind generator. Furthermore, the 12 pole, 15 slot configuration has a low fundamental winding factor of 0.81.
Fig. 6.9. Open circuit flux plot and flux density map of SMC-based PM wind generator with 14 poles and 15 slots.

Fig. 6.10. Normal component of the airgap flux density of SMC-based PM wind generator with 14 poles and 15 slots.
Fig. 6.11. Flux plot and flux density map of 14 pole PM wind generator with 15 slots and bevelled slot openings

Fig. 6.12. Normal component of the airgap flux density for the 14 pole PM wind generator with 15 slots and bevelled slot openings
Fig. 6.13. Flux plot and flux density map of SMC-based PM wind generator with 12 poles and 15 slots

Fig. 6.14. Normal component of the airgap flux density of SMC-based PM wind generator with 12 poles and 15 slots
6.8 Construction of the Prototype

The construction of the SMC-based axial-flux PM wind generator is discussed in this section.

6.8.1 SMC Teeth and Steel Yoke

In [6.7] and [6.8], a hollow cylindrical SMC core was machined to produce a slotted stator core for an axial-flux PM wind generator. A single-stator, double-rotor axial-flux machine topology was implemented. Semi-closed slots were machined along the length of the SMC core and a conventional overlapping winding arrangement was used. A prototype of the machine was constructed and fully tested. The measured full-load efficiency of the prototype was however very low (<50%). This was attributed to the inherently low efficiencies associated with small machines. However, the low efficiency could be attributed to the following reasons:

- The inherently higher core losses associated with SMC material compared to silicon steel
- Use of SMC material only for constructing the stator core
- A change in the micro-structure of the SMC core, due to the machining process. This process causes a breakdown of the insulation between iron regions on the machined surfaces and results in elongated iron regions on the machined surfaces. This smearing effect decreases the surface resistivity significantly, thereby aiding the flow of induced eddy currents. The eddy current losses were therefore excessive on the machined surfaces, hence contributing to the low efficiency.

In prototyping the SMC-based axial-flux PM wind generator presented in this paper, the stator cores should ideally have been pressed. However, this was not financially viable due to the high cost associated with pressing a prototype SMC part. Also, proof
of concept was desired prior to refining the design by means of a pressed SMC stator core. Alternative techniques were therefore considered for producing the slots in the cylindrical SMC core samples available for this research.

The first technique considered was the use of water-jetting. The potential problems associated with this technique include:

- A rough surface finish produced on the cutting edges due to the grinding action of the grit used to produce the cutting action
- Low dimensional tolerances, typically 0.015" 
- Practical difficulty in producing slots on one end of the cylindrical core without damaging the inner surface of the core located diametrically opposite

The water-jetting technique was therefore ruled out as a possible technique for producing the slots in the cylindrical SMC core.

The second technique considered was the Electrostatic Discharge Method (EDM). This technique uses an electrode to electrostatically erode the SMC core into the desired shape. Previous research has shown that the electrical properties of a SMC core are not affected adversely by this process. The EDM technique would therefore be the most suitable choice, based on the afore-mentioned discussion. Slots could be cut into the cylindrical SMC core by means of the wire EDM technique and the steel stator yoke section could be formed by means of a sinker EDM technique. The EDM technique is reported by Haganas (North America) to provide good performance of SMC parts, close to that achieved by pressed SMC parts. Furthermore, good dimensional tolerances can be achieved by this technique.

Another technique considered was machining the slots in the SMC core samples by means of an end-mill. This provided a cost effective and easily accessible solution to prove the concept of the SMC-based AFPM wind generator. The machining process
SMC core that has been machined would involve chemical post-processing. This is addressed in a subsequent section of this chapter.

The machining of a SMC core for the prototype machine, by means of an end-mill is illustrated in Fig. 6.15. The SMC core is rotated through one slot-pitch each time to allow the end-mill to machine subsequent slots. A completely slotted SMC core is shown in Fig. 6.16. The high metallic lustre on the machined surfaces is clearly indicative of the breakdown of the insulation between iron regions on the machined surfaces of the SMC core. The teeth of the slotted core shown in Fig. 6.16 are separated by means of 1 mm slot openings, which are cut by a slotting saw. The cutting of the slot openings by means of a 1 mm slotting saw is shown in Fig. 6.17.

The stator yoke of the prototype wind generator was formed by stacking 29 gauge M19 silicon steel laminations. The M19 steel was fully processed and coated with a S3 coating prior to being laser cut. Slots were laser cut into the laminations for the SMC teeth.
Fig. 6.15. Machining of an SMC core by means of an end-mill

Fig. 6.16. Slotted SMC core of prototype PM wind generator
Fig. 6.17. Cutting of slot openings and separation of teeth by means of a 1 mm slotting saw.
6.8.2 Interface Between SMC Teeth and Steel Yoke

The joints between the SMC teeth and laminated stator yoke are subjected to the axial force exerted by the PMs on the SMC teeth. Furthermore, good electromagnetic contact is required between the two materials in order to ensure minimum reluctance to flux crossing the interface between the two materials. In this design, adequate contact and strength of this joint is ensured by means of a pressed fit and a wedge/retaining ring arrangement.

The mechanical tolerances achieved with laser cutting the laminations and machining of the SMC teeth were sufficient to provide a tight fit between the SMC teeth and laminations. The wedge in the SMC teeth and the groove for the retaining ring which holds the laminations in place can be seen clearly in Fig. 6.16. This joint ensures a low reluctance path to flux and also simplifies the assembly of the machine by eliminating the need for a complicated clamping arrangement as in [6.4].

An assembled stator core with the SMC teeth and steel yoke interface is shown in Fig. 6.18. The SMC teeth protrude through the steel yoke and are locked into place with a stainless steel spiral retaining ring as shown in the figure.

Fig. 6.18. Assemble stator core showing the SMC teeth and steel yoke interface.
6.8.3 Stator Core Assembly

The SMC wind generator has two stator cores, each consisting of a silicon steel yoke, 15 SMC teeth and 15 concentrated coils. The coils are hand-wound with 90 turns of 20-AWG magnet wire. The stator cores are assembled by first positioning the coils on each SMC tooth and then inserting the laminations from the yoke side of the core. The structures are then secured by means spiral retaining rings. A partially assembled stator core is shown in Fig. 6.19. The concentrated double layer winding configuration can be clearly seen in the figure.

![Fig. 6.19. Partially assembled stator core of SMC wind generator](image-url)
6.8.4 Rotor Core

The rotor core of the SMC wind generator consists of 14 NdFeB permanent magnets and a low carbon steel (1018) disc. The magnets are grade N28 sintered NdFeB PMs and are sector-shaped with dimensions: R61.1mm x R43.9 mm x H4.7 mm x 16.25°. The PMs are fixed onto both sides of the steel disc by means of a high impact epoxy adhesive from Loctite (E-20HP). The steel rotor disc is inserted for mechanical rigidity only, since the flux path between adjacent PMs crosses the rotor disc axially. It can therefore be of minimum thickness required for this purpose. The rotor core and an assembled stator core are shown in Fig. 6.20.

Fig. 6.20. Rotor core and assembled stator core of SMC wind generator
6.8.5 The Contra-rotating Generator Support Structure

The support structure of the SMC wind generator was designed for operation with a new contra-rotating wind turbine [6.12]. This proposed turbine has two sets of rotor blades, one coupled directly to the rotor shaft of the wind generator and the other coupled to the stator shaft. A schematic diagram of the proposed turbine is illustrated in Fig. 6.21.

The front-end blades capture power from the incident wind as with a conventional turbine. The back-end blades rotate in an opposite direction to the front-blades and extract power from the wake of the front-end blades. A turbine can extract a maximum of 59% of the aerodynamic power available in the wind. The back-end blades are intended to capture some of the power not captured by the front-end blades. If the aerodynamic efficiency of the front-end blades are 35 – 45% and the back-end blades capture at least 15% of the power from the wake of the front-end blades, then there can be a net increase in the aerodynamic efficiency of this turbine of 5.25 – 6.75%. Of course, there are issues relating to the cost of the additional blades and the increased strength of the tower for this new turbine. However, small wind turbines are less sensitive to these issues compared to utility-scale turbines. Research is currently being conducted by an Aeronautical Engineering group at Clarkson University, to investigate the exact configuration of the new turbine. In particular, the research is focused on the relative sizing of front and back-end blades, the axial distance between blade sets, the optimum blade number and blade profile for this new turbine.

The SMC wind generator presented in this chapter forms part of a collaborative effort to develop a new contra-rotating wind energy conversion system. The support structure of the SMC wind generator was therefore designed to allow simultaneous rotation of the rotor and stator in opposite directions. A brush and slip-ring structure is intended to enable the extraction of power from the rotating stator.

A schematic diagram of the generator support structure is shown in Fig. 6.22. One of the stator cores are supported on a bearing located on the rotor shaft. The other stator core and its shaft are supported on a double bearing structure, which is bolted to the test-bed. The rotor disc and shaft is supported on a similar double bearing structure,
which is also bolted to the test-bed. The double bearing structures are intended to support the weight of the rotor and stator cores and thereby to ensure axial alignment of the rotor and stator shafts. The two stator cores are coupled by means of axial threaded rods.

Fig. 6.21. Schematic diagram of proposed contra-rotating wind turbine
Fig. 6.22. Schematic diagram of SMC wind generator support structure
A detailed view of the contra-rotating support structure is shown in Fig. 6.23. Only mechanical parts are shown in the figure and the white discs represent the stator laminations of the actual machine. The rotor disc is shown and its shaft extends to the left of the figure. The stator shaft extends to the right of the figure.

The completely assembled prototype SMC wind generator with its contra-rotating support structure is shown in Fig. 6.24 and Fig. 6.25.

Since the prototype SMC wind generator was tested in a laboratory, the brush and slip-ring structure was not assembled. Furthermore, the machine was tested with the stator cores held stationary. Increased friction and windage losses can be expected if both the rotor and stator structures rotate in opposite directions. However, in operating the generator with a contra-rotating turbine, the rated relative speed between rotor and stator will be the same as the rated shaft speed at which the generator was tested. The friction & windage losses may therefore only increase slightly. This will be assessed and disseminated in future publications.

Fig. 6.23. Contra-rotating support structure for SMC wind generator
Fig. 6.24. Completely assembled prototype SMC wind generator

Fig. 6.25. Detailed view of completely assembled prototype SMC wind generator and contra-rotating support structure
6.8.6 Acid Treatment of SMC Core

The insulated iron particle regions are clearly visible from the microscopic image of the surface of a pressed and heat-treated SMC core. This is shown in Fig. 6.26. In contrast, the breakdown of the insulation between iron regions on the machined surfaces of an SMC core is clearly visible in Fig. 6.27. The elongated iron regions are apparent in this figure.

A chemical process was investigated for treating the smear of iron particles on the machined surfaces. The smeared iron particles over the surface were chemically dissolved in a 50% phosphoric acid solution. The acid reacts with iron and not with the insulation material, thus etching the smeared iron on the machined surfaces exposing the insulation coating between the iron regions as would be expected. The unmachined surfaces of the SMC core are coated with a layer of petroleum jelly before submerging the core into the acid. This is done in order to avoid a reaction between the iron on these surfaces with the acid. The etched surface of an SMC core is illustrated in Fig. 6.28. It can be seen that most of the iron particles are removed from the surface, which results in a rough surface finish.

A serious consequence of treating an SMC core with phosphoric acid is that corrosion sets in almost immediately on the surfaces which are exposed to the acid. In order to stop further reaction on the machined surfaces, the core is washed under running water to remove the phosphoric acid, and then ultrasonically cleaned immediately in methanol.

These preliminary tests indicate the successful removal of the smear of the iron particles on the machined surfaces of the SMC cores. However, the prototype SMC wind generator was assembled with chemically untreated SMC teeth. Future work will include the assembly of a prototype with chemically treated teeth. The improvement provided by the chemical treatment process will be assessed and disseminated in future publications.
Fig. 6.29. Voltages induced in two coils located on adjacent SMC teeth

Fig. 6.30. Line to neutral voltages of phases A and C
The variation of the measured open circuit phase voltage of the wind generator with shaft speed is shown in Fig. 6.32. The PM flux linkage \( \lambda_{PM} \) is estimated from the slope of the best-fit line to the experimental data, shown in the figure. The value obtained (0.116 \( \text{Wb-turns} \)) compares favourably to the PM flux linkage estimated from the analytical design procedure (0.124 \( \text{Wb-turns} \)), for an airgap clearance of 1.75 mm.

![Fig. 6.31. Line to line voltages of SMC wind generator: \( V_{AC} \) and \( V_{CB} \)](image)

![Fig. 6.32. Open circuit (rms) phase voltage versus shaft speed](image)
6.9.2 Rotational Losses

The variation of the rotational losses of the SMC wind generator with shaft speed is shown in Fig. 6.33. These losses were measured under no-load conditions and include the friction & windage losses and core losses in the machine. Core losses are present in a surface PM machine even at no-load due to the high rate of change of flux density in the teeth and yoke caused by the edges of the PMs [6.13],[6.14].

The separation of friction & windage losses ($P_{fw}$) and core losses from the measured rotational losses is difficult. However, the friction & windage losses were estimated using empirical formulae and subsequently deducted from the rotational losses in Fig. 6.33 to give an estimate of the core losses at no-load in the SMC wind generator [6.2],[6.9].

The variation of core losses with load current can be estimated on the basis that core losses can be approximated by the product of a frequency dependent function $K_c(f)$ and the net stator flux linkage ($\lambda_s$). This can be expressed as [6.15]:

$$P_{core}(f, B) = k_h B_{max}^n + k_e f^2 B_{max}^2 = K_c(f) \lambda_s^2$$

where $k_h$ and $n$ are hysteresis loss constants and $k_e$ is the eddy current loss constant.

The stator flux linkage ($\lambda_s$) can be expressed in terms of its $d$ and $q$-axis components ($\lambda_d$ and $\lambda_q$), and the $d$ and $q$-axis components of generator current ($i_d$ and $i_q$) as:

$$\lambda_s^2 = \lambda_d^2 + \lambda_q^2$$
$$\lambda_d = \lambda_{PM} - L_s i_d$$
$$\lambda_q = -L_s i_q$$

where $\lambda_{PM}$ is the PM flux linkage and $L_s$ is the synchronous inductance.

The frequency dependent function $K_c(f)$ can be estimated from the core losses at no-load by realising that $\lambda_s^2 = \lambda_{PM}^2$ under no-load conditions. Thus,
\[ K_c(f) \approx \left[ \frac{P_{core}(f, B)}{B_{PM}^2} \right]_{no\,load} \]  

\[ (6-11) \]

The variation of \( K_c(f) \) with shaft speed is shown in Fig. 6.34. The core losses of the SMC wind generator under load conditions can be estimated with the aid of (6-9), by using the measured shaft speed and hence \( K_c(f) \) from Fig. 6.34 and the estimated stator flux from (6-10).

![Fig. 6.33. Rotational losses of the SMC wind generator versus shaft speed](image)

![Fig. 6.34. Frequency dependent function \( K_c(f) \) for estimating core losses under load](image)
6.9.3 Terminal Voltage Characteristic

The SMC wind generator was loaded with a resistive load and its phase voltage \( V_a \) and current \( I_a \) was measured. The terminal voltage characteristic of the wind generator obtained in this manner is illustrated in Fig. 6.35, for operation at rated speed (428rpm). It can be seen that the terminal voltage drops as the load resistance decreases and hence the load current increases. This is due to the volt drop across the internal impedance of the machine. The difficulties with maintaining the airgap clearance at 1mm, but instead at 1.75mm, resulted in a lower open circuit voltage and hence a lower terminal voltage characteristic than expected. The rated load current of 3.3A could therefore not be delivered to a resistive load at a terminal voltage of 24V.

The per-phase equivalent circuit of a synchronous generator can be used to write an expression for its terminal voltage characteristic under unity power factor operation. This can be expressed as:

\[
V_a = \sqrt{E_j^2 - I_a^2 X_s^2 - I_a R_a} \tag{6-12}
\]

where \( E_j \) is the open circuit voltage, \( X_s \) and \( R_a \) are the synchronous reactance and resistance per phase.

By fitting a curve of the form of (6-12) to the terminal voltage characteristic data in Fig. 6.35, the parameters of the wind generator can be easily determined. The best-fit curve is shown in the figure. The phase winding resistance and synchronous reactance determined in this manner are: \( R_a = 3.18 \Omega \) and \( X_s = 14.07 \Omega \). Thus, the synchronous inductance of the wind generator is: \( L_e = 44.8 \text{mH} \).

With the aid of a general expression for the terminal voltage characteristic of a synchronous generator, the characteristic of the SMC wind generator can be predicted at various load power factors. This is illustrated in Fig. 6.36 for lagging, unity and leading power factors at rated speed. The experimental data and predicted characteristic for unity power factor operation are shown in the figure. It can be seen that the terminal voltage will be boosted by the reactive power supplied to the machine under leading power factor operation.
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Fig. 6.35. Terminal voltage characteristic of SMC wind generator at rated speed

Fig. 6.36. Predicted terminal voltage characteristic of SMC wind generator at various power factors and rated speed (428rpm)
6.9.4 Generator dq-model for Unity Power Factor Operation

The $d$ and $q$-axis voltages ($v_d$ and $v_q$) of a PM synchronous generator with surface mounted magnets can be written as [6.15],[6.16]:

\[
\begin{align*}
    v_d &= -i_d r_s + \omega L_s i_q \\
    v_q &= -i_q r_s + \omega L_s i_d - \omega L_s i_d
\end{align*}
\]  

(6-13)

where $i_d$ and $i_q$ are the $d$ and $q$-axis currents, $\omega$ is the electrical angular frequency in [rad/sec] and $r_s$ is the stator winding resistance per-phase ($r_s = R_a$).

The real and reactive powers at the terminals of the generator can be expressed as:

\[
\begin{align*}
    P &= \frac{3}{2} [v_d i_d + v_q i_q] \\
    Q &= \frac{3}{2} [v_d i_d - v_q i_q]
\end{align*}
\]  

(6-14)

Under unity power factor operation, $Q = 0$ and $v_q i_d = v_d i_q$. Thus, (6-13) can be used to write expressions for $i_d$ and $i_q$ under unity power factor operation. These can be expressed as:

\[
\begin{align*}
    i_d &= \frac{I_s}{\lambda_{max}} \cdot \frac{2 \tilde{f}_d^2}{t_d} \\
    i_q &= \sqrt{\frac{\lambda_{max} t_d - t_d^2}{t_s}}
\end{align*}
\]  

(6-15)

The $d$ and $q$-axis current trajectories are illustrated in Fig. 6.37 for the resistive load test at rated speed. It can be seen that a demagnetising component of $i_d$ increases with load, thereby reducing the net stator flux ($\lambda_s$) and hence the stator voltage. Also, the torque component of stator current ($i_q$) increases to a maximum value ($i_{q_{max}}$) for unity power factor operation and then decreases. From (6-15), $i_{q_{max}}$ occurs under the following condition:
\[ I_d = I_{d_{\text{max}}} = \frac{1}{2} \frac{\dot{\lambda}_{\text{PM}}}{I_s} = 1.29 A \]  \hspace{1cm} (6.16)

Fig. 6.37. Trajectories of \( d \) and \( q \)-axis currents for resistive load test at rated speed
6.9.5 Power

The input shaft power ($P_{shaft}$) and output load power ($P_{load}$) of the SMC wind generator was measured for the resistive load test at rated speed. This is illustrated in Fig. 6.38. The maximum load power achieved was 57.4W. This was primarily due to the drop in terminal voltage as the load current increased. This was caused by difficulties with the mechanical support structure of the machine not being able to maintain the airgap clearance at 1mm, and also the increase in the demagnetising component of stator current ($i_d$) with load (which in turn reduces the net stator flux ($\lambda_s$) and hence the stator voltage). The rated load current, voltage and hence load power of 220W could therefore not be delivered to a resistive load.

The $d$ and $q$-axis current components of Fig. 6.37 can be used to estimate the copper losses of the SMC wind generator with a resistive load. This can be expressed as:

$$P_{Cu-da} = \frac{3}{2} (i_d^2 + i_q^2) R_s = \frac{3}{2} \frac{\lambda_{em}}{L_s} i_d i_q$$  \hspace{1cm} (6-17)

The estimated copper losses of the wind generator are shown in Fig. 6.38.

The developed (electromagnetic) power of the wind generator can be estimated as:

$$P_{em} = \frac{3}{2} \omega_{em} i_d i_q$$ \hspace{1cm} (6-18)

This is also illustrated in Fig. 6.38.

The core losses of the wind generator under load conditions can be estimated with the aid of (6-9), by using the measured shaft speed (428rpm) and hence $K_{eff}$ from Fig. 6.34, and the estimated stator flux from (6-10). The stator flux can be estimated easily with the aid of the $d$ and $q$-axis current components of Fig. 6.37. The estimated core losses ($P_{core-dq}$) of the SMC wind generator are shown in Fig. 6.38. It can be seen that the core losses decrease with an increase in the resistive load. This is primarily due to a decrease in the net stator flux as the resistive load increases.
The load power \( P_{\text{load}-dq} \) can be estimated from (6-14), (6-13) and the \( d \) and \( q \)-axis current components of Fig. 6.37. This is also shown in Fig. 6.38.

Finally, the input shaft power \( P_{\text{shaft}-dq} \) can be estimated from the estimated load power and the sum of the estimated losses. This can be expressed as:

\[
P_{\text{shaft}-dq} = P_{\text{load}-dq} + P_{\text{cv}-dq} + P_{\text{core}-dq} + P_{fW} = P_{e-dq} + P_{\text{core}-dq} + P_{fW}
\]  

(6-19)

where \( P_{fW} \) is the friction & windage losses at rated speed.

The estimated shaft power is shown in Fig. 6.38 and agrees favourably with the measured input power for the resistive load test at rated speed.

Fig. 6.38. Power of SMC wind generator for the resistive load test at rated speed
6.9.6 Torque

The shaft torque of the SMC wind generator was measured for the resistive load test at rated speed. This is illustrated in Fig. 6.39.

The electromagnetic torque developed by the machine can be estimated from the expression for the torque developed by a PM synchronous machine:

\[
T_{e-dq} = \frac{3}{2} p \hat{\lambda}_{ph} l_q
\]  

(6-20)

The estimated torque developed by the wind generator \(T_{e-dq}\) is shown in Fig. 6.39.

The maximum torque developed with the resistive load can be estimated with the aid of (6-16) and (6-20). This can be expressed as:

\[
T_{e-dq,\text{max}} = \frac{3}{2} p \hat{\lambda}_{ph} l_{q,\text{max}} = \frac{3}{4} p L_s \hat{\lambda}_{ph}^2
\]  

(6-21)

The maximum torque developed is: 1.57Nm.

The torque required to overcome the core losses in the machine \(T_{\text{core-dq}}\) is estimated from \(P_{\text{core-dq}}\) and is shown in Fig. 6.39. Similarly, the friction & windage loss torque is determined from \(P_{\text{fr-w}}\). Finally, the shaft torque is estimated from the sum of the developed torque, core loss and friction & windage loss torques. The estimated shaft torque is shown in Fig. 6.39 and agrees favourably with the measured shaft torque for the resistive load test at rated speed.
Fig. 6.39. Torque of SMC wind generator for the resistive load test at rated speed
6.9.7 Efficiency

The efficiency of the SMC wind generator was measured for the resistive load test at rated speed. The variation of generator efficiency with load current is shown in Fig. 6.40. The highest efficiency achieved was 72%. This could be considered as reasonable performance for a small machine, even though an SMC core was used, which has inherently higher losses compared to steel. The estimated efficiency from the estimated load and shaft powers ($P_{\text{load-dq}}$ and $P_{\text{shaft-dq}}$) is also shown in Fig. 6.40. It can be seen that the estimated and measured efficiencies agree favourably.

Fig. 6.40. Variation of efficiency with load current for a resistive load
6.10 Conclusions

This chapter outlined a procedure for sizing an SMC-based axial-flux PM wind generator based on wind speed requirements and SMC core samples available for the research. Issues relating to the number of teeth, poles and concentrated winding design were analysed. The support structure of the wind generator was designed for a new turbine with contra-rotating blades. A chemical processing technique was investigated for treating the smear of iron particles on the machines SMC surfaces. Preliminary tests indicated the successful removal of the smear of iron particles from the machined surfaces of the SMC cores. A prototype was assembled and tested. The output voltage waveform was sinusoidal and contained very little harmonic distortion. The maximum load power achieved was lower than anticipated. This was primarily due to the drop in terminal voltage as the load current increased. This was caused by difficulties with the mechanical support structure of the machine not being able to maintain the airgap clearance at its design value, and also the increase in the demagnetising component of stator current with load. The highest efficiency achieved was reasonable for a small machine with an SMC core.
6.11 References


Chapter 7

Conclusions and Recommendations

7.1 Conclusions

In this thesis, contributions are made to the design of small PM wind generators, which includes the application of SMC materials to the design of these machines. Based on the findings of this thesis, the following conclusions can be drawn:

7.1.1 An analytical model of a radial-flux PM wind generator was formulated. The model relates the mechanical design specifications of the machine to its electrical equivalent circuit parameters and performance. Experimental validation of the model was provided by considering a 3.5kW, 8 pole, three-phase PM synchronous machine.

7.1.2 The design space that exists between multi-blade, high-solidity wind turbines and modern high speed 2 and 3-bladed HAWTs was explored.
Furthermore, the performance of a small 12-bladed, high solidity HAWT to that of a modern 3-bladed design was compared. A procedure was outlined for adapting a small PM wind generator, intended for high speed operation with a 3-bladed HAWT, for low speed operation with a 12-bladed, high solidity HAWT. The redesigned machine was shown to be capable of delivering rated power at the reduced speed required by the multi-blade, high solidity HAWT, whilst operating at a good efficiency. The overall system performance of the 12-bladed, high solidity HAWT coupled to the redesigned wind generator was satisfactory.

7.1.3 An alternative mode of operation of a variable speed wind turbine was considered. In particular, operation of a turbine was considered at a tip speed ratio which ensures that maximum shaft torque is captured as opposed to maximum power. The performance of a small PM wind generator operated under these conditions was evaluated. Operation of the turbine in region I resulted in increased shaft torque throughout the wind/shaft speed range when compared to its operation in region I. The increased driving torque is thus available at the shaft of the PM generator to overcome the parasitic torques inherently present in a PM machine. Furthermore, the electrical energy captured by the WECS when operated in region I increases with the average wind speed of the area in which it is located. Operation in region I also results in increased efficiency of the WECS in low wind/shaft speed conditions. These benefits are however obtained at the expense of a reduction in the output electrical power and hence the total energy captured by the WECS.

7.1.4 A design procedure was outlined for a PM wind generator, which optimises its performance over a wide operating wind speed range. An optimum permutation of generator design variables was selected by an optimisation routine, which minimises the mass of active materials, maximises the efficiency the machine at rated wind speed and also ensures rated power delivery at rated wind speed. The optimisation routine evaluates trial wind
generator designs under operation with a turbine and uses this to guide the
search for an optimised wind generator design. An optimised design of a
1kW PM wind generator was produced for a rated wind speed of 12m/s. A
reasonably high efficiency of 92.37% was achieved with a relatively flat
efficiency curve over a wide operating wind speed range. This is in contrast
to a commercial system that was investigated, which was characterised by a
rather low efficiency at rated wind speed and also by a larger drop in
efficiency over a wide range of wind speeds. The efficiency of the
optimised design drops by 0.58% of its maximum during approximately
half of its operating time in region I. Furthermore, its efficiency drops by
2.49% of its maximum during 71.6% of its operating time in region I. This
characteristic improves the energy capture of the generator during
fluctuations around the rated wind speed.

A procedure was outlined for sizing an SMC-based axial-flux PM wind
generator based on wind speed requirements and SMC core samples
available for the research. Issues relating to the number of teeth, poles and
concentrated winding design were analysed. The support structure of the
wind generator was designed for a new turbine with contra-rotating blades.
A chemical processing technique was investigated for treating the smear of
iron particles on the machines SMC surfaces. Preliminary tests indicated the
successful removal of the smear of iron particles from the machined
surfaces of the SMC cores. A prototype was assembled and tested. The
output voltage waveform was sinusoidal and contained very little harmonic
distortion. The maximum load power achieved was lower than anticipated.
This was primarily due to the drop in terminal voltage as the load current
increased. This was caused by difficulties with the mechanical support
structure of the machine not being able to maintain the airgap clearance at
its design value, and also the increase in the demagnetising component of
stator current with load. The highest efficiency achieved was reasonable for
a small machine with an SMC core.
7.2 Recommendations

Based on the findings of this thesis, the following recommendations can be made:

7.2.1 The procedure for redesigning the PM wind generator for the multi-blade, high solidity wind turbine demonstrated the possibility of modifying the machine design in order to adapt it to the new application. However, an optimisation algorithm could be used to search for a better redesign of the generator for the multi-blade, high solidity wind turbine.

7.2.2 The optimisation of the PM wind generator design can be extended to a multi-objective optimisation problem. Here, a pareto-based multi-objective design routine can be used to search for an optimum design.

7.2.3 The design of small PM wind generators for operation with a rectifier load should be investigated. Small PM wind generators are often connected directly to a 3-phase rectifier, which is then connected to a DC-DC converter. This research could investigate the optimum design of these machines for operation with the harmonic currents injected by a rectifier load. This research could consider minimising the increased core losses due to the presence of these harmonic currents. It could also be extended to investigate the optimum voltage waveform for these machines.

7.2.4 The effect of eliminating the smear on machined SMC surfaces by means of the chemical processing technique should be assessed by assembling and testing a machine with chemically treated SMC material.

7.2.5 Further detailed consideration should be given to the support structure of the SMC-based wind generator. Better mechanical design of these components will assist with maintaining the airgap clearance at the design value, which will improve the performance of the machine.
7.2.6 The performance of the SMC-based wind generator should be fully assessed when operating with the contra-rotating wind turbine. The additional losses due to the slip-ring/brush assembly should be assessed. The effect of different torques from the front and back-end turbine blade sets acting on the rotor and stator shafts needs to be assessed. Mechanical vibration and alignment issues arising from the contra-rotating blade sets need to be fully assessed.
Appendix A

Author's publications

A.1 Conferences


Appendix A: Author's publications

A.2 Journals


Fig. 6.26. Microscopic image of a pressed and heat-treated SMC core

Fig. 6.27. Microscopic image of a machined surface of an SMC core

Fig. 6.28. Microscopic image of an acid treated SMC core surface
6.9 Performance of the SMC PM Wind Generator

The SMC wind generator prototype was fully tested in a laboratory. Experimental results are presented in this section. In assembling the prototype, difficulties were experienced with maintaining the airgap clearance at its design value of 1\text{mm}. This was due to a weakness in the mechanical support structure of the machine, which could not withstand the high attractive forces between the rotor disk and stator cores at an airgap clearance of 1\text{mm}. The actual airgap was therefore set to 1.75\text{mm}. This resulted in lower open circuit voltages than expected. The performance of the machine was assessed under these conditions.

6.9.1 Open Circuit Voltage

The voltages induced in two coils located on adjacent SMC teeth of the same stator core are shown in Fig. 6.29. The coil voltages are displaced by 168°\text{electrical} from each other and are somewhat distorted by harmonics present in the airgap flux density waveform.

The phase windings of the two stator cores were connected in series to form the phases of the SMC wind generator. The open circuit voltages of phases A and C of the wind generator are shown in Fig. 6.30. The low harmonic content is apparent from the sinusoidal shape of the waveforms. This is primarily due to the coil connections and hence the low harmonic winding factors associated with the phase windings. Furthermore, it can be seen that phase C voltage leads phase A by 120°.

The open circuit line to line voltages ($V_{AC}$ and $V_{CB}$) of the wind generator are shown in Fig. 6.31. With the third harmonic eliminated in the line voltages, it can be seen that the waveforms are more sinusoidal. This is desirable in the application of the machine as a wind generator.