A Generator Perspective on Vertical Axis Wind Turbines

FREDRIK BÜLOW
Abstract


The wind energy conversion system considered in this thesis is based on a vertical axis wind turbine with a cable wound direct drive PM generator. Diode rectifiers are used to connect several such units to a single DC-bus and a single inverter controls the power flow from the DC-bus to a utility grid. This work considers the described system from a generator perspective i.e. the turbine is primarily seen as a torque and the inverter is seen as a controlled load.

A 12 kW VAWT prototype with a single turbine has been constructed within the project. The power coefficient of this turbine has been measured when the turbine is operated at various tip speed ratios. This measurement determines both how much energy the turbine can convert in a given wind and at what speed the turbine should be operated in order to maximise the energy capture. The turbine torque variation during the revolution of the turbine has also been studied.

A PM generator prototype has been constructed in order to study power loss in the stator core at low electrical frequencies. Heat exchange between the stator and the air-gap between the stator and the rotor has been studied. Heat exchange between the stator and the air-gap is increased by turbulence caused by the rotor. The generator was also used in a demonstration of a DC-grid where two diode rectified PM generators supplied power to a single DC load. An initial study of an inverter suitable for grid connection of the 12 kW PM generator has been performed.

Several turbine control strategies are evaluated in simulations. The control strategies only require the parameter "turbine speed" to determine the optimal system load.

Keywords: VAWT, PM generator, Wind power, Stator core loss

Fredrik Bülow, Uppsala University, Department of Engineering Sciences, Electricity, Box 534, SE-751 21 Uppsala, Sweden.

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Till Karin Flemström, min Mormor.
List of Papers

This thesis is based on the following papers, which are referred to in the text by their respective Roman numerals.


*European Wind Energy Conference & Exhibition, April, Warsaw.*


*Conditionally accepted for publication in in Renewable Energy.*

*Submitted to Renewable Energy.*

*Submitted to Renewable Energy.*

*EWEA Annual Event, April, Copenhagen.*

Reprints were made with permission from the publishers.
The author has also contributed to the following papers, not included in the thesis:


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<td>Mass density of air</td>
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<tr>
<td>$\rho_c$</td>
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<td>$\rho_f$</td>
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<td>$\omega$</td>
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<td>$\omega_g$</td>
<td>rad/s</td>
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<td>$\omega_t$</td>
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<td>Turbine angular velocity</td>
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<td>$\tau$</td>
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<td>Torque</td>
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<td>$\tau_c$</td>
<td>Nm</td>
<td>Torque due to core loss</td>
</tr>
<tr>
<td>$\tau_g$</td>
<td>Nm</td>
<td>Generator torque</td>
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<td>$\tau_t$</td>
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</tr>
<tr>
<td>AC</td>
<td>Alternating current</td>
<td></td>
</tr>
<tr>
<td>DC</td>
<td>Direct current</td>
<td></td>
</tr>
<tr>
<td>DD</td>
<td>Direct drive</td>
<td></td>
</tr>
<tr>
<td>HAWT</td>
<td>Horizontal axis wind turbine</td>
<td></td>
</tr>
<tr>
<td>IGBT</td>
<td>Insulated-gate bipolar transistor</td>
<td></td>
</tr>
<tr>
<td>PM</td>
<td>Permanent magnet</td>
<td></td>
</tr>
<tr>
<td>PWM</td>
<td>Pulse-width modulation</td>
<td></td>
</tr>
<tr>
<td>RBS</td>
<td>Radio base station</td>
<td></td>
</tr>
<tr>
<td>RMS</td>
<td>Root mean square</td>
<td></td>
</tr>
<tr>
<td>VAWT</td>
<td>Vertical axis wind turbine</td>
<td></td>
</tr>
<tr>
<td>WEC</td>
<td>Wind energy converter (except in Paper I where it stands for wave energy converter)</td>
<td></td>
</tr>
<tr>
<td>emf</td>
<td>Electromotive force</td>
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1. Introduction

The purpose of modern large scale wind energy converters (WECs) is to convert the kinetic energy of natural wind into electric energy and supply it to an electrical grid. The wind turbine is the part of the energy conversion system that physically interacts with the wind and extracts its kinetic energy. The torque and rotary motion of the turbine is converted into electric energy in a generator. Today, the vast majority of large wind turbines are three bladed with a horizontal axis of rotation. Many WECs include a gearbox to reduce the required generator torque. This thesis concerns a wind energy conversion system based on a vertical axis wind turbine (VAWT), with no gearbox and with a relatively simple grid connection strategy.

1.1 Project motivation

It has long been known that power can be extracted from the wind. Recent research in wind power is motivated by society's urgent need for clean, renewable and cost-effective energy rather than just scientific curiosity.

Today, several corporations are providing wind turbines in the multi-megawatt range, some over 7MW [1]. In total, over 280GW of wind power has been installed globally and 44.7GW was installed during 2012 alone [2]. Fierce competition between wind turbine producers is likely to gradually develop the horizontal axis wind turbine (HAWT) technology to its full potential. However, commercial wind turbine manufacturers focus almost exclusively on the HAWT. There are reasons to believe that large scale VAWTs can become more economical than large scale HAWT [3]. Research projects focused on the VAWT are important to assure that the technology is not overlooked just because it differs from the currently prevalent evolutionary branch of wind turbines. The particular WEC design studied here is outlined in chapter 3.

1.2 Background

Wind power was one of the first energy sources to be harnessed by man. Evidence suggests that sails were used on boats as early as 5000 BC [4]. The first known reference to a windmill is found in Hero of Alexandria dating from
somewhere between 100 BC and 100 AD, it is however unclear if the device described there actually ever existed [5]. There are historical records of vertical axis wind turbines (VAWTs) being used in Iran as early as 644 AD [5]. The earliest VAWTs were based on the principle of aerodynamic drag. The power absorbed was used to perform mechanical tasks such as pumping water or grinding. Drag-based VAWTs are usually referred to as Savonius turbines after J. Savonius who patented such a device [6]. Lift-based VAWTs are referred to as Darrieus-type turbines after their inventor G. J. M. Darrieus [7].

Several VAWT research programs were carried out during the 1970s and 1980s. Sandia National Laboratories investigated the VAWT concept for 12 years constructing several prototypes, the largest being 34 m in diameter [8, 9]. The largest VAWT that has ever been built was the Canadian Éole, rated at 4.2 MW [10].

The HAWT probably originates from the Dutch windmills. C. Brush was one of the first to connect a generator to a turbine with the intention of converting the wind’s kinetic energy into electric energy [5, 11]. During the following 125 years, new materials and a substantial engineering effort have gradually scaled up that initial concept to machines with a power rating above 7 MW, with even larger machines being considered [1, 12].

Global energy consumption has increased considerably since the dawn of industrialisation [13]. As a result, today’s modern society is highly dependent on affordable energy. A significant part of this energy is obtained from fossil fuels. The use of fossil fuels have many negative environmental impacts and global reserves are limited. The global oil production appears to be declining which suggests that peak oil has already occurred [14, 15]. Wind power has very low emissions of green house gases and other pollutants [16]. Unlike fossil fuels, wind power is renewable and is currently the second most cost-effective renewable energy source, with hydroelectric power ranking first [17]. The global technical potential of wind power is estimated to be 96 PWh per year: about 6 to 7 times the world’s total electricity consumption in 2001 [18]. The cost of energy and the level of our technology determine how much of the wind resource that will be utilised.
2. Theory

Wind power is an interdisciplinary subject that includes several areas of engineering. This chapter presents fundamental relations that are relevant for the presented papers.

2.1 Torque and power

Most wind turbines convert the kinetic energy of the wind into mechanical energy in the form of torque applied to a rotating shaft. The power required to assert the torque $\tau$ on an axis rotating with the angular velocity $\omega$ is

$$ P = \tau \omega. \quad (2.1) $$

When a net torque is applied to a body with moment of inertia, $J$, it accelerates according to

$$ \frac{d\omega}{dt} = \frac{\tau}{J}. \quad (2.2) $$

Consequently the power required to maintain the acceleration is

$$ P = J \omega \frac{d\omega}{dt}. \quad (2.3) $$

In Paper IV, equation (2.3) was used to estimate no-load power loss through observation of rotor deceleration. The control strategies presented in Papers V and VI all rely on a mismatch between rotor and generator power when the rotor speed is above or below optimal speed.

A VAWT design option is to extend the shaft between the turbine and the generator so that the generator can be located at ground level. However, torsional vibrations of the turbine and rotor can be an issue. Slow torsional vibrations are well described by the simplified model illustrated in Fig. 2.1. The torque mediated by the shaft is proportional to the twist angle or angle difference between the turbine and the rotor. The motion of the two body system is described by

$$ \begin{cases} J_t \frac{d\omega_t}{dt} = \tau_t - k \int (\omega_t - \omega_g) \, dt \\ J_g \frac{d\omega_g}{dt} = k \int (\omega_t - \omega_g) \, dt - \tau_g, \end{cases} \quad (2.4) $$
where $J_t$ is the moment of inertia of the turbine, $\tau_t$ is the turbine torque, $k$ is the torsional stiffness of the shaft, $J_g$ is the moment of inertia of the rotor of the generator and $\tau_g$ is the generator torque. In Paper VII, this model was used to determine the transfer function from current ripple to torque ripple. Equation (2.4) can be solved by integrating turbine and generator angles separately. However, it is numerically beneficial to integrate the total angle of one body and the twist angle, since both angles eventually become large, thus limiting the numerical accuracy of their difference.

Torsional vibrations were not considered to be important for evaluation of the control strategy in Paper V or the electrical topologies studied in Paper VI. Therefore, Papers V and VI use a simpler rigid one body model for each turbine described by

$$\frac{d\omega_t}{dt} = \frac{\tau_t - \tau_g}{J_t + J_g}.$$  \hspace{1cm} (2.5)

2.2 Aerodynamics

The total mechanical power extracted from the wind by a turbine is

$$P_t = \frac{1}{2} C_P \rho_{\text{air}} A_t V^3,$$  \hspace{1cm} (2.6)

where $C_P$ is the power coefficient, $\rho_{\text{air}}$ is the density of air, $A_t$ is the projected turbine area and $V$ is the wind speed far upstream of the turbine. In (2.6) the absorbed power is proportional to the cube of the wind speed. This has several important implications:

- It generally makes little sense to design a system that is very efficient at low wind speed, since low-speed wind contains little energy.
- At high wind speed, the power in the wind is large and the survival of the wind turbine is more important than extracting energy from the wind.
- The average wind speed of a specific site is often given as a root mean cube value, since such an average is proportional to the average available power. Betz’s limit states that the power coefficient of a flat turbine is

\[ C_P \leq \frac{16}{27}. \]  

(2.7)

Strictly speaking, the Betz limit does not apply to the straight-bladed Darrieus-type turbine, since it is not flat. Minor changes in turbine design can often improve the \( C_P \) and thereby increase the total yield of the entire wind energy converter. However, it is apparent from (2.6) that \( A_t \) and \( C_P \) are equally important, i.e. a less efficient turbine that sweeps a larger area for the same cost can be the better choice in terms of cost of energy.

The power coefficient of a turbine with fixed blades is a function of wind speed and turbine speed. To a first approximation, the power coefficient of such a turbine is a function of the turbine tip speed ratio. Many control strategies, including the implementation of the robust control strategy in Papers V and VI, are based on this approximation [19]. The tip speed ratio is defined as

\[ \lambda = \frac{\omega_t R_t}{V}, \]  

(2.8)

where \( \omega_t \) is the angular velocity of the turbine and \( R_t \) is the turbine radius.

### 2.2.1 Wind shear

The wind speed at low altitude is reduced through interaction with the ground, a phenomenon known as wind shear. Wind shear can be modelled with a logarithmic profile or with the simplified power law profile [5]. According to the power law profile, the wind speed at height \( z \) above the ground is

\[ V(z) = V(z_0) \left( \frac{z}{z_0} \right)^\eta \]  

(2.9)

where \( V(z_0) \) is the wind speed at the reference height \( z_0 \) and \( \eta \) is a location specific “roughness parameter”. According to (2.9), an increase of the turbine tower height increases the available wind resource at a given site. The power law profile is used to compensate for wind shear in Paper II.

### 2.2.2 Turbine torque ripple

Torque ripple denotes time variations in torque, which are transmitted through the various components of a wind energy converter [20]. Even under ideal conditions with constant wind from a fixed direction, most wind turbines produce
a fluctuating torque. The torque ripple of a HAWT is typically caused by wind shear and tower shadow [21]. The torque ripple of a VAWT originates from the continuously changing angles of attack between the apparent wind and the turbine blades [22, 23]. A VAWT, designed to minimise the turbine torque ripple, is presented in Fig. 2.2. The torque of a VAWT, operating in wind of constant speed, is periodic due to the rotation and can therefore be written as

\[
\tau_t = \bar{\tau}_t \left( 1 + \sum_{n=1}^{\infty} \tilde{\tau}_{tn} \cos(n\theta_t + \Theta_n) \right), \tag{2.10}
\]

where \(\bar{\tau}_t\) is the average turbine torque, \(\theta_t\) is the turbine angle, \(\tilde{\tau}_{tn}\) and \(\Theta_n\) are the relative intensity and the phase shift of the \(n\)th ripple component. It is common to refer to the various ripple components as the component number followed by the letter \(p\), i.e. \(\tilde{\tau}_2\) is the amplitude of the \(2p\) component. Only the mean torque, not the torque ripple, contributes to the power absorption. According to (2.1) and (2.6)

\[
C_P = \frac{2\bar{\tau}_t \bar{\omega}_t}{\rho_{air} A_t V^3}. \tag{2.11}
\]

Turbine torque ripple increases both structural loads and material fatigue. It can also increase the requirements on the electronics that supply the converted power to the electrical grid. In Paper VII, the harmonic content of the turbine torque ripple was measured at various tip speed ratios.

Figure 2.2: A VAWT with low torque ripple.
2.3 Electricity

Diverse electromagnetic phenomena are explained by Maxwell’s equations: Gauss’s law,
\[ \nabla \cdot \mathbf{D} = \rho_f, \]  
(2.12)

where \( \mathbf{D} \) is the electric displacement field and \( \rho_f \) is the free charge density; Gauss’s law for magnetism
\[ \nabla \cdot \mathbf{B} = 0, \]  
(2.13)

where \( \mathbf{B} \) is the magnetic flux density; Faraday’s law of induction
\[ \nabla \times \mathbf{E} = -\frac{\partial \mathbf{B}}{\partial t}, \]  
(2.14)

where \( \mathbf{E} \) is the electric field and Ampère’s law
\[ \nabla \times \mathbf{H} = \mathbf{J}_f + \frac{\partial \mathbf{D}}{\partial t}, \]  
(2.15)

where \( \mathbf{H} \) is the magnetic field strength and \( \mathbf{J}_f \) is the free current density. The geometry and constitutive relations of the materials determine \( \mathbf{E} \) and \( \mathbf{B} \) from the applied \( \mathbf{D} \) and \( \mathbf{H} \). The constitutive relations take into account magnetisation and polarisation. In general,
\[ \mathbf{D} = \varepsilon_0 \mathbf{E} + \mathbf{P}, \]  
(2.16)

where \( \mathbf{P} \) is the polarisation density and
\[ \mathbf{B} = \mu_0 (\mathbf{H} + \mathbf{M}), \]  
(2.17)

where \( \mathbf{M} \) is the magnetisation. In the special case of linear, homogeneous and isotropic materials
\[ \mathbf{B} = \mu \mathbf{H}, \]  
(2.18)

and
\[ \mathbf{D} = \varepsilon \mathbf{E} \]  
(2.19)

where \( \mu \) is the permeability and \( \varepsilon \) is the permittivity of the material. Ohm’s law is the constitutive relation between electric field and free current.

In a generator, a variation of the magnetic flux density is brought about through mechanical means and currents through the induced electric field absorbs the converted mechanical power. An axial cross section of a four pole permanent magnet synchronous generator is illustrated in Fig. 2.3. The winding consists of a material with high conductivity, typically copper. The stator core consists of a material with high permeability, typically electrical steel sheets. All generators in this thesis have a few characteristics in common:
• the rotor has non-salient magnetic poles and multiple pole pairs;
• the rotor magnetisation is provided by surface mounted neodymium magnets;
• the stator winding has three phases that are spaced 120 electrical degrees apart;
• the electromotive force (emf) of each phase is nearly sinusoidal;
• the stator is cable wound.

2.3.1 Power loss within a stator

The variation of the magnetic field in the stator core incurs power loss through two separate phenomena: resistive loss due to eddy currents and hysteresis loss due to the continuous magnetisation and demagnetisation of the stator core. A typical stator core material magnetisation curve is illustrated in Fig. 2.4. The power loss per cycle due to hysteresis depends on the amplitude of the magnetic field which, in turn, depend on the magnitude of the magnetic field. Usually, the field dependence of the energy loss per cycle is approximated according to

$$k_{hy}(B_{max}) = k_h B_{max}^\beta,$$  \hspace{1cm} (2.20)

where $B_{max}$ is the amplitude of $B$, $k_h$ is a material specific constant and $\beta$ is the Steinmetz number, which is material specific but usually close to 1.6 when $H < H_c$ [24]. The specific power loss due to hysteresis is proportional to the frequency of the applied field

$$P_{hy} = k_{hy}(B_{max}) f_{el}.$$  \hspace{1cm} (2.21)
Figure 2.4: Illustration of a hysteresis loop for electrical steel.

The eddy currents are induced in the same way as the armature currents, the specific eddy current loss is

$$P_{\text{ed}} = \frac{\pi^2 \sigma d^2}{6 \rho_c} B_{\text{max} f_{\text{el}}}^2 = k_c B_{\text{max} f_{\text{el}}}^2,$$

(2.22)

where $\sigma$ is the sheet conductivity, $d$ is the sheet thickness and $\rho_c$ is the mass density of the core [25]. Power loss due to eddy currents is reduced through reduction of the conductivity or reduction of thickness of the core laminations.

The total specific loss is the sum of hysteresis and eddy current loss, i.e. (2.21) and (2.22). It is common to also include an *excess loss term* which gives the following expression for the total specific loss [26, 25]

$$P_{\text{Fe,s}} = k_h B_{\text{max} f_{\text{el}}}^\beta + k_e B_{\text{max} f_{\text{el}}}^{3/2} + k_c B_{\text{max} f_{\text{el}}}^2.$$

(2.23)

The excess loss, also called anomalous loss, is caused by currents with high frequency that occur when magnetic domain walls move [27]. The excess loss term is somewhat controversial since it is possible to predict core loss very accurately even if the excess term is forced to zero [28]. Typically, the coefficients of (2.23) are determined through curve fitting of measured loss [25]. Equation (2.23) is not the best core loss model in existence. More advanced models for the specific loss take into account both variations in the direction of the magnetic flux and the history of the magnetic flux. Such models can predict the power loss more accurately [29].

The magnetic flux within the stator during operation can be determined numerically, for instance with the finite element method. Integrating (2.23) over the stator yields the total power loss in the stator core, $P_{\text{Fe}}$, given by

$$P_{\text{Fe}} = k_H f_{\text{el}} + k_E f_{\text{el}}^{3/2} + k_C f_{\text{el}}^2,$$

(2.24)
where $k_H$, $k_E$, and $k_C$ are generator specific constants related power loss through hysteresis, excess eddy currents and classical eddy currents respectively. This loss corresponds to a generator torque of

$$
\tau_c = \frac{P_{Fe} \left( \frac{\omega_g}{2\pi N_{pp}} \right)}{\omega_g}
$$

(2.25)

where $N_{pp}$ is the number of pole pairs of the rotor and $\omega_g$ is the angular velocity of the rotor.

Resistive loss occurs in the stator winding. At any given instant this loss is

$$
P_w = \sum_n R I_n^2,
$$

(2.26)

where $R$ is the per phase resistance, $I_n$ is the $n$th phase current and the summation is taken over all phases.

### 2.3.2 Generator model

The emf induced in the $n$th stator winding is

$$
\mathcal{E}_n = \oint_{C_n} \mathbf{E} \cdot d\mathbf{l} = -\frac{d}{dt} \iint_{A_n} \mathbf{B} \cdot d\mathbf{A},
$$

(2.27)

where $C_n$ is the path along the phase winding and $A_n$ is the area enclosed by the phase winding. The currents flowing through the stator winding absorb power according to

$$
P_{gc} = \sum_n \mathcal{E}_n I_n,
$$

(2.28)

where the summation is taken over all phases. The magnetic flux is determined by (2.15) and the geometry and constitutive relations of the stator, rotor and air-gap. The finite element method is often used to estimate the magnetic reluctance with respect to both rotor magnetisation and armature currents [30]. Typically, the reluctance is dominated by the air-gap and the permanent magnets, due to the high permeability of stator and rotor steel. The total reluctance usually varies with the rotor position, which causes a variation in the total energy of the magnetic field during the rotor rotation. These variations in potential energy give rise to a pulsating torque referred to as the cogging torque [31]. Over a full revolution, the average power due to the cogging torque is zero.

Under the assumption that the flux linkage varies sinusoidally and that the three phases are $120^\circ$ apart, the induction of phase $n$ is

$$
\mathcal{E}_n = \omega_g \Lambda \cos \left( N_{pp} \theta_g - n \frac{2\pi}{3} \right),
$$

(2.29)
where $\omega_g$ is the rotor angular velocity, $\Lambda$ is the peak flux linkage (normalised to mechanical frequency) and $\theta_g$ is the (mechanical) rotor angle. Each phase winding has internal resistance, self inductance and mutual inductance with the other phases. The inductance and mutual inductance of a non-salient rotor machine are close to constant due to the small variations in overall geometry during a rotor revolution. The line to neutral voltage of a Y-connected generator is

$$U_{nN} = \mathcal{E}_n - RI_n - L_{\text{self}} \frac{dI_n}{dt} - L_{\text{mutual}} \frac{d}{dt} \sum I_n. \quad (2.30)$$

According to Kirchoff’s current law (which follows from the divergence of (2.15)) a floating neutral enforces

$$I_n = - \sum_{k \neq n} I_k. \quad (2.31)$$

According to (2.30) and (2.31)

$$U_{nN} = \mathcal{E}_n - RI_n - L \frac{dI_n}{dt} \quad (2.32)$$

where

$$L = L_{\text{self}} + L_{\text{mutual}}. \quad (2.33)$$

Papers I, V, VI and VII are based on the generator model given by (2.24), (2.25), (2.28), (2.29) and (2.32).

### 2.3.3 Permanent magnets

A permanent magnet is a material with high remanent magnetisation $M_r$ and high coercivity $H_c$, see Fig. 2.5 and compare with Fig. 2.4. In generator applications, permanent magnets are typically mounted on the rotor and provide the magnetic field of the rotor. Permanent magnets do not require power and are considered to be reliable. Unfortunately the magnetisation of a permanent magnet is not easily controlled and demagnetisation can be an issue. The most common magnetic materials in generators and motors are ferrites or rare earth magnets, such as neodymium magnets ($\text{Nd}_2\text{Fe}_{14}\text{B}$) or samarium-cobalt magnets ($\text{SmCo}_5$). Rare earth magnets provide a higher magnetisation, but are currently more expensive than ferrites [32]. Permanent magnets are often modelled as a material with a fictitious surface current [33].

### 2.3.4 The diode rectifier

The operating speed of a synchronous generator that is connected directly to an electrical grid is set by the grid frequency and the number of poles on the rotor of the generator. Wind turbines, especially with fixed blade pitch,
favour variable speed operation. Variable turbine speed is achieved with power electronics. A rectifier converts the generator output to DC current which is then converted into grid frequency currents by an inverter. Modelling of the rectifier is an essential part of Papers V, VI and VII.

A six step diode rectifier is illustrated in Fig. 2.6. Diodes are nonlinear components with a low forward voltage drop and a high reverse breakdown voltage, i.e. within a large voltage range they conduct well in one direction and not at all in the opposite direction. For simplicity, the small forward voltage drops of the diodes are not considered here. If a phase current, for example $I_1$, is positive then that terminal will be in direct contact with the positive side of the DC-bus. Similarly, a phase with negative current will be in direct contact with the negative DC-bus. Without loss of generality, assume that $U_1 \leq U_2 \leq U_3$. Depending on rotor speed, and the voltage of the DC-bus, there are four distinct situations (Fig. 2.7).

The rectified voltage, $U_{DC}$, is never smaller than the largest line-to-line voltage of the generator connected to the rectifier. In the case of a PM generator,
the emfs are proportional to the rotor speed according to (2.29). Consequently, $U_{DC}$ sets the speed at which the generator starts to deliver power to the DC-bus.

![Diagrams](image)

Figure 2.7: Rectification, all possible rectifier states given that $U_1 \geq U_2 \geq U_3$. Current reference directions are chosen according to the generator convention, i.e. positive currents are exiting the generator.

The benefits of the diode rectifier are that the internal loss is low, reliability is high and a controller is not required. However, the diode rectifier causes harmonic content in the phase currents and it does not draw current at unity power factor. Compared to an active rectifier, the diode rectifier both introduces torque ripple and increases resistive loss in the stator winding. The minimal resistive loss, for a given converted power, occurs when currents are drawn at unity power factor. In Paper V, the winding loss with a diode rectifier is compared to winding loss when currents are drawn at unity power factor. For a Y-connected three phase system the resistive loss, when operated at unity power factor, is

$$P'_w = 3RI^2 = 3R \left( \frac{P_{gc}/3}{\omega_g \Lambda/\sqrt{2}} \right)^2 = \frac{2R}{3} \left( \frac{P_{gc}}{\omega_g \Lambda} \right)^2,$$

where $P'_w$ is the resistive loss at unity power factor and $I$ is the RMS of a phase current. In Paper V, power loss with a diode rectifier is compared to power loss at unity power factor; extra degrees of freedom were added to the state space to directly compute integrals of (2.26) and (2.34) during the simulations.
The terminal voltage of the diode rectifier changes abruptly whenever a phase current changes direction. Numerical solution of an ordinary differential equation involves adaptive step-size control based on continuous error estimation. An abrupt change in system behaviour is interpreted as a “too large step” and step size is reduced (indefinitely).

An alternative approach is to implement the four different models in Fig 2.7 and carefully try to estimate precisely when the current is very close to zero and, at that time, set the current to precisely zero and change circuit model. However, this approach has the same fundamental problem as the first approach – the time when the current is precisely zero must be determined. This second approach is also more complex to implement.

In Papers VII, V and VI each diode pair, in the rectifier, is modelled by an error function according to

\[ U_n(I_n) = \frac{U_{DC}}{2} \text{erf}(I_n/\Delta I), \]  

(2.35)

where \( U_{n} \) is the terminal voltage of terminal \( n \), \( U_{DC} \) is the DC-voltage and \( \Delta I \) is the parameter of diode approximation. The terminal voltage attains the potential of the positive or negative DC potential when the phase current magnitude becomes much larger than \( \Delta I \). This approach recreates the behaviour outlined in Fig. 2.7 and allows for more advanced stepping functions since the first and second derivative of the error function are easily estimated. The approximation is easily extended to a higher or lower number of phases, or even multi generator systems with standard adaptive step algorithms. The approximation can be made arbitrarily close to the ideal diode bridge by decreasing \( \Delta I \). The difference between approximation 2.35 and an ideal diode bridge is visualised in Fig. 2.8.

![Figure 2.8: Approximate rectifier voltage (smooth) and ideal rectifier voltage (abrupt).](image)
2.4 Heat

The power limits of electrical machines are often determined by the temperature limits of the used materials, the internal machine power loss and the available cooling. Well designed cooling can increase both the capacity and reliability of electrical machines and components. There are four fundamental modes of heat transfer:

**Conduction** The transfer of heat between objects that are in physical contact.

**Convection** The transfer of heat between an object and its environment, due to fluid motion.

**Radiation** The transfer of heat to or from a body by means of the emission or absorption of radiation.

**Advection** The transfer of heat from one location to another as a side effect of physically moving an object containing the heat.

Conduction is described by the heat equation

\[
\frac{\partial T}{\partial t} = \alpha \nabla^2 T + \frac{P_d}{\rho c},
\]

(2.36)

where \( T \) is the temperature, \( \alpha \) is the thermal diffusivity, \( P_d \) is the heat dissipation, \( \rho \) is the mass density and \( c \) is the specific heat. The first term in (2.36) is the heat transfer due to temperature gradients and the second term is the internal power dissipation. Within a homogeneous body of thermal conductivity \( \kappa \), the heat flow is

\[
q = -\kappa \nabla T.
\]

(2.37)

The heat exchange through a surface is limited by the temperature difference, the contact area and the thermal transfer coefficient of the surface

\[
q_{ab} = (T_a - T_b) h_{ab} A,
\]

(2.38)

where \( T_a - T_b \) is the average temperature difference between the two materials, \( h_{ab} \) is the heat transfer coefficient of the surface and \( A \) is the area of the surface through which the heat transfer takes place.

The thermally most sensitive component of a generator is usually the winding insulation. Unfortunately, the winding itself is difficult to cool and it is therefore usually the hottest part of a generator. To the best of the authors knowledge, all cable based generator windings are cooled exclusively by means of conductive heat transfer through the cable insulation. The heat exchange through the winding insulation is driven by the temperature difference between the winding and the outer surface of the insulation and limited by the insulation thickness and thermal resistance of the winding insulation. An alternative to increasing cooling is, of course, to reduce
losses which is achieved by increasing the amount of copper in the winding. Reduction of the power loss has the additional benefit of increased efficiency. Both reducing the ohmic loss within the winding and increasing the mass of the winding benefit overload capability. The convective heat transfer coefficient between the stator and the air-gap of a generator was measured in Paper VIII.

2.4.1 Stationary heating

In Paper VIII the generator described in section 4.1 is studied in order to determine the heat transfer coefficient between the stator and the air-gap. A complication during the experiment was that the ambient room temperature increased during the measurement. However, the rate of room temperature increase was close to constant. Under such conditions the generator does not reach a thermal equilibrium characterised by steady temperature, instead a quasi stationary state is reached where the temperature of the body increases at the same rate everywhere.

![Figure 2.9: Circuit equivalent of quasi stationary heating. Temperatures are equivalent to voltages, heat flows are represented by currents and internal heat dissipation is represented by external current sources. The box is a complex network of resistors where a capacitor and an external current source are connected to each node.](image)

The conductive heat flow within a body is driven by the temperature gradients and the direction of flow is such that temperature differences are reduced rather than increased. In a situation where the boundary temperature and the dissipated heat are both constant, a temperature field is built up in such a way the temperature is constant in time (but not spatially). For a reader with a background in electricity, it is helpful consider the circuit equivalent of the thermal flow that is displayed in Fig. 2.9. Here, temperatures are represented by the capacitor voltages, internal heat dissipation is represented by the injected currents. The boundary temperature is represented by $T_g$ and the bulk insulation between the boundary is represented by $h_{\text{bulk}}$. When heat is applied, all capacitors will charge to a constant level at which point the net heat into each capacitor is zero and the current through $h_{\text{bulk}}$ is exactly equal to the total power being dissipated within the body. Initially the node temperatures will
vary, but after a thermal settling time the temperature of each node will be constant. Note that different nodes will have different temperatures depending on the insulation and heat dissipation of the individual nodes.

Now consider the situation where $T_g$ is increasing at constant rate $\dot{T}$. Here, the state of constant heat flux corresponds to the temperature of nodes increasing at the same rate and thereby keeping the temperature differences between adjacent nodes constant. The heat flowing into each node is not zero but exactly large enough to increase the temperature of that node at the same rate as the boundary temperature increases. The heat transferred from a node to its surrounding is the difference between internal heat dissipation in that node and the heat required to increase the temperature of the node itself. At quasi stationary heat transfer, the temperature everywhere increases at the rate $\dot{T}$ and the spatial temperature differences satisfy

$$\dot{T} = \alpha \nabla^2 T + \frac{P_d}{\rho c},$$

(2.39)

cf. (2.36). Note that due to preservation of energy

$$\iiint_V P_d dV = q_{bdry} + \dot{T} \iiint_V \rho c dV,$$

(2.40)

where $q_{bdry}$ is the total heat leaving the system. In Paper VIII the total dissipated heat is the electric power entering the generator and the total thermal capacity is the sum of the thermal capacity of the stator, $c_{ps}$, and the thermal capacity of the rotor, $c_{pr}$. Consequently, (2.40) is written as

$$q_{bdry} = P_{el} - (c_{pr} + c_{ps})\dot{T},$$

(2.41)

in Paper VIII.

2.4.2 Air-gap temperature profile

The convective heat transfer between stator and air-gap is driven by the temperature difference between the stator and the air-gap. Heat absorption increases the air-gap temperature and gradually decreases the temperature difference between the air-gap and its surroundings. In the limit of low airflow, the air will reach the stator temperature almost immediately upon entering the air-gap, and the temperature difference will be virtually zero. In the limit of high airflow, the entire air-gap will have the temperature of the air at the inlet.

In Paper VIII, a model for how rapidly the air temperature increases is required. This section is a derivation of the approximate expression used for the average temperature difference; i.e. this section is a derivation of equation (9) and (10) in Paper VIII.

The physical situation is illustrated in Fig. 2.10, air continuously flows through the air-gap in the vertical direction. Fans below and above the gen-
Figure 2.10: The thin segment of air has uniform temperature and moves upwards with velocity $v$ while exchanging heat with the stator and rotor. The segment is a thin disc with a central hole enclosing the rotor.

Generator provides the airflow. Both stator and rotor surface temperatures are assumed to be constant (spatially) while the air temperature increases along the air-gap. The derivation of Equation (9) in Paper VIII is based on the following approximations:

1. Stator temperature is constant.
2. The air-gap only exchanges heat with the stator, the rotor and the surrounding air. Heat exchange with the surrounding air only occurs as the air enters and leaves the air-gap.
3. There is no internal heating, such as viscous friction, within the air-gap.
4. All heat exchange with the surroundings, except via the air-gap, is small.
5. Heat transferred from the air-gap to the rotor is a fixed fraction of the heat transfer from the stator to the air-gap (this is an approximation).
6. The average vertical air velocity is constant.
7. The internal loss in the rotor is small.
8. The temperature of all components of the stator and rotor increase at the same constant rate $\dot{T}$ (see section 2.4.1).
Consider the upwards moving air-gap segment of height $\Delta h$ and temperature $T_g$ (Fig. 2.10b). The temperature of this segment increases according to
\[ \frac{dT_g}{dt} = \frac{\Delta q}{\Delta C}, \] (2.42)
where $T_g$ is the temperature of the air in the air-gap segment, $\Delta q$ is the heat transferred to the segment of the air-gap $\Delta C$ is the heat capacity of the segment. The thermal capacity of the segment is
\[ \Delta C = c_{\text{air}} \rho_{\text{air}} \Delta h \pi \left( r^2 - (r - \delta)^2 \right) = c_{\text{air}} \rho_{\text{air}} \Delta h \pi (2r\delta - \delta^2). \] (2.43)
where $c_{\text{air}}$ is the specific heat of air, $\rho_{\text{air}}$ is the density of air, $r$ is the inner radius of the stator and $\delta$ is the width of the air-gap. The heat balance of the air segment is
\[ \Delta q = q_{sg} - q_{gr}, \] (2.44)
where $q_{sg}$ is the heat flux from the stator to the air-gap segment and $q_{gr}$ is the heat flux from the air-gap segment to the rotor. According to assumption 5, (2.44) can be written as
\[ \Delta q = k_r q_{sg}, \] (2.45)
where $k_r$ is the ratio of the heat flow from the stator that does not “immediately proceed” from the air-gap to the rotor. According to (2.38), the heat transfer from the stator to the air-gap is
\[ q_{sg} = 2 \pi r \Delta h h_{sg}(T_s - T_g), \] (2.46)
where $h_{sg}$ is the convective heat transfer coefficient between the inner surface of the stator and the air-gap and $T_s$ is the temperature of the stator inner surface. Similarly,
\[ q_{gr} = 2 \pi (r - \delta) \Delta h h_{gr}(T_g - T_r), \] (2.47)
where $h_{gr}$ is the convective heat transfer coefficient between the rotor surface and the air-gap and $T_r$ is the temperature of the rotor surface. According to (2.42) to (2.47)
\[ \frac{dT_g}{dt} = \frac{2k_r \pi r h_{sg} (T_s - T_g)}{(2r\delta - \delta^2) c_{\text{air}} \rho_{\text{air}}}. \] (2.48)

Define $t = 0$ as the time when the segment enters the air-gap. At this time the temperature of the segment is $T_{in}$ i.e.
\[ T_g(0) = T_{in}. \] (2.49)
If the air remained within the stator indefinitely it would eventually assume the temperature of the stator

$$\lim_{t \to \infty} T_g(t) = T_s. \quad (2.50)$$

According to (2.48), (2.49) and (2.50)

$$T_g(t) = T_s - (T_s - T_{in}) \exp \left( \frac{-2rk_r h_{sg}}{(2r \delta - \delta^2)c_{air} \rho_{air}} t \right). \quad (2.51)$$

By the time the air exits the stator, it has attained the temperature $T_{out}$. According to assumption 6

$$T_g(h/v) = T_{out} \quad (2.52)$$

where $v$ is the average vertical air velocity. According to (2.51) and (2.52)

$$h_{sg} = \frac{(2r \delta - \delta^2)c_{air} \rho_{air} v}{2rk_r h} \log \left( \frac{T_s - T_{in}}{T_s - T_{out}} \right). \quad (2.53)$$

The vertical air velocity, $v$, is not measured directly and must be estimated. Due to the preservation of energy

$$(T_{out} - T_{in})c_{air} \dot{m} = P_{el} - (c_{ps} + c_{pr}) \dot{T}, \quad (2.54)$$

where $T_{out}$ is the exhaust air temperature, $T_{in}$ is the intake air temperature, $c_{air}$ is the specific heat of air and $\dot{m}$ is the air mass flow rate. The last term in the right hand side of (2.54) is power that remains in the material due to quasi stationary heating, see section 2.4.1. The total airflow is

$$\dot{m} = v \rho_{air} \pi (2r \delta - \delta^2). \quad (2.55)$$

According to (2.54) and (2.55), the velocity estimated from the temperature increase is

$$v = \frac{P_{el} - (c_{ps} + c_{pr}) \dot{T}}{c_{air} \rho_{air} \pi (2r \delta - \delta^2)(T_{out} - T_{in})}. \quad (2.56)$$

According to (2.53) and (2.56)

$$h_{sg} = \frac{P_{el} - (c_{ps} + c_{pr}) \dot{T}}{2\pi r h k_r (T_{out} - T_{in})} \log \left( \frac{T_s - T_{in}}{T_s - T_{out}} \right), \quad (2.57)$$

which is equation (9) in Paper VIII.

The coefficient $k_r$ follows directly from the heat required for stationary heating. According to (2.44) and (2.45)

$$k_r q_{sg} = q_{sg} - q_{gr}. \quad (2.58)$$
Due to the power flow illustrated in Fig. 2.10b

\[ q_{sg} = P_{el} - P_t - c_{ps} \dot{T}, \quad (2.59) \]

where \( q_{sg} \) is Heat flow from the stator to the air-gap and \( P_t \) is Internal rotor heating. Further,

\[ q_{gr} = c_{pr} \dot{T} - P_t, \quad (2.60) \]

where \( q_{gr} \) is the heat flow from air-gap to rotor. According to (2.58), (2.59) and (2.60)

\[ k_r = 1 - \frac{c_{pr} \dot{T} - P_t}{P_{el} - P_t - c_{ps} \dot{T}}, \quad (2.61) \]

which is (10) in Paper VIII.
3. Direct drive VAWT concept

The wind power project at Uppsala University is focused on the straight-bladed Darrieus-type turbine, also known as the H-rotor, with a direct driven PM generator. Our working hypothesis is that such a wind turbine can be more cost-effective than the mainstream HAWT [3, 34].

In this chapter the studied concept is explained and potential benefits and challenges are highlighted. The design philosophy is “keep it simple”; the number of mechanical degrees of freedom is kept minimal and passive components are used whenever possible. The result should be a reliable system with low maintenance requirements. Statistics on failures in HAWT show that failures in gears, generators, pitch systems and yaw systems cause a significant part of the total down time, see Table. 3.1. Gears, yaw and pitch system are not required at all in the presented concept and the generator can be more robust due to relaxed size restrictions.

<table>
<thead>
<tr>
<th>Component</th>
<th>Downtime (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Gears</td>
<td>19.4</td>
</tr>
<tr>
<td>2. Yaw system</td>
<td>13.3</td>
</tr>
<tr>
<td>3. Blades/Pitch</td>
<td>9.4</td>
</tr>
<tr>
<td>4. Control system</td>
<td>18.3</td>
</tr>
<tr>
<td>5. Electric System</td>
<td>14.3</td>
</tr>
<tr>
<td>6. Generator</td>
<td>8.9</td>
</tr>
<tr>
<td>7. Sensors</td>
<td>5.4</td>
</tr>
<tr>
<td>8. Hydraulics</td>
<td>4.4</td>
</tr>
<tr>
<td>9. Drive train</td>
<td>2.4</td>
</tr>
<tr>
<td>10. Entire unit</td>
<td>1.7</td>
</tr>
<tr>
<td>11. Mechanical brakes</td>
<td>1.2</td>
</tr>
<tr>
<td>12. Structure</td>
<td>1.2</td>
</tr>
</tbody>
</table>

Table 3.1: Percentage of downtime per component in Swedish wind turbines between 2000–2004 [35]. Systems 1 to 3 are not required in the presented concept.
3.1 The turbine

The straight-bladed Darrieus-type turbine (Fig. 3.1) is the turbine of choice. This turbine is omni-directional and does therefore not require a yawing system. The turbine blades have fixed pitch, i.e. they are rigidly attached to their supporting struts. The WEC adapts to the wind speed by changing the turbine speed instead of pitching the blades. Turbines with fixed blades are typically not aerodynamically self starting, instead the start of the turbine is achieved by operating the generator as a motor [36]. A turbine with a vertical axis can have the generator located at ground level, which is a major benefit as it simplifies deployment, simplifies maintenance and relaxes size constraints. Instead of being optimised for low weight and volume, the generator can be efficient, direct driven and utilise inexpensive, but heavy, ferrite magnets [32]. Simulations predict that the straight-bladed Darrieus-type turbine produces less noise than the typical HAWT [37].

3.2 Electrical design

Here, focus is on a converter topology based on a cable wound direct driven PM generator with rectification and a full inverter. Such a converter topology can be efficient and allows fully variable speed [38]. The electrical system of an entire wind farm is illustrated in Fig. 3.2. Each generator is equipped with a diode rectifier, all rectifiers connect to a mutual DC-bus from which a single
inverter feeds power to a utility grid. The studied electrical system is very similar to the electrical system previously intended for the Windformer™ [39].

![Diagram of electrical system](image)

**Figure 3.2:** Concept for the electrical system. A. Cable wound PM generators with passive rectification; B. Local farm DC grid; C. Inverter; D. Transmission grid and load.

Active rotor magnetisation requires power and maintenance, a permanent magnet rotor requires neither. The magnets are either magnetised after or magnetised before they are attached to the rotor, both approaches are associated with certain difficulties.

Direct driven generators must handle high torque and are therefore relatively bulky compared to high speed generators of similar rating [3]. This is less of an issue when the generator is located at ground level but it is a significant drawback for a HAWT where the generator is mounted in the nacelle. Still most major manufacturers of HAWT move towards direct driven solutions to avoid the maintenance, reduced reliability and losses associated with the use of a gearbox. A way to achieve high generator torque, without increasing the required amount of active material, is to increase the generator radius [40].

The use of a cable based winding allows for higher generator voltage [41]. Here, the benefit of high voltage generators is that the local DC grid voltage is increased (B in Fig. 3.2). Hence, the local DC grid can be widespread without having a too high resistive power loss. An alternative method to increase the voltage of the local DC grid, is to equip each turbine with a DC-boost. The DC-boost approach increases system complexity, incurs losses and is costly. However, the DC-boost approach reduces resistive winding power loss by drawing current at unity power factor and enables individual turbine control which can improve turbine energy absorption. Connecting several generators with different characteristics to a mutual DC-bus was tested in Paper I. The benefit of drawing current at unity power factor was studied through sim-
ulations in Paper V. The improvement in energy capture through individual control was studied in Paper VI.

Passive rectifiers can also introduce torque ripple by drawing phase currents with high harmonic content. This problem is circumvented with active rectification. A benefit of the diode rectifier is that it is very easy to scale up to high voltage.

Generators that connect directly to the grid should have a sinusoidal voltage with low harmonic content. What about generators that connect to diode rectifiers? Consider the situation in Fig. 2.6 at light load with low per phase resistance and negligible per phase reactance. Under such circumstances, a pure sinusoidal emf would always cause currents to flow according to Fig. 2.7b and the entire rectified current passes a resistance of $2R$. A generator where each emf has a perfect square waveform would always cause currents to flow according to Fig. 2.7c and Fig. 2.7d, where the entire rectified current passes a resistance of only $3R/2$. This example illustrates that a sinusoidal waveform is not beneficial when a diode rectifier is used.

The inverter, part C in Fig. 3.2, controls how much power that is supplied to the utility grid and assures that the delivered current has the right frequency, acceptable harmonic content etc. An inverter is being designed for the VAWT prototype of section 4.2, a preliminary evaluation of the inverter is presented in Paper IX. The inverter uses a tap transformer which allows the inverter to operate efficiently over a wide range of DC-voltages. The internal impedance of the transformer contributes to the output filter and consequently the filter characteristics depends on what tap that is used.

### 3.3 Typical operation

A typical control strategy for a fixed pitch turbine is outlined in Fig. 3.3. At wind speeds below the rated wind speed, the turbine speed is controlled in such a way that power absorption is maximised. At wind speeds above the rated, the turbine is operated below optimal speed in order to reduce power absorption and protect the turbine. Reducing power absorption by operating the turbine at sub-optimal speed is referred to as *passive stall*. In very strong winds the turbine will be stopped completely to reduce wind load [42].

Straight-bladed Darrieus-type turbines are typically not self-starting but require a certain tip speed ratio in order to extract power from the wind. A grid connected turbine, or a turbine with an energy storage, is easily started by operating the generator as a motor [36]. An auxiliary system is often used to determine when wind conditions are good enough to start the turbine.

Turbine speed is controlled electrically through control of the electrical load. Papers V and VI investigate a control strategy that maintains a high power coefficient at wind speeds below rated without continuous monitoring of the wind speed; this control strategy is henceforth referred to as the *robust*
control strategy. The idea behind the robust control strategy is explained in section 3.3.1. In section 3.3.2 different versions of the robust control strategy are discussed and the tradeoff between fast control and optimal steady state performance is explained. Finally, section 3.3.3 outlines how the strategy is applied to an entire wind farm.

3.3.1 The robust control strategy

The purpose of the control system for a wind turbine is to maximise the power extracted from the wind while still protecting the wind turbine when the wind is very strong. Protection of a fixed pitch turbine is achieved by locking the turbine and thereby minimising the interaction with the wind. At lower wind speeds the control system should maximise the turbine power by maintaining the turbine speed that maximises $C_P$ for the given wind speed. If the wind speed is measured continuously, then the optimal turbine speed can be maintained by adjusting the DC-level according to the wind speed [43]. Papers V and VI are based on a class of control strategies where wind speed is not measured, instead the extracted power is a function of generator speed.

According to (2.6), the absorbed power increases with the cube of the wind speed as long as the power coefficient $C_P$ is constant. As a first approximation, the power coefficient is a function of the tip speed ratio $\lambda$. When $\lambda$ is kept constant then, according to (2.6), the turbine power will vary with the cube of the wind speed. The idea behind the robust control strategy is that in many situation the opposite of the previous statement is true; if the power extracted by the generator is proportional to the cube of the turbine speed then a constant tip speed ratio will be maintained. With the robust control strategy, the power
extracted by the generator is

\[ P_g = k_2 \omega_t^3, \]  

(3.1)

where \( k_2 \) is a constant. The constant \( k_2 \) is chosen so that the turbine power and generator power are equal when the turbine is operating at the optimal tip speed ratio i.e.

\[ k_2 \omega_{\text{opt}}^3 = P_t(\omega_{\text{opt}}) = \frac{1}{2} C_P(\lambda_{\text{opt}}) A_t \rho \text{air} V_t^3. \]  

(3.2)

\[ \text{Normalised turbine speed} \]

\[ \text{Normalised power} \]

\[ \text{Rotational speed} \]

\[ \text{Power} \]

\[ P_t(\omega_{\text{opt}}) \]

\[ \lambda_{\text{eq}} \]

\[ \lambda_{\text{opt}} \]

\[ C_{P_{\text{opt}}} \]

\[ \omega_{\text{eq}} \]

\[ \omega_{\text{opt}} \]

\[ \text{Normalised power} \]

\[ \text{Figure 3.4: The Robust control strategy at constant wind speed. The power extracted by the generator is normalised in the same way as the power coefficient. } C_{P_{\text{opt}}} \text{ denotes the maximal power coefficient.} \]

Consider a situation with constant wind speed. The power absorbed by the turbine and the power extracted by the generator are illustrated in Fig. 3.4. Here, the turbine speed will converge to \( \omega_{\text{opt}} \) provided that the initial speed is above \( \omega_{\text{eq}} \), otherwise the turbine will stop. The equilibrium at the peak power is always stable since the turbine power is flat near its optimum and the generator power always increases with rotor speed. Additional equilibria can only exist below \( \omega_{\text{opt}} \). Assume that the system is operating at \( \lambda_{\text{opt}} \); what happens if the wind speed is increased? An increase in the wind speed does not affect the mapping from \( \lambda \) to \( C_P \) nor the mapping from \( \lambda \) to normalised load (top and right axis of Fig. 3.4). That is, the system remains stable and will converge to \( \lambda_{\text{opt}} \) provided that the increase in wind speed is small enough so that the tip speed ratio remains above \( \lambda_{\text{eq}} \), otherwise the turbine will stop.

Wind speed does affect response time of the controlled system, the system response is faster in higher wind speed. Again, consider a situation with constant wind speed and a turbine operating in the immediate vicinity of \( \omega_{\text{opt}} \). The turbine power is roughly constant in the proximity of \( \omega_{\text{opt}} \) i.e.

\[ P_t \approx P_t(\omega_{\text{opt}}). \]  

(3.3)
According to (3.1), the net torque that accelerates the system is

\[ \tau = \tau_t - \tau_g = \frac{P_t(\omega_{opt}) - P_g}{\omega_t} = \frac{P_t(\omega_{opt}) - k_2 \omega_t^3}{\omega_t}. \]  

(3.4)

So, according to (3.3), (3.4) and (3.2)

\[ \tau = k_2 \frac{\omega_{opt}^3 - \omega_t^3}{\omega_t} \]  

(3.5)

the stiffness of the equilibrium at \( \omega_{opt} \) is

\[ \left. \frac{d\tau}{d\omega_t} \right|_{\omega_t = \omega_{opt}} = -3k_2 \omega_{opt} \]  

(3.6)

The negative sign of (3.6) indicates that the equilibrium is stable. The magnitude of (3.6) is strictly increasing with \( \omega_{opt} \) which implies that the higher the wind speed, the higher the stiffness of the equilibrium. Higher stiffness is equivalent to a faster system since the moment of inertia of the system is constant.

### 3.3.2 Versions of the robust control strategy

The control strategy given in section 3.3.1 is referred to as strategy A in Paper V and it is henceforth denoted as strategy A here as well. Strategy A has two potential problems in dynamic wind. In low wind speed there is very little power to be absorbed and extracting power increases the risk that the turbine will stop. In very strong winds it is more important to protect the turbine than to have optimal power extraction; strategy A operates the turbine at optimal tip speed ratio regardless of wind speed.

Strategies A, B and C from Paper V are illustrated in Fig. 3.5. Neither strategy B nor strategy C extract any power below \( \omega_0 \) thus minimising the risk that the turbine will stop.

Strategy B increases the generator power more rapidly than strategy A. The higher rate of power increase reduces the response time of the control system (c.f. (3.4) and (3.6)). Another benefit of the more rapid power increase is that turbine speed is limited in strong winds but the limit is soft. The primary disadvantage of strategy B is that the equilibrium tip speed ratio does not coincide with \( \lambda_{opt} \) except at \( \omega_0 \).

Strategy C mimics the exact behaviour of strategy A in the region between \( \omega_1 \) and \( \omega_2 \) and rapidly increases or decreases the torque outside this region to allow low speed freewheeling and limit turbine speed in strong wind.
All strategies A–C can be expressed on the form

\[
P_g = \begin{cases} 
0 & \omega \leq \omega_0, \\
k_1 \omega^2 (\omega - \omega_0) & \omega_0 < \omega \leq \omega_1, \\
k_2 \omega^3 & \omega_1 < \omega \leq \omega_2, \\
k_3 \omega^2 (\omega - \omega_2) + k_2 \omega_2 \omega^2 & \omega_2 < \omega,
\end{cases}
\]  

(3.7)

where

\[
k_1 = \frac{\omega_1}{\omega_1 - \omega_0} k_2
\]

(3.8)

due to continuity. In Papers V and VI \( k_1 = k_3 \) was arbitrarily chosen. The parameter values for strategies A–C are given in Table 3.2. Strategy C is used both in Paper V and VI while strategies A and B only occur in Paper V.

<table>
<thead>
<tr>
<th></th>
<th>A</th>
<th>B</th>
<th>C</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \omega_0 ) rad/s</td>
<td>0</td>
<td>0.9231</td>
<td>0.9231</td>
</tr>
<tr>
<td>( \omega_1 ) rad/s</td>
<td>0</td>
<td>( \infty )</td>
<td>1.4538</td>
</tr>
<tr>
<td>( \omega_2 ) rad/s</td>
<td>( \infty )</td>
<td>( \infty )</td>
<td>2.9077</td>
</tr>
<tr>
<td>( k_1 ) Nms(^3)/rad(^3)</td>
<td>–</td>
<td>( 9.1848 \times 10^3 )</td>
<td>( 1.3178 \times 10^4 )</td>
</tr>
<tr>
<td>( k_2 ) Nms(^3)/rad(^3)</td>
<td>( 4.8111 \times 10^3 )</td>
<td>–</td>
<td>( 4.8111 \times 10^3 )</td>
</tr>
<tr>
<td>( k_3 ) Nms(^3)/rad(^3)</td>
<td>( 4.8111 \times 10^3 )</td>
<td>–</td>
<td>( 4.8111 \times 10^3 )</td>
</tr>
</tbody>
</table>

Table 3.2: Parameters of (3.7) for strategies A–C used in Papers V and VI.
3.3.3 Robust control of several turbines

In Paper VI, the power drawn from the DC-bus is the sum of the powers that would be drawn form each turbine if they were controlled individually i.e.

\[ P_{\text{farm}} = \sum_{i=1}^{N_{\text{farm}}} P_g(\omega_i), \]  

(3.9)

where \( P_{\text{farm}} \) is the power extracted from the \( N_{\text{farm}} \) turbines in the farm and \( \omega_i \) is the speed of the \( i \)th turbine.

It is not obvious that (3.9) is a good way to determine the total load. The topology with a single DC-bus tends to operate all turbines at similar speeds and when the total extracted power is chosen as in (3.9) then the power contribution of freewheeling turbines in low wind speed is overestimated. A weighted average could probably improve overall system performance. However, the small performance difference between wind farm simulations with individually controlled turbines and wind farm simulations with the single DC-bus topology suggests that the improvement with a weighted average would be small (Paper VI).
4. Prototypes

A wind turbine prototype and a generator setup have been constructed within the framework of this VAWT project. The generator setup consists of a generator driven by a variable speed motor. The small VAWT prototype constitutes a demonstration of the concept outlined in section 3. The thesis also comprises a second VAWT turbine prototype that demonstrates how the technology can be adapted for telecommunication installations.

4.1 PM generator

Figure 4.1: In house generator setup. The 12kW PM generator is driven by a variable speed motor.

A setup with a generator and a variable speed motor was constructed within the project, Fig. 4.1. Generator parameters are presented in Table 4.1 and the geometric properties of the air-gap are found in Fig. 4.2. The generator has
been instrumental in evaluations of the generator design procedure, see Papers IV, VIII and [33, 44, 45]. The setup has also served as an in house testing facility for hardware developed for other prototypes such as the VAWT described in section 4.2 and the telecom adapted VAWT in section 4.3. Further, the setup was used during development and testing of the inverter presented in Paper IX and in the DC-bus study in Paper I.

<table>
<thead>
<tr>
<th>Core material</th>
<th>M270-50A</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of slots per pole and phase</td>
<td>5/4</td>
</tr>
<tr>
<td>Stator inner diameter</td>
<td>mm 760</td>
</tr>
<tr>
<td>Stator length</td>
<td>mm 222</td>
</tr>
<tr>
<td>Stator outer diameter</td>
<td>mm 886</td>
</tr>
<tr>
<td>$N_{pp}$ Number of poles</td>
<td>32</td>
</tr>
<tr>
<td>$c_{px}$ Thermal capacity of stator and winding</td>
<td>kJ/K 110</td>
</tr>
<tr>
<td>$c_{ps}$ Thermal capacity of stator and winding</td>
<td>kJ/K 84</td>
</tr>
<tr>
<td>$\sigma$ Stator sheet conductivity</td>
<td>Sm$^{-1}$ $1.8 \times 10^6$</td>
</tr>
<tr>
<td>$d$ Stator sheet thickness</td>
<td>mm 0.5</td>
</tr>
</tbody>
</table>

Table 4.1: Properties of the PM generator.

Figure 4.2: The air-gap: 1. Stator inner radius ($r$) is 380mm; 2. Air gap ($\delta$) is 10mm; 3. Slot opening is 8mm; and 4. Tooth width is 12mm.
4.2 VAWT prototype

The vertical axis wind turbine (Fig. 4.3) is rated at 12kW in 12 m/s wind. The direct driven PM generator is identical to the generator described in section 4.1.

The turbine has a radius of 3 m and a projected cross-section of 30 m². Both ends of each blade are tapered. The tapering starts one meter from each end and reduces the chord length by 60% at the blade tip. The H-rotor’s three blades are connected to the rotating shaft through two streamlined struts per blade. The struts have a blade profile similar to NACA0025. Further turbine characteristics are given in Table 4.2.

<table>
<thead>
<tr>
<th>Aerodynamic control</th>
<th>Passive stall</th>
</tr>
</thead>
<tbody>
<tr>
<td>Blade aerofoil</td>
<td>NACA0021</td>
</tr>
<tr>
<td>Blade length (m)</td>
<td>5</td>
</tr>
<tr>
<td>Blade tip speed (nominal) (m/s)</td>
<td>40</td>
</tr>
<tr>
<td>Chord length (m)</td>
<td>0.25</td>
</tr>
<tr>
<td>Hub height (m)</td>
<td>6</td>
</tr>
<tr>
<td>Nominal rotational speed (rpm)</td>
<td>127</td>
</tr>
<tr>
<td>Number of blades</td>
<td>3</td>
</tr>
</tbody>
</table>

Table 4.2: Properties of VAWT prototype.

Figure 4.3: The wind energy converter. (59°55′32″ N, 17°35′12″ E)

The electrical system of the prototype wind turbine is illustrated in Fig. 4.4. The inverter that supplies the converted power to the utility grid is not yet
implemented, instead the converted power is consumed by a switched load. The power consumed by the load depends on the DC-bus voltage and the duty cycle of the switch. In Papers II and VII, the duty cycle is determined from the voltage of the load side capacitor according to

$$D = \min \left( 1, \max \left( 0, \frac{U_{\text{load}} - U_{\text{min}}}{U_{\text{max}} - U_{\text{min}}} \right) \right),$$  \hspace{1cm} (4.1)

where $U_{\text{max}}$ and $U_{\text{min}}$ are voltage limits. The power supplied to the resistive load is

$$P_{\text{load}}(U_{\text{load}}) = D \frac{U_{\text{load}}^2}{R_{\text{load}}}. \hspace{1cm} (4.2)$$

Decreasing the difference $U_{\text{max}} - U_{\text{min}}$ increases the system stiffness in the sense that the load increases more rapidly with turbine speed. A small difference between $U_{\text{min}}$ and $U_{\text{max}}$ implies that the $U_{\text{DC}}$ will be held constant for a wide range of rectified generator current magnitudes. To a first approximation, such a control will set an upper limit for the generator speed [46]. This speed control is used in Papers II and VII.

![Schematic view of the electrical system of the VAWT prototype.](image)

**Figure 4.4:** Schematic view of the electrical system of the VAWT prototype.

The wind speed at the site is measured by a cup anemometer placed at hub height on a distance of 15 meters (2.5 times the rotor diameter) from the turbine. The absolute error in measured wind speed is typically less than 0.3 m/s according to the manufacturer\(^1\). A power coefficient measurement is presented in Paper II. A measurement of the turbine torque ripple is presented in Paper VII and initial tests on an inverter for grid connection this turbine are presented in Paper IX. The turbine is not aerodynamically self-starting. Start of the turbine is performed by a 6 step inverter connected to an auxiliary low voltage stator winding [36].

\(^1\)http://www.vaisala.nl/files/WMS301and302%20Quick%20Reference%20Guide.pdf (2011-04-07)
4.3 Telecom adaptation

Providing power to telecommunications equipment in remote areas is costly. Diesel powered generators are often used; a solution that has some environmental impacts and requires fuel to be transported to the site. Renewable energy is an attractive alternative since it can be acquired on site and has very low emissions [16]. In a joint project, Ericsson AB, Vertical Wind Communications AB and Uppsala University developed a wind energy conversion system for the Tower Tube (Paper III).

![Diagram](image)

*Figure 4.5: The Towertube is concrete tower that houses a radio base station.*

The Towertube (Fig. 4.5a) is a construction that houses a radio base station (RBS) and antennas. The RBS is initially installed at the bottom of the tower and then raised to the top by an elevator operating within the tower. All sensitive equipment is enclosed within the tower. At the top of the tower there is a weatherproof enclosure (the radome) that protects the RBS. By positioning the RBS at height, feeder losses are reduced, which allows improved network coverage and capacity. The tower has a significant chimney effect that benefits passive cooling which reduces the power required for active cooling. The combination of low power consumption and the tower’s concrete exterior that protects the equipment effectively from harsh weather conditions, makes the Towertube ideal for deployment to remote areas.

It is not trivial to equip the Towertube with a wind turbine. A system that is to be deployed in a remote location should be reliable and require little maintenance. Furthermore, the WEC must not interfere with the normal operation of the telecommunication systems. The turbine should not be mounted at the top of the tower as it then would interfere with the radio transmissions. Also, the WEC must be mounted entirely on the outside of the tower since the interior is used by the maintenance elevator. These challenges were met by constructing an outer rotor generator with an inner radius so large that the
generator could be mounted around the tower, Fig. 4.5b. Observed powers at some wind speeds were recorded and are presented in Fig. 4.6.

Figure 4.6: Measured power from the telecom adaptation of a VAWT.
5. Experiments

This chapter summarises the experiments in Papers II, IV, VII and VIII. Sections 5.3 and 5.1 investigates power loss and cooling of the generator setup described in section 4.1. Sections 5.3 and 5.4 outlines measurements of different aspects of the turbine torque observed on the VAWT prototype described in section 4.2.

5.1 Measurement of no-load core loss

In Paper IV, the power loss in the stator core was measured at no-load and low electrical frequency. At no-load the power loss mechanisms in the generator are rotor windage, bearing friction and core loss due to eddy currents and hysteresis.

The idea behind the measurement is simple, the rotor is accelerated by a well defined torque and is thereafter allowed to decelerate due to power loss while the rotor speed is measured. Power loss is thereafter estimated from the rate of rotor deceleration according to (2.3). The measurement was performed with the mechanical setup shown in Fig. 5.1. Torque is provided by weights attached to a rope that is wound around the central shaft of the generator, Fig. 5.1b. The rope is rigged so that it automatically detaches from the central shaft when the weights reach the ground. After the detachment of the weights, the rotor to decelerates due to power loss alone.

Power loss was measured both before and after the permanent magnets were mounted on the rotor in order to isolate the core loss from the total power loss. Rotor acceleration is always given by (2.2) but both the torque and the moment of inertia depends on the situation, see Table 5.1. Fig. 5.2 illustrates typical acceleration and deceleration with and without mounted magnets. For further details, see Paper VII.
Figure 5.1: 12kW PM generator for wind power, see section 4.1. Weights apply torque on the rotor shaft via a rope.

![Image](image.png)

(a) The actual setup  
(b) Torque applied by one weight

Table 5.1: Applied torque and total inertia during the different stages of the measurement of power loss in the stator core. The acceleration of the rotor is given by (2.2).

<table>
<thead>
<tr>
<th>Magnets mounted</th>
<th>Acceleration</th>
<th>Deceleration</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Yes</td>
<td>No</td>
</tr>
<tr>
<td>Torque ((\tau))</td>
<td>(gr_cm1 - \tau_c - \tau_F)</td>
<td>(gr_cm2 - \tau_F)</td>
</tr>
<tr>
<td>Angular inertial ((J))</td>
<td>(J_g + J_{PM} + m_1r_c^2)</td>
<td>(J_g + m_2r_c^2)</td>
</tr>
</tbody>
</table>

Figure 5.2: Acceleration and deceleration with magnets (solid line) and without magnets (dashed line). At \(t_1\) and \(t_2\) the weights are detached and deceleration begins. At \(t_3\) the rotor with magnets comes to a full stop. The rotor without magnets accelerates more rapidly since it has a lower moment of inertia, it also decelerates more slowly since there is no core loss.
5.2 Measurement of air-gap cooling

In Paper VIII, the heat transfer coefficient between the stator and air-gap, of the generator described in section 4.1, was estimated. The idea behind the measurement is to set up a situation where the heat flow is known and the temperature difference between the stator surface and the air-gap can be measured.

![Thermometer locations.](image1)

(a) Thermometer locations.  

![Insulated generator without lid.](image2)

(b) Insulated generator without lid.

Figure 5.3: Generator setup for the measurement of heat exchange between the stator and air-gap.

Fans were mounted above and below the generator to provide a vertical airflow through the air-gap. Several thermometers were mounted at various locations as shown in Fig. 5.3a. The generator was not connected to any mechanical load and was insulated to minimise the power that left the stator by means other than heat exchange with the air-gap, Fig. 5.3b. The power entering the generator was measured electrically directly at the terminals. The generator was operated for several hours while the temperatures of the stator, the airflow entering the air-gap and the airflow exiting the air-gap were measured.

The temperature of the room where the experiment was performed increased during the experiment. Eventually the generator reached a state of quasi stationary heating where the temperature everywhere increases at the same rate, see section 2.4.1. Then, the heat being transferred to the air gap is the difference between the electric power being supplied to the generator and the heat required to heat up the generator. Two models were used to estimate the air-gap temperature gradient, a linear model and the model derived in section 2.4.2. For further details, see Paper VIII.

5.3 Measurement of the power coefficient

The turbine described in section 4.2 was operated at constant speed for 350h during March, April and May 2009. In this measurement the turbine was oper-
Figure 5.4: The current $I_1$ and the voltage difference $U_1 - U_N$ were both measured, in order to determine the electric power, during the measurement of the power coefficient in Paper II.

operated at close to constant speed while power output and wind speed was monitored. Constant operating speed was achieved by switching the load in such a manner that $U_{\text{load}}$ was held constant.

The turbine speed, wind speed and electrical output from the generator were recorded. The instantaneous generator output

$$P_g = \sum_{k=1,2,3} (U_k - U_N)I_k,$$  \hspace{1cm} (5.1)

was approximated as

$$P_g \approx 3(U_1 - U_N)I_1.$$  \hspace{1cm} (5.2)

For further details, see Paper II.

5.4 Measurement of torque ripple

Torque ripple of the wind turbine described in section 4.2 was measured indirectly through observation of the harmonic content of the rectified generator current $I_{\text{rect}}$. The turbine was operated at close to constant speed for one day while the rectified current $I_{\text{rect}}$ and rotor speed $\omega_g$ were measured. The recorded data was divided into segments comprising 10 full revolutions each. The discrete Fourier transform was used to determine the harmonic content of the rectified current and the component intensities were summarised in a histogram according to turbine tip speed ratio. A linearised model was used to map the observed current ripple into torque ripple. For further details, see Paper VII.
6. Results

Sections 6.1 to 6.3 describes results obtained from the generator setup. Results based on tests of the 12 kW VAWT prototype are found in sections 6.4 and 6.5, both results are investigations and characterisation of the turbine torque. The simulation results in sections 6.6 and 6.7 both concern the robust control strategy.

6.1 DC-grid demonstration

A multi generator grid with passive rectifiers and a common DC-bus has been demonstrated. Fig. 6.1 shows phase voltages and currents of two generators that supply power to a common capacitor bank through passive diode rectifiers. The electrical topology in Paper I differs from the one used in Papers II, VII, V and VI in that the neutral terminal of the generators was connected to the mid point of the capacitor bank.

*Figure 6.1:* Two generators are supplying power to a mutual DC bus.
6.2 Core losses

Figure 6.2: Power loss in the stator core. The highest line represents the measured loss with standard deviations that are enlarged by a factor of 10 for visibility. The middle line represents simulated core loss based on specific power loss measured at frequency. The lowest line represents simulated core loss based on specific power extrapolated from specific loss at 50Hz.

Manufacturers of electrical steel often only provide the specific loss of stator sheets at 50Hz or 60Hz since most electrical machines operate at these frequencies. Generators in variable speed wind turbines are often operated at significantly lower electrical frequencies. This raises the question: Is the specific loss density at a high frequency sufficient to predict the core loss of a generator operating over a range of lower electrical frequencies?

A case study was performed where the power loss in the stator core was both measured and simulated. Two simulations were performed, in the first one the specific core loss at low frequency was extrapolated from core loss at the grid frequency. The second simulation was based on specific loss measured at low frequency. Measured and simulated losses are presented in Fig. 6.2. Both simulations underestimate the power loss in the stator core. As expected, simulations based on extrapolated specific loss appear to be less accurate.

6.3 Air-gap cooling

The measurement described in section 5.2 resulted in the estimates of the convective heat transfer coefficient shown in Table 6.1. The convective heat transfer coefficients are averages over the entire inner surface. Slot wedges cover 40% of the inner stator surface, see Fig. 4.2. These wedges consist of glass reinforced plastic which has over 100 times lower thermal conductivity than the stator steel. Consequently, the heat transfer coefficient of the sta-
tor teeth is up to 67% higher than the surface average. A typical handbook\(^1\) range for the convective heat transfer coefficient under forced convection is 10 W/Km\(^2\) to 200 W/Km\(^2\).

<table>
<thead>
<tr>
<th>Rep</th>
<th>I</th>
<th>II</th>
<th>III</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>(P_{el})  W</td>
<td>638</td>
<td>438</td>
</tr>
<tr>
<td>2.</td>
<td>Speed ratio</td>
<td>1.8</td>
<td>0.5</td>
</tr>
<tr>
<td>3.</td>
<td>(\dot{T})  K/s</td>
<td>(0.36 \times 10^{-3})</td>
<td>(0.18 \times 10^{-3})</td>
</tr>
<tr>
<td>4.</td>
<td>(m)  kg/s</td>
<td>(76 \times 10^{-3})</td>
<td>(62 \times 10^{-3})</td>
</tr>
<tr>
<td>5.</td>
<td>(h_{\text{sg}}^{\ast})  W/Km(^2)</td>
<td>94</td>
<td>(1.2 \times 10^2)</td>
</tr>
<tr>
<td>6.</td>
<td>(h_{\text{sg}}^{\ast\ast})  W/Km(^2)</td>
<td>(1.0 \times 10^2)</td>
<td>(1.3 \times 10^2)</td>
</tr>
</tbody>
</table>

Table 6.1: Row 1 to 4 describe the operating condition during each repetition of the experiment. Speed ratio is the speed ratio between the average vertical airflow and the rotor surface. Row 5 and 6 gives estimates of the convective heat transfer coefficient between the stator and the air-gap. The estimate \(h_{\text{sg}}^{\ast}\) is based on a linear temperature gradient in the air-gap. The estimate \(h_{\text{sg}}^{\ast\ast}\) is based on the temperature gradient given in section 2.4.2.

### 6.4 Turbine power coefficient

![Figure 6.3](image-url)  

*Figure 6.3:* Observed power coefficient when the turbine is operated at various tip speed ratios. Each data point corresponds to 10 minutes of operation.

The power coefficient of the VAWT prototype described in section 4.2 is presented in Fig. 6.3. The power coefficient peaks at about 30% at a tip speed

\(^1\)http://www.engineeringtoolbox.com/convective-heat-transfer-d_430.html
ratio near 3.3. The width of the peak in $C_P(\lambda)$ is such that 95% of the maximum $C_P$ is obtained when the tip speed ratio is between 3.1 and 3.8. A wider peak reduces the importance of accurate speed control.

6.5 Turbine torque ripple

![Figure 6.4: Turbine torque ripple of the three bladed H-rotor. The intensity of the lines correspond to the magnitude of the $\tilde{\tau}_{ti}$ in (2.10). (Fig. 8 in Paper VII)](image)

The torque of most wind turbines vary during the turbine revolution. This phenomenon is referred to as turbine torque ripple. Observed torque ripple of the 12kW VAWT, at various tip speed ratios, is presented in Fig. 6.4. The frequency components of the ripple are usually referred to by their relative frequency followed by a $p$, i.e. the line at $\omega/\omega_t = 3$ is referred to as the 3$p$ component of the turbine torque ripple. The most pronounced torque ripple components in Fig. 6.4 are described in Table 6.2.

It is demonstrated that turbine torque ripple of a VAWT can be estimated from the harmonic content of the rectified generator current, Fig. 6.4. The measured torque ripple shows that the 3$p$ component is much more pronounced than the 6$p$ component. This suggests that most of the energy is extracted on the windward side. The most interesting lines observed in Fig. 6.4 are listed in Table 6.2.

A lower ripple is beneficial since it reduces material stress and improves the quality of the power output. In a previous study it was established that the turbine has the highest power coefficient when the tip speed ratio is close to 3.3. At this tip speed ratio the torque ripple is relatively low compared to torque ripple at lower tip speed ratios.
Component Interpretation

3p Corresponds to blade passing on the windward side. This ripple component dominates throughout the interval.

6p Leeward and windward blade passing. The low amplitude of this component implies that the force of a blade passing on the leeward side is much smaller than the force of a blade passing the windward side.

7p The width of this peak and the lack of a 14p harmonic suggests that this is a resonance peak of the turbine rather than a component of the turbine torque.

9p Harmonic of the 3p torque variation, low amplitude.

Table 6.2: Origins of the strongest lines in Fig. 6.4.

6.6 Single turbine simulation

The common characteristic of the control strategies described in section 3.3.2 is that the generator power is determined from turbine speed alone. In Paper V the three versions of the robust control strategy are evaluated in situations with variable wind speed. The evaluation is based on a model of the electrical system described in section 3.2 coupled with a vortex model based simulation of the turbine aerodynamics. This reasonably realistic system simulation helps to avoid the design of a control system that only works for idealised systems and allows for the system efficiency to be evaluated.

A single turbine was subjected to an oscillating wind field. The oscillations were implemented as a variation of the asymptotic wind speed which is equivalent to the turbine being mounted on a vehicle that is accelerating and braking periodically. Fig. 6.5 shows the average power absorption with the three control strategy implementations described in section 3.3.2 together with a reference strategy that controls the turbine according to the asymptotic wind speed. The primary limitation of the reference strategy is that the power flow from the generator is unidirectional.

All three robust strategies work well without stopping the turbine. In Fig. 6.5a, strategy B performs better than the other strategies when the wind changes rapidly. At frequencies above 0.018Hz, the reference control strategy starts to drop in performance. The reference strategy can rapidly decrease the rotational velocity by increasing the DC load, but acceleration is limited to the turbine torque. In rapidly oscillating wind, the turbine acceleration is insufficient and the average turbine speed is too low. The large torque fluctuations with the reference strategy increase the resistive loss in the stator winding, though this effect is less significant since the power loss
Average wind speed is 5.5 m/s.

Average wind speed is 9.5 m/s.

Figure 6.5: Delivered power for oscillating wind fields, the amplitude of the oscillations is 2.5 m/s. (Fig. 9 and 10 in Paper V)

in the stator core is the dominating loss. It is therefore not surprising that the reference strategy is outperformed by the robust control strategies in rapidly changing wind.

The control strategies were also tested at a higher average wind speed and the results are found in Fig. 6.5b. With higher average wind speed, the turbine torque is higher and the effect of a too low rotational velocity is less severe. The speed of strategy B comes at the expense of reduced steady state performance far away from $\omega_b$ (Fig. 3.5). Strategy B outperforms the other strategies in high frequency wind when the average wind speed is close to the design point (6 m/s), but performs slightly worse in the case with higher average wind speed, Fig. 6.5. In the high wind speed case, all strategies perform well and the differences between the strategies are only a few percent. The lower power extraction for strategy C, as compared to strategy A, is intentional to reduce structural loads, see section 3.3.2.
6.7 Wind farm simulations

The concept outlined in section 3.2 specifies an electrical farm topology where multiple WECs connect to a mutual DC-bus. Individual turbine control is not possible with this topology since the only control parameter for the entire farm is the voltage of the mutual DC-bus. The performance impact of this reduced controllability was evaluated through simulations in Paper VI.

In a first simulation each turbine was in a separate wind field, i.e. there is no aerodynamic coupling. The asymptotic wind speed of each turbine is changed according to Fig. 6.6a, the resulting turbine speeds are shown in Fig. 6.6b. Figure 6.6 illustrates the consequence of using a single DC-bus. All turbines connected to the DC-bus tends to operate at more similar speeds than when individual control is used.

![Wind at each turbine.](image1)

![Speed of each turbine.](image2)

*Figure 6.6: Simulation with aerodynamically uncoupled turbines.*

Multiple aerodynamically coupled turbines are also considered in Paper VI. Two geometric farm layouts, line formation and square formation, are considered. In each simulation the wind direction was changed gradually, the average
delivered power at steady state operation is shown in Fig. 6.7. The geometric layout of the farm has a much greater impact on the delivered power than the choice of electrical topology, Fig. 6.7a and 6.7b.

\[ \text{Figure 6.7: Delivered power with different farm layouts and different electrical topologies. The distance between turbines } D \text{ is expressed in turbine diameters. } \text{Mut. is the mutual topology (a single DC-bus) and } \text{Sep. is the separate topology where each turbine is controlled individually.} \]
7. Conclusions

The conclusions from the papers included in this thesis are:

- Simulations predict that it is possible to control one turbine efficiently by adjusting the load according to the turbine speed. (Paper V)
- Simulations predict that it is possible to control several wind energy converters, connected to a single DC-bus, by adjusting the power extracted from the DC-bus according to the speeds of the turbines. (Paper VI)
- Individual turbine control is not possible when several turbines are controlled exclusively through extraction of power from a single DC-bus. The reduced control implies that all turbines will not be operated at the optimal tip speed ratio. However, simulations predict that the power absorption penalty is small compared to a reference case where each turbine is controlled according to strategy C in section 3.3.2. (Paper VI)
- Simulations predict that efficient control of a wind farm via a single DC-bus tends to operate turbines in weaker wind at a higher than optimal speed. The higher rotor speed increases the total power loss in the stator cores of the generators. (Paper VI)
- The power coefficient of the VAWT outlined in section 4.2 attains the maximal value of 29% when the turbine tip speed ratio is in the range 3.1 to 3.8. (Paper II)
- Core loss, at low electrical frequency, is more accurately predicted by simulations based on specific loss measured at low electrical frequency than if specific loss at low frequency is extrapolated from the specific loss at 50 Hz. (Paper IV)
- The turbine torque ripple of the VAWT described in section 4.2 has been measured. The strongest variation in the turbine torque is due to blades passing on the windward side. The turbine torque ripple of the VAWT described in section 4.2 depends on the tip speed ratio of the turbine. (Paper VII)
- A telecom adaptation of a VAWT has been demonstrated. The wind energy converter has a unique design where the generator has a very large inner radius and an outer rotor onto which the turbine is mounted directly. (Paper III)
8. Suggestions for future work

Simulations predict that the robust control strategy of Papers V and VI is well suited for control of one or more VAWT. The next step is to test the control strategy in practice on the already existing 12kW VAWT prototype.

Many aspects of the proposed systems can be studied with the coupled aerodynamic and electromechanical simulation model that is presented in Paper V and used in Paper VI. The mechanical model should be extended with additional degrees of freedom such as torsional vibrations in the shaft connecting the rotor and the turbine and the bending of the struts holding the blades. Similarly, the electrical model can be extended to study various grid aspects or other electrical topologies.
9. Summary of papers

In this chapter the included papers are summarised and the author’s contribution to each paper is stated.

Paper I

**Offshore underwater substation for wave energy converter arrays**

This paper demonstrates how diode rectifiers can be used to connect generators with different characteristics to a mutual capacitor bank and load. The paper also contains substantial background information regarding the wave energy converter research facility in Lysekil, Sweden. The author built parts of the onshore experimental setup with two generators that were connected to a single DC-bus with a single load. *Published in IET Renewable Power Generation, 4(6):602–612, 2009.*

Paper II

**Power coefficient measurement on a 12 kW straight bladed vertical axis wind turbine**

This paper presents a measurement of the power coefficient of the turbine prototype. It was determined that the maximum value of the power coefficient was 29%. The power coefficient attained its maximum value when the turbine was operated at a tip speed ratio near 3.3. The author and the first author of the paper planned and executed the experiment, analysed the data and wrote the paper. *Published in Renewable Energy, 36(11):3050–3053, 2010.*

Paper III

**Adapting a VAWT with PM generator to telecom applications**

Providing power for telecommunication equipment in remote areas can be costly. Diesel powered generators are often used; a solution that has a negative environmental impact and require fuel to be transported to the site. This paper presents a wind energy converter that has been tailored for a telecom tower, the Tower Tube, which is suitable for deployment at remote sites. The author
wrote the paper and made the accompanying poster. *Presented at European Wind Energy Conference & Exhibition, Warsaw, 2010.*

**Paper IV**

**No-load core loss prediction of PM generator at low electrical frequency**

Manufacturers of electrical steel often only provide specific loss data at grid frequency, 50Hz. Variable speed wind turbines are often operate at significantly lower electrical frequencies. In the presented study it was investigated how well power loss at low electrical frequency can be extrapolated from specific loss at grid frequency. It was further demonstrated how loss prediction is improved if actual low frequency loss data is available. The finite element method was used to simulate the generator loss and the actual loss was determined from the deceleration of the rotor at no-load. The author planned and executed the experiment, analysed the data and wrote the paper. *Published in Renewable Energy, 43:389–392, 2012.*

**Paper V**

**Robust VAWT control system evaluation by coupled aerodynamic and electrical simulations**

Three similar control strategies, where the extracted power is determined from the rotor speed alone, were evaluated in simulations. The simulation model consisted of an aerodynamic model that was coupled with a model of the electromechanical system. Simulations predict that the power absorption with the evaluated strategies is similar to, and sometimes better than, a reference strategy where the rotor speed is controlled according to asymptotic wind velocity. Further, the increase of resistive power loss in the stator winding due to passive rectification was estimated. The author was solely responsible for the electromechanical part of the simulation while the first author of the paper was solely responsible for the modelling of the aerodynamics. *To appear in Renewable Energy, DOI:10.1016/j.renene.2013.03.038.*

**Paper VI**

**Aerodynamic and electrical evaluation of a VAWT farm control system with passive rectifiers and mutual DC-bus**

This paper is a continuation of Paper V. One of the control strategies presented in Paper V was applied in simulations of a wind farm of four turbines. Here, an electrical topology where all turbines were controlled via a mutual DC-bus was compared to a reference topology where each turbine was con-
trolled individually. The choice of electrical topology only had a small impact on the energy delivered by the farm. The author significantly contributed to every part of the paper except the aerodynamic code and was solely responsible for the model of the electromechanical system. *Conditionally accepted for publication in* Renewable Energy, 2013.

**Paper VII**

**Torque ripple of a straight-bladed Darrieus turbine measured using armature currents of direct driven PM generator**

Torque ripple measured on the 12kW prototype is presented. The torque ripple was not measured directly, instead it was estimated from the harmonic content of the rectified current from the generator. It is suggested that indirect estimation of torque ripple via current ripple is suitable for monitoring of deployed turbines. Possible applications of ripple monitoring are discussed. The author planned the experiment, built parts of the experimental setup, analysed the data and wrote the paper. *Submitted to Renewable Energy, 2013.*

**Paper VIII**

**Stator cooling by axial flow through air-gap of high torque PM generator**

The convective heat transfer coefficient between the air-gap and the stator was measured on a 12kW PM generator. It is argued that the air-gap cooling is more suitable for direct drive PM generators than for conventional machines. The author planned the experiment, analysed the data and wrote the paper. The author built the experimental setup and performed the measurements together with the second author of the paper. *Submitted to Renewable Energy, 2013.*

**Paper IX**

**Laboratory verification of system for grid connection of a 12 kW variable speed wind turbine with a permanent magnet synchronous generator**

A first test of an inverter, which allows for variable input voltage and utilise a tap-transformer, is presented here. The IGBT based inverter used pulse width modulation to produce sinusoidal output currents. The high frequency harmonics, caused by switching, were reduced with an LCL-filter. The tap-transformer has an internal inductance which contributes to the filter. Changing the transformer ratio changes the impedance of the transformer. The paper illustrates how tap changing affects the filter characteristics. The author mainly contributed to the theoretical aspects of the filter design. *Presented at EWEA Annual Event, Copenhagen, 2012.*
9.1 Errata to papers

- Paper IV, the relative differential permeability $\mu_d = 1.03$ should have been given in Table 2.
- Paper IV, the remanence $B_r = 1.32\,\text{T}$ should have been given in Table 2.
10. Acknowledgements

Ett stort tack till mina vänner och kollegor som gjort tiden här på avdelningen så bra som den varit: Anders, Boel, Deepak, Elísabet, Emilia, Erland Jon, Ling, Linnea, Mattias, Mikael, Mårten, Olle, Olov, Senad, Staffan, Stefan, Rafael, Remya, Wei och Yue.

VAWT-entusiasterna: Anders, Eduard, Hans, Jon, Jon, Marcus, Mikael, Per, Sandra, Senad och Stefan.

Sushi-fixarna: Stefan, Linnea och Mikael.

Jag vill också rikta ett särskilt tack till Hans och Mats för att ni vågar mena allvar med vertikalaxlad vindkraft.

Slutligen ett stort tack till finansiärerna som gjort den här avhandlingen möjlig. Finansieringen av mitt doktorandprojekt kom från Energimyndigheten (Projektnr. 20883-2) samt från Centrum för förnybar elenergiomvandling, som finansieras av Energimyndigheten, VINNOVA, Statkraft och Uppsala Universitet.
11. Sammanfattning på svenska

Det finns mycket som talar för att vertikalaxlade vindkraftverk kan bli kostnadseffektivare än de idag dominerande horisontalaxlade vindkraftverken. De potentiella fördelarna ligger främst i att dyra och underhållskrävande mekaniska komponenter, så som växellåda samt system för gironing av turbin samt vridning av turbinbladen, helt kan undvikas. Tabell 11.1 visar hur stor andel av det totala produktionsbortfallet som orsakats av olika delsystem.

<table>
<thead>
<tr>
<th>Komponent</th>
<th>(%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Växellåda</td>
<td>19,4</td>
</tr>
<tr>
<td>2. Girning av turbinen</td>
<td>13,3</td>
</tr>
<tr>
<td>3. Vinkling av blad</td>
<td>9,4</td>
</tr>
<tr>
<td>4. Kontrol system</td>
<td>18,3</td>
</tr>
<tr>
<td>5. Elekiska system</td>
<td>14,3</td>
</tr>
<tr>
<td>6. Generator</td>
<td>8,9</td>
</tr>
<tr>
<td>7. Sensorer</td>
<td>5,4</td>
</tr>
<tr>
<td>8. Hydralik</td>
<td>4,4</td>
</tr>
<tr>
<td>9. Drivlina</td>
<td>2,4</td>
</tr>
<tr>
<td>10. Hela enheten</td>
<td>1,7</td>
</tr>
<tr>
<td>11. Mekanisk brom</td>
<td>1,2</td>
</tr>
<tr>
<td>12. Övrigt</td>
<td>1,2</td>
</tr>
</tbody>
</table>


Ett av avhandlingens huvudresultat är att den aerodynamiska effektfaktorn hos en vertikalaxlad turbinprototyp har uppmätt. Mätningen visar också vilket löptal, eller hastighet relativt vinden, turbinen skall arbeta med för att uppnå maximal verkningsgrad. Det visade sig att den maximala verkningsgraden är 29% och att detta uppnås då turbinbladen rör sig med löptal 3,3, d.v.s. bladen rör sig 3,3 ggr snabbare än vindhastigheten.

I avhandlingen studeras tre styrstrategier vilka syftar till att styra turbinen till rätt löptal. Samtliga strategier bygger på att man på förhand tabulerat vil-


![Figur 11.1: Hur vridmomentet pulserar vid olika löptal. Normerad frekvens är kvoten mellan frekvensen med vilken vridmomentet pulserar och frekvensen med vilken turbinen snurrar.](image)

Oftast är den elektriska frekvensen i elektriska maskiner densamma som nätets frekvens (50Hz i Europa), därför tillhandahåller tillverkare av elektrisk plåt ofta bara förlustsiffror vid just denna frekvens. För att en turbin med fixa blad skall fungera bra är det viktigt att den körs med rätt hastighet relativt vinden och den elektriska frekvensen kommer att variera med vindhastigheten. För att undersöka hur väl det går att extrapolera förlustdata till låga frekvenser så gjordes ett test. I testet mättes förlusterna i en generators statorplåt genom ett mekaniskt inbromsningsförlopp. De uppmätta förlusterna jämfördes sedan med förluster som beräknats med hjälp av förluster vid 50Hz samt förluster som beräknats med hjälp av förluster vid lägre frekvens. I det här fallet visade
det sig att man underskattar förlusterna med närmare 50% om man extrapolar i frekvens.

Figur 11.2: Experimentupptäckningar.


Den experimentella verksamhet som ligger till grund för avhandlingen har i huvudsak utförts på en prototyp av ett vertikalaxlat vindkraftverk (se Figur 11.2a) samt en generatorupptäckning (se Figur 11.2b).
Bibliography


A doctoral dissertation from the Faculty of Science and Technology, Uppsala University, is usually a summary of a number of papers. A few copies of the complete dissertation are kept at major Swedish research libraries, while the summary alone is distributed internationally through the series Digital Comprehensive Summaries of Uppsala Dissertations from the Faculty of Science and Technology.