Citation for published item:

Further information on publisher’s website:
https://doi.org/10.1049/iet-pel.2016.0802

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Impact of wind conditions on thermal loading of PMSG wind turbine power converters

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Abstract: Power converter reliability is critical for permanent magnet synchronous generator (PMSG) wind turbines. Converter failures are linked to power module thermal loading but studies often neglect turbine dynamics, control and the impact of wind speed sampling rate on lifetime estimation. This study addresses this using a 2 MW direct-drive PMSG wind turbine model with a two-level converter, and simulating junction temperatures ($T_j$) using a power module thermal equivalent circuit under various synthetic wind speed conditions. These synthetic wind conditions include constant and square wave profiles representing stable and gusty wind conditions. Responses to square wave wind speeds showed that the lower the gust frequency, the higher $\Delta T_j$ becomes, demonstrating that low turbulence sites have greater thermal variation in the converter. In contrast, wind speed variations with frequencies >0.25 Hz deliver only small increases in $\Delta T_j$. It is concluded that reasonable approximations of $T_j$ profiles can be made with 0.25 Hz wind speed data, but that lower data rate wind measurements miss essential, damaging characteristics.

1 Introduction

To meet EU renewable energy targets for 2020 and beyond, the levelised cost of energy (LCoE) of offshore wind must be reduced to below £100/MWh [1]. Operation and maintenance (O&M) accounts for ∼30% of the LCoE [2]. A key aspect of O&M is turbine sub-system reliability. By understanding which components have the greatest impact on downtime and power production, O&M resources can be focused to minimise turbine disruption and reduce the LCoE of offshore wind.

1.1 Wind turbine power converter reliability

Numerous studies have explored the reliability of wind turbine sub-systems using operational data. Carroll et al. [3] examined a large dataset for offshore wind turbines with mixed turbine technology to determine the main causes of failure and concluded that power converters had a typical failure rate of ∼0.2 failures/turbine/year, much higher than the highest failure rate of >1 failure/turbine/year for pitch systems. However, a more focused study on turbine type [4] found that the failure rate of fully-rated converters (FRC) in permanent magnet synchronous generator (PMSG) turbines was 0.593 failures/turbine/year compared with 0.106 failures/turbine/year for partially-rated converters in doubly fed induction generator turbines. This suggests that the unique operating conditions of PMSG-FRCs are causing higher failure rates. Furthermore, Spring et al. [5] examined large wind turbine datasets and used expert knowledge to determine the impact of component failure to turbine downtime, compiling a top 30 list of failure sources. It was concluded that power converters were the highest source of turbine downtime, with their failure modes occupying the top 15 positions. Converter reliability must, therefore, be examined with a focus on the FRC in PMSG turbines.

Of the failures outlined in [4], power module failure is the failure mode for nearly all major converter repairs. Traditionally, power module failure has been linked to power module thermal loading, where the variation of temperature in the insulated gate bipolar transistors (IGBT) and diode cases causes fatigue through power module failure has been linked to power module thermal loading. However, a more focused study on turbine type [6] has shown that power converter reliability is critical for permanent magnet synchronous generator (PMSG) wind turbines. Converter failures are linked to power module thermal loading but studies often neglect turbine dynamics, control and the impact of wind speed sampling rate on lifetime estimation. This study addresses this using a 2 MW direct-drive PMSG wind turbine model with a two-level converter, and simulating junction temperatures ($T_j$) using a power module thermal equivalent circuit under various synthetic wind speed conditions. These synthetic wind conditions include constant and square wave profiles representing stable and gusty wind conditions. Responses to square wave wind speeds showed that the lower the gust frequency, the higher $\Delta T_j$ becomes, demonstrating that low turbulence sites have greater thermal variation in the converter. In contrast, wind speed variations with frequencies >0.25 Hz deliver only small increases in $\Delta T_j$. It is concluded that reasonable approximations of $T_j$ profiles can be made with 0.25 Hz wind speed data, but that lower data rate wind measurements miss essential, damaging characteristics.

1.2 Power converter reliability studies

This approach has been applied in a number of studies to explore the expected reliability of power converters in wind turbines [7–16]. However, these studies often have limitations. Some studies neglect the impact of wind turbine dynamics and control, so wind speed inputs are directly converted into a $T_j$ [7–9], which will deviate significantly from the true $T_j$ profile in the converter. The use of wind speed distributions [8, 10] and large time steps, e.g. 3-hourly [11] neglects the impact of wind speed history, which has been shown to have a large impact on the current loading, and subsequently, the thermal loading of the converter [17]. For example, the use of supervisory control and advisory data acquisition (SCADA) data may only provide a mean and maximum wind speed over a 10 min period, which may hide a large amount of variation that is causing damage to the converter.

Some studies have included both realistic wind speed profiles and drive train models [12–15]. However, only two have studied a PMSG wind turbine [14, 15], and these two studies disagree whether high-frequency wind speed events impact the thermal loading of the converter significantly. There is therefore a need for a detailed study into the impact of operating conditions on power converter reliability to help inform how the turbine should be operated in order to extend its life and reduce the LCoE.

1.3 Research contributions

These limitations mean that operational profiles and failure data may not be representative of converter operation in the field, and there is no consensus on the required wind speed data frequency for accurate thermal simulation. This paper addresses these limitations by identifying the temporal fidelity over which wind events (such as gusts) cause the highest thermal variation. The minimum wind speed sampling frequency that will still provide accurate thermal profile simulation can then be determined. The impact of modelling assumptions on the estimated $T_j$ profiles is also explored. These results will provide guidance for future simulation and experimental studies to improve converter reliability analysis accuracy, with an aim of improving best practice in both academia and industry.

To simulate these thermal loading profiles, a drive train model (Section 2.1), power loss model (Section 2.2), thermal model (Section 2.3), and wind inputs (Section 2.4) are required. The...
impacts of temporal fidelity and modelling assumptions were explored through an analysis of individual \( T_j \) cycles (Section 3.1), comparison of constant wind speed results with a comparable study (Sections 3.2 and 3.3) and analysis of the converter response to synthetic wind speed time series (WSTS) (Section 3.4). A summary of the main findings is given in Section 4.

2 Approach

First, an approach to identifying the thermal loading of a wind turbine power converter was developed as follows:

- Modelling of a wind turbine drive train to provide the current throughput of the converter.
- Modelling of the resultant power losses in the converter due to the current throughput.
- Modelling the power module thermal processes in response to the power losses.
- Simulation of power module thermal response to selected wind speed inputs.

Fig. 2 illustrates the full-system model. \( U \) is the incoming wind speed, \( I_{sw} \) is the converter switch current, \( V_{DC} \) is the DC link voltage, and \( P_{loss} \) is the power module device switching losses.

This section outlines the details of each of the sub-systems.

2.1 Drive train model

The drive train model developed for this work was a 2 MW FRC-PMSG, direct-drive turbine. The model was split into five sub-systems: rotor power extraction, drive train dynamics, generator, machine-side converter (MSC), and turbine control. Fig. 3 provides a summary of the drive train model. \( T_t \) is the turbine torque extracted from the wind, \( T_m \) is the mechanical torque resulting from the shaft stiffness and damping, \( T_g \) is the electromagnetic torque, \( \omega_t \) is the turbine rotational speed, \( \omega_g \) is the generator rotational speed, \( \beta \) is the pitch angle, \( \beta_{ref} \) is the reference pitch angle, \( I_{abc} \) is the generator output current, \( V_{t,abc} \) is the generator terminal voltage applied by the MSC, and \( V_{ref,abc} \) is the reference MSC output voltages.

This section outlines the core aspects of each sub-system. Detailed descriptions can be found in [17, 18].

2.1.1 Turbine power extraction: \( T_t \) from the wind is calculated using the following equation:

\[
T_t = \frac{0.5C_p\rho\pi r^2 U^3}{\omega_t} \tag{1}
\]

\( C_p \) is the power coefficient, \( \rho \) is air density, and \( r \) is the turbine radius. \( C_p \) depends on the tip speed ratio (\( \lambda \)) and \( \beta \). The \( C_p, \lambda, \) and \( \beta \) relationship is turbine specific but it is typical to use a numerical approximation (2) and (3) [19], with \( \lambda \) calculated using (4)

\[
\frac{1}{\lambda_i} = \frac{1}{\lambda + H\beta} - \frac{K_1}{\beta^2 + 1} \tag{2}
\]

\[
C_p = A_i \left( \frac{B_i}{\lambda_i} - C_i \beta - D_i \beta^2 - F_i \right) e^{-G_i / \lambda_i} \tag{3}
\]

\[
\lambda = \frac{\alpha r}{U} \tag{4}
\]

\( A_i \) to \( F_i \) and \( K_1 \) are turbine specific constants. The values used can be found in the Appendix.

2.1.2 Drive train dynamics: \( T_t \) is applied to the drive shaft. The drive train can be modelled as a mechanical mass-spring-damper system which dynamically impacts the \( T_m \) applied to the generator.
The drive train was modelled as a two-mass system, rather than a lumped-mass system, to include the dynamic effects of shaft stiffness and damping. The two-mass system is defined by the following equation [20]:

\[
\begin{bmatrix} J_t & 0 \\ 0 & J_g \end{bmatrix}\begin{bmatrix} \omega_t \\ \omega_g \end{bmatrix} + \begin{bmatrix} C_d & 0 \\ 0 & C_d \end{bmatrix}\begin{bmatrix} \omega_t \\ \omega_g \end{bmatrix} + \begin{bmatrix} K & -K \\ -K & K \end{bmatrix}\begin{bmatrix} \theta_t \\ \theta_g \end{bmatrix} = \begin{bmatrix} T_t \\ T_g \end{bmatrix}
\]

(5)

\(J_t, J_g\) are the moments of inertia of the turbine and generator, respectively, \(\theta_t, \theta_g\) are the rotational displacements of the turbine and generator, respectively, \(C_d\) is the shaft damping coefficient, \(K\) is the shaft stiffness, and \(\alpha_t, \alpha_g\) are the rotational accelerations of the turbine and generator, respectively. The expanded matrix can be solved numerically, with \(T_m\) calculated using the following equation:

\[
T_m = (\omega_t - \omega_g)C_d + (\alpha_t - \alpha_g)K
\]

(6)

2.1.3 Generator: The generator model used is a second-order non-salient PMSG in the \(dq0\) reference frame [21] with a current rating of 1868 A rms. The mechanical component was modelled with the torque swing equation. The generator parameters can be found in the Appendix.

2.1.4 MSC: In a typical wind turbine, the converter is comprised of an MSC and grid-side converter (GSC). The role of the MSC and GSC differs depending on control strategy but the MSC typically controls the speed of the wind turbine for optimum power production whilst the GSC controls power export to maintain the DC-link voltage.

Due to the turbine's variable speed operation for maximum power extraction, the MSC experiences a more varied operating profile compared with the GSC, which operates at fixed frequency. The MSC is consequently of greater interest for reliability analysis. Here, only the MSC is modelled while the GSC is replaced with a constant voltage source of 1150 V DC (± 575 V DC).

The MSC parameters were based on the power modules found in the SEMIKRON SKSB2100GD69/11-MAPB stacks [22]. These stacks have a maximum DC voltage of 1200 V and a maximum current of 1000 A rms. The stacks use SKiiP2013GB172-4DWV3 half-bridge integrated power modules [23].

The voltage output of the MSC is determined by \(V_{abc,\text{ref}}\) from the machine-side controller (Section 2.1.5). Pulse width modulation (PWM) converts the modulated \(V_{abc,\text{ref}}\) (\(V_m\)) into a switching pattern for the IGBTs in order to produce the three-phase converter output voltage (\(V_{c,abc}\)). Space vector PWM was chosen and therefore \(V_m\) was calculated using the following equation:

\[
V_m = \frac{\sqrt{3}}{\sqrt{V_{DC}}} V_{abc,\text{ref}}
\]

(7)

The current through the devices is split between diode and IGBT depending on the current polarity. Since two parallel stacks are required to reach the current rating of the turbine (Section 2.1.3), this current is split equally between stacks [24].

2.1.5 Turbine control: Power extraction is controlled in two ways depending on operating region. Maximum power point tracking (MPPT) is used for below rated speed, while active pitch control is used above rated speed to limit power.
For MPPT, $C_p$ must be maximised ($C_{p,max}$). By controlling $\omega_t$, the optimum $\lambda$ can be maintained ($\lambda_{opt}$) when below rated wind speed:

$$\omega_{t,\text{opt}} = \frac{U_{\lambda_{opt}}}{r}$$

(8)

$\omega_{t,\text{opt}}$ is the optimum turbine rotational speed at a given wind speed.

As $U$ is not measured in this control strategy, $\omega_{t,\text{opt}}$ is unknown. Instead $\omega_t$ is varied until the turbine reaches steady state, which occurs when $\omega_t = \omega_{t,\text{opt}}$. $\omega_t$ is varied via $T_g$, which is carried out using direct-quadrature-zero current ($I_{dq0}$) control applied to the MSC. Fig. 4a illustrates the machine-side control algorithm.

$I_d$ is maintained at 0 A [25]. These currents are achieved by applying a controlled voltage on the generator terminals using the MSC.

To determine the required generator currents, $\omega_t$ is related to the required $T_g$ ($T_{ref}$) via the turbine power curve using a turbine specific constant $K_{MPPT}$ (9) and (10). The reference $I_q$ is calculated using a known relationship between $I_q$ and $T_g$ in the generator (11). $I_d$ is maintained at 0 A [25]. These currents are achieved by applying a controlled voltage on the generator terminals using the MSC.

$$T_{ref} = K_{MPPT} \omega_t^2$$

(9)

---

Fig. 4 Schematics of
(a) Machine side controller, (b) Pitch controller, (c) Pitch actuator

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To produce a reference energy loss is given by the conditions at the first low-high (L-H) switching instance. The energy is modulated over the switching cycle (between L-H and the next L-H) to provide a constant switching power loss. This was deemed acceptable as the device thermal time constants (μs-ms) will dominate the thermal profile [29].

With the above assumptions, the switching/RR losses were found by

(i) Determining the energy losses at L-H switching events. The switching/RR energy loss is given as a function of input current at two reference $V_{DC}$ [23] and is assumed to be linear. The IGBT energy loss is $E_{on-off}$, whilst the diode energy loss is twice the reverse energy loss ($E_{rr}$).

(ii) Calculating the equivalent modulated power losses over the switching cycle using the following equations [30]:

\[
P_{sw}(t; T_{sw}, \{1 + T_{p, sw}(t)\}) = \frac{E_{on-off}(t)}{p_{sw}(t)}
\]

\[
P_a(t; T_{sw}, \{1 + T_{p, a}(t)\}) = \frac{E_{a}(t)}{p_{a}(t)}
\]

\[
P_{sw,T}(t) = \left(1 + T_{C_{sw}}(t)_{IGBT}(t) - T_{ref}\right) P_{sw}(t)
\]

\[
P_{a,T}(t) = \left(1 + T_{C_{a}}(t)_{diode}(t) - T_{ref}\right) P_{a}(t)
\]

2.2 Converter power loss model

To convert the current throughput into $T_j$ profiles, power losses must be calculated, specifically:

- The IGBT and diode conduction losses.
- The IGBT switching losses and diode reverse recovery (RR) losses.

The conduction and switching losses are summed for each device. The power loss model used is based on [29, 30].

2.2.1 Conduction losses: Conduction losses depend on device internal resistance so are calculated using the device voltage and current

\[
P_{C,IGBT} = V_{ce} I_c
\]

\[
P_{C,diode} = V_I I_I
\]

$P_{C,IGBT}$ and $P_{C,diode}$ are the IGBT and diode conduction losses, respectively, $V_{ce}$ is the IGBT collector–emitter voltage, $V_I$ is the diode forward voltage, $I_c$ is the IGBT collector current, and $I_I$ is the diode forward current.

$I_c$ and $I_I$ are the input currents, $V_{ce}$ and $V_I$ are functions of $I_c$ and $I_I$, respectively, and the device $T_j$. The functions are given in the manufacturer's data sheet [23] for a $T_j$ of 25 and 125°C. $V_{ce}$ and $V_I$ are calculated by interpolating between the values given at these reference temperatures.

2.2.2 Switching/RR losses: Switching and RR losses occur when there is a change in direction of voltage and current. Device response is not instantaneous but occurs over nanoseconds [29]. Nanosecond simulation is impractical for run times longer than a few seconds and the energy loss information given in manufacturer’s datasheets is not detailed enough for accurate temporal loss simulation. For example, the energy loss during switch on ($E_{on}$) and switch off ($E_{off}$) are not given separately, but reported as a summation of the two ($E_{on-off}$) [23]. As such, a simplified approach has to be taken.

It has been assumed that the energy loss is given by the conditions at the first low-high (L-H) switching instance. The energy is modulated over the switching cycle (between L-H and the next L-H) to provide a constant switching power loss. This was deemed acceptable as the device thermal time constants (μs-ms) will dominate the thermal profile [29].

With the above assumptions, the switching/RR losses were found by

(i) Determining the energy losses at L-H switching events. The switching/RR energy loss is given as a function of input current at two reference $V_{DC}$ [23] and is assumed to be linear. The IGBT energy loss is $E_{on-off}$, whilst the diode energy loss is twice the reverse energy loss ($E_{rr}$).

(ii) Calculating the equivalent modulated power losses over the switching cycle using the following equations [30]:

As the RC network data was readily available, a common practice approach of thermal equivalent circuit modelling was used. The data given in [23] is for a Foster RC network. To provide a more accurate half-bridge temperature profile the Foster thermal resistance ($R_{th,t}$) and time constant ($\tau$) parameters were converted into RC parameters [31], and then converted into Cauer RC parameters to provide a more realistic thermal profile throughout the device [32]. Fig. 5 gives the half-bridge Cauer RC network. The parameters are available in the Appendix.

The power losses ($P_{th}$) are dependent on $T_j$ and therefore the power loss and thermal sub-systems must be run concurrently. Due to the power-thermal inter-dependency, the initialisation of $C_{th}$ temperatures was solved iteratively. The steady-state temperatures are related to the $R_{th}$ only [30]. Therefore, initial temperatures were
3 Results and discussion

This section outlines the analysis performed on $T_j$ profiles produced in response to constant and square wave WSTS.

3.1 Individual $T_j$ cycles

The power loss and temperature profiles over individual power cycles were examined. Fig. 6 compares the diode and IGBT current $T_j$ profiles at a constant 12.7 m/s. The $T_j$ profile for the diode (Fig. 6d) is comparable with the expected response found in Fig. 5.2.13 in the manufacturer's handbook [30], although it should be noted that the manufacturer's data considers only the average power loss of the switching cycle, rather than individual switching events. This simulation output can show higher frequency temperature variation than revealed in the manufacturer's data.

This higher frequency temperature variation becomes most apparent in the IGBT (Fig. 6c), particularly in the first half of the current cycle. This can be attributed to the converter being connected to a PMSG. The PMSG reactance causes the current to be out-of-phase with the voltage, and therefore the switching cycles are not distributed symmetrically over the input current, with a period of low voltage (Fig. 6a). The low voltage means the IGBT has a smaller duty cycle. Where there is infrequent current due to a low duty cycle (Fig. 6a), $T_j$ varies more (Fig. 6c). This effect is not seen in the diode as the current not flowing through the IGBT must pass through the diode, creating a near continuous current throughput (Fig. 6b) and therefore a smooth $T_j$ response (Fig. 6d).

3.2 Constant wind speeds

Fig. 7 shows the thermal response to constant WSTS for an IGBT and diode with an ambient temperature ($T_r$) of 40°C [8]. The mean $T_j$ increases non-linearly as wind speed increases due to the cubic relationship between wind speed and power. The $\Delta T_j$ also increases due to the higher power loss per cycle, and the $\Delta T_j$ frequency increases due to the higher generator rotational speed.

The diode temperatures $T_j$ and $\Delta T_j$ (Fig. 7b) are consistently higher than the IGBT (Fig. 7a). This is due to the higher power losses experienced by the diodes and the higher $R_{th}$ of the diodes. The higher diode $R_{th}$ (K/W) means that for every watt of heat loss, the diode experiences a greater rise in temperature than the IGBT. This is then coupled with the greater power losses due to the more continuous current flow through the diode (Section 3.1), causing the higher $T_j$ and $\Delta T_j$. This was also found for the MSC devices in [14] and suggests that the diode is more vulnerable to thermal cycling, with both higher mean $T_j$ and $\Delta T_j$.

3.3 Study comparison

The results in Figs. 7a and b were compared with the 1.55 MW turbine in [14] (Fig. 7c); it is assumed the scaled power ratings would have limited impact on the thermal loading as the converter rating would also be scaled, leading to comparable $T_j$ profiles for a given wind speed. However, whilst it was found that the $\Delta T_j$ for both IGBTs and diodes was comparable at a given wind speed, the mean $T_j$ of a given wind speed was higher in this study than in [14], despite $T_r$ being 10°C lower. This is in part due to the lack of $R_{th}$ value for the heatsink in [14]. At steady-state conditions this will create a higher case temperature ($T_c$) and therefore higher mean $T_j$. The mean $T_j$ change from 12 to 8.5 m/s is also much lower in [14]. This suggests that the MSC in this work is more susceptible to $T_j$ rises due to the higher $R_{th}$ values in the devices.

This highlights three key conclusions that must be made.

- The $\Delta T_j$ magnitude for IGBT and diodes is consistent with those found in [14], but there is greater variation in mean $T_j$ between wind speeds in this study.
The value of $T_a$ can have a large impact on the mean $T_j$ value. The inclusion of the heatsink thermal parameters in the model causes a significant increase in the mean $T_j$.

### 3.4 Response to varying wind speed input

The $T_j$ response of the power module to a range of square wave WSTS is detailed to understand what might be masked by using SCADA data in lifetime/temperature swing calculations. For comparison, the maximum $T_j$ swing over the simulation period ($\max \Delta T_j$) have been plotted for square gust amplitudes of 1 and 2 m/s, for varying frequencies and mean wind speeds, for both IGBT and diode (Fig. 8).

In general, the higher the frequency of wind speed variation, the lower $\Delta T_j$ becomes. This is as the turbine inertia acts as a low-pass filter, restricting the high-frequency wind speed variation being transmitted as current variation. Indeed, wind speed variations with frequency $>0.25$ Hz lead to a minimal increase in $\Delta T_j$ compared with the constant wind speed case (0 Hz). Therefore, reasonable approximations of $T_j$ profiles can be made (within 1°C) with 0.25 Hz wind speed data. Furthermore, these results imply that lower turbulence wind farm sites, such as offshore, have more damaging thermal profiles in the converter than higher turbulence onshore sites.

There are exceptions to this trend. $\Delta T_j$ becomes relatively consistent below 0.03 Hz. This is because the turbine has time to respond to the change of wind speed and reaches its steady operating state. The turbine is then at this steady-state condition long enough for $T_j$ to reach its maximum before the wind speed reduces. Lower frequencies will increase the number of times that the maximum $T_j$ is reached during a particular gust, but will not affect the maximum $T_j$. The same will also be true for the minimum $T_j$. Therefore, gust frequencies of 0.03 Hz and below provides the maximum $\Delta T_j$.

These results show that the use of one wind speed data point for a long time period, e.g. 10 min SCADA data, or the use of a wind speed distribution in [8, 10], can mask a large amount of information and will underestimate the $T_j$ variation significantly; in Fig. 8d the diode $\Delta T_j$ at $U_m$ of 12 m/s increases by up to 71%. Therefore, these results agree with the conclusions in [15] that higher frequency wind speed data is required for accurate $T_j$ profile estimation, and it is suggested that a minimum WSTS frequency of 0.25 Hz is required, though it is recognised that this will not always be available/practical. This, however, would reduce the amount of data required for studies such as in [12–15]. The results at higher $U_m$ also indicate that the unique operating conditions may have a significant effect on the $T_j$ profile experienced by the power converter, and therefore the lack of drive train dynamic modelling in [7–9] will change the $T_j$ profiles significantly.

### 4 Conclusions

The power converter is reliability critical for FRC-PMSG wind turbines. Converter failures are typically linked to the thermal loading of the power module. This paper models the converter thermal loading when the turbine is subjected to various synthetic WSTS to explore and demonstrate the impact of the frequency of wind speed variation on power converter thermal loading.

The thermal simulation has three main parts: a PMSG drive train model, a converter power loss model based on conduction and switching/RR, and a thermal equivalent circuit model. Both constant and square wave WSTS were tested to replicate real wind characteristics. From the results, it can be concluded that:

**Fig. 6** Cycle view of 
(a) IGBT current with the current waveform peak magnified, (b) Diode current with the current waveform peak magnified, (c) IGBT $T_j$, (d) Diode $T_j$ at a constant 12.7 m/s
• At high wind speeds the switching pattern of the IGBT causes intermittent $T_j$ profiles.

• The diodes experience greater thermal loading than the IGBTs in all comparative cases.

• A comparison with another study showed that the inclusion of heat sink thermal parameters and ambient temperature are important for providing accurate $T_j$ profiles.

• The lower the frequency of wind speed variation, the higher $\Delta T_j$ becomes, implying that low turbulence sites such as offshore have greater thermal variation, and therefore damage, in the converter.

• For the first time, the minimum wind speed data frequency for accurate converter thermal simulation has been determined. Wind speed variations with frequency >0.25 Hz have a small increase in $\Delta T_j$ and therefore reasonable approximations of $T_j$ profiles can be made with 0.25 Hz wind speed data. Wind speed data at lower frequencies allow simulations to overlook damaging temperature variations.

5 Acknowledgments

This work was supported by the EPSRC SUPERGEN Wind project (grant number EP/L014106/1).
Fig. 8 Max $\Delta T_j$ over varying mean wind speeds ($U_m$) and square gust frequencies for (a) IGBT for $U_m$ of 1 m/s, (b) IGBT for $U_m$ of 2 m/s, (c) Diode for $U_m$ of 1 m/s, (d) Diode for $U_m$ of 2 m/s.

References


[8] Fuchs, F., Mertens, A.: ‘Steady state lifetime estimation of the power IGBT for ΔTj and square gust frequencies for varying mean wind speeds ($U_m$) and square gust frequencies’. EPE, Birmingham, UK, August 2011


Appendix

\[ P_{\text{rat}} \] is the rated turbine power, \( f_{\text{rat}} \) is the rated frequency, \( U_{\text{rat}} \) is the rated wind speed, \( V_{\text{rat}} \) is the rated line voltage, \( I_{\text{rat}} \) is the rated current, \( T_{\text{rat}} \) is the rated torque, \( V_f \) is the IGBT forward voltage, \( V_{fd} \) is the diode forward voltage, \( T_{f,t} \) is the IGBT fall time and tail time, respectively, \( R_{\text{on}} \) is the IGBT on-state slope resistance, \( P_{p,iq} \) are the proportional gains for the pitch, \( I_{p,iq} \) and \( I_{q} \) controllers, respectively, \( I_{p,iq} \) are the integral gains for the pitch, \( I_{p,iq} \) and \( I_{q} \) controllers, respectively, and \( f_{\text{sw}} \) is the switching frequency (see Table 1).
### Table 1a  Continued

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<td>$\omega_{rat}$</td>
<td>22.5 rpm</td>
<td>[21]</td>
</tr>
<tr>
<td>$f_{rat}$</td>
<td>9.75 Hz</td>
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</tr>
<tr>
<td>$\lambda_{opt}$</td>
<td>6.3</td>
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</tr>
<tr>
<td>$C_{p,max}$</td>
<td>0.438</td>
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</tr>
<tr>
<td>$U_{rat}$</td>
<td>12.7 m/s</td>
<td>—</td>
</tr>
<tr>
<td>$r$</td>
<td>34 m</td>
<td>—</td>
</tr>
<tr>
<td>$\rho$</td>
<td>1.225 kg/m$^3$</td>
<td>[25]</td>
</tr>
<tr>
<td>$J_t$</td>
<td>$2.92 \times 10^8$ kg/m$^2$</td>
<td>[34]</td>
</tr>
<tr>
<td>$J_g$</td>
<td>200 kg/m$^2$</td>
<td>[35]</td>
</tr>
<tr>
<td>$K$</td>
<td>$4.0 \times 10^7$ Nm/rad</td>
<td>[34]</td>
</tr>
<tr>
<td>$C_d$</td>
<td>$6.72 \times 10^5$ Nms/rad</td>
<td>—</td>
</tr>
<tr>
<td>$V_{f,\text{rat}}$</td>
<td>690 $V_{rms}$</td>
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</tr>
<tr>
<td>$I_{\text{rat}}$</td>
<td>1867.76 $A_{rms}$</td>
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</tr>
<tr>
<td>$T_{\text{rat}}$</td>
<td>848.826 kNm</td>
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</tr>
<tr>
<td>$R_s$</td>
<td>$8.21 \times 10^{-4}$ $\Omega$</td>
<td>[23]</td>
</tr>
<tr>
<td>$L_d$</td>
<td>1.5731 mH</td>
<td>[23]</td>
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<tr>
<td>$\rho$</td>
<td>52</td>
<td>[23]</td>
</tr>
<tr>
<td>$\phi$</td>
<td>8.24 Vs (peak)</td>
<td>[23]</td>
</tr>
<tr>
<td>$V_t$</td>
<td>0.95 V</td>
<td>[23]</td>
</tr>
<tr>
<td>$V_d$</td>
<td>1.9 V</td>
<td>[23]</td>
</tr>
<tr>
<td>$R_{on}$</td>
<td>0.925 m$\Omega$</td>
<td>[23]</td>
</tr>
<tr>
<td>$V_{DC}$</td>
<td>1150 V</td>
<td>—</td>
</tr>
<tr>
<td>$\beta_{\text{max}}$</td>
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<td>[36]</td>
</tr>
<tr>
<td>$\beta_{\text{min}}$</td>
<td>0$^\circ$</td>
<td>[36]</td>
</tr>
<tr>
<td>$\beta_{\text{rate,max}}$</td>
<td>8$^\circ$/s</td>
<td>[36]</td>
</tr>
<tr>
<td>$\beta_{\text{rate,min}}$</td>
<td>$-8^\circ$/s</td>
<td>[36]</td>
</tr>
<tr>
<td>$r$</td>
<td>0.5 s</td>
<td>[37]</td>
</tr>
<tr>
<td>$P_p$</td>
<td>3.357</td>
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</tr>
<tr>
<td>$I_p$</td>
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### Table 1b  Drive train parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{id}$</td>
<td>$-0.148$</td>
<td>—</td>
</tr>
<tr>
<td>$I_{id}$</td>
<td>$-5.377$</td>
<td>—</td>
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<tr>
<td>$P_{iq}$</td>
<td>$-0.155$</td>
<td>—</td>
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<tr>
<td>$I_{iq}$</td>
<td>$-2.689$</td>
<td>—</td>
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<tr>
<td>$f_{sw}$</td>
<td>2 kHz</td>
<td>[22]</td>
</tr>
<tr>
<td>$R_{th,c,\text{IGBT}(1)}$</td>
<td>$1.5 \times 10^{-3}$ kW</td>
<td>—</td>
</tr>
<tr>
<td>$R_{th,c,\text{IGBT}(2)}$</td>
<td>$7.3 \times 10^{-3}$ kW</td>
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</tr>
<tr>
<td>$R_{th,c,\text{IGBT}(3)}$</td>
<td>$5.9 \times 10^{-3}$ kW</td>
<td>—</td>
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<tr>
<td>$R_{th,c,\text{IGBT}(4)}$</td>
<td>$2.5 \times 10^{-3}$ kW</td>
<td>—</td>
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<td>$R_{th,c,\text{IGBT}(5)}$</td>
<td>$0.37 \times 10^{-3}$ kW</td>
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<tr>
<td>$C_{th,c,\text{IGBT}(1)}$</td>
<td>0.55 Ws/K</td>
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<tr>
<td>$C_{th,c,\text{IGBT}(2)}$</td>
<td>3.61 Ws/K</td>
<td>—</td>
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<tr>
<td>$C_{th,c,\text{IGBT}(3)}$</td>
<td>35.90 Ws/K</td>
<td>—</td>
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<tr>
<td>$C_{th,c,\text{IGBT}(4)}$</td>
<td>476.61 Ws/K</td>
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<td>$C_{th,c,\text{IGBT}(5)}$</td>
<td>$4.81 \times 10^{-3}$ Ws/K</td>
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<td>$R_{th,c,\text{diode}(1)}$</td>
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<td>$R_{th,c,\text{diode}(4)}$</td>
<td>$11.9 \times 10^{-3}$ kW</td>
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<td>$R_{th,c,\text{diode}(5)}$</td>
<td>$8.6 \times 10^{-3}$ kW</td>
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<td>$R_{th,c,\text{diode}(6)}$</td>
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<tr>
<td>$C_{th,c,\text{diode}(1)}$</td>
<td>0.773 Ws/K</td>
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<td>$C_{th,c,\text{diode}(4)}$</td>
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<td>$C_{th,c,\text{diode}(5)}$</td>
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<td>$C_{th,c,\text{diode}(6)}$</td>
<td>$1.60 \times 10^{-4}$ Ws/K</td>
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<td>$R_{th,c,h(1)}$</td>
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<td>$R_{th,c,h(2)}$</td>
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<td>$R_{th,c,h(3)}$</td>
<td>$4.3 \times 10^{-3}$ kW</td>
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<td>$R_{th,c,h(4)}$</td>
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<tr>
<td>$R_{th,c,h(5)}$</td>
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<tr>
<td>$C_{th,c,h(1)}$</td>
<td>337.28 Ws/K</td>
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<tr>
<td>$C_{th,c,h(2)}$</td>
<td>409.76 Ws/K</td>
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<tr>
<td>$C_{th,c,h(3)}$</td>
<td>$1.37 \times 10^{-3}$ Ws/K</td>
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</tr>
<tr>
<td>$C_{th,c,h(4)}$</td>
<td>$1.91 \times 10^{-4}$ Ws/K</td>
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</tr>
<tr>
<td>$C_{th,c,h(5)}$</td>
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<tr>
<td>$T_{C_{Esw}}$</td>
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<td>[30]</td>
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<tr>
<td>$T_{C_{Err}}$</td>
<td>0.006</td>
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</tr>
<tr>
<td>$T_{ref}$</td>
<td>125$^\circ$C</td>
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</tr>
</tbody>
</table>