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EQUALIZING THE RADIAL ATTRACTION FORCES IN DIRECT-DRIVE PM GENERATORS

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Abstract

Large scale PM generators are subject to very large radial attraction forces between the rotor and stator. Combined with unequal deformation of the airgap clearance, this can lead to serious challenges for the generator structure. To equalise the radial forces in different parts of the machine, stator modules with variable flux-weakening and flux-strengthening are used in this paper. A case study of a permanent magnet generator for a direct-drive wind turbine is analysed magneto-statically. The results show that by shifting the current angle in the modules, the imbalance in radial forces (1.1×10^3N) can be equalized. Simplified analytical modelling is shown – by 2D finite element analysis – to over-predict the radial forces, so the required variation in current angle shift will be less than predicted analytical (i.e. +15.8° in one module and -12.2° in another). This motivates the development of the analytical model to include slotting, saturation and higher spatial airgap flux density harmonics.

1 Introduction

Wind energy is a growing form of power generation, with progressively lower costs [1]. Countries with access to seas are increasingly developing offshore wind farms. The UK government – for example – recently announced plans to expand its offshore wind capacity from 30GW to 40GW by 2030 [2]. In order to reduce the cost of energy of the overall wind farm, there has been a move to higher powered wind turbines. Offshore, there is higher premium for wind turbine powertrains which are more efficient and have lower downtime. This has led many wind turbine manufacturers to choose direct-drive permanent magnet (PM) generators, which are robust (with no brush gear and no gearbox) and efficient (no generator excitation losses).

One of the downsides is that direct-drive generators are very large, as they have a large torque rating. As such, direct-drive PM generators are heavy, both due to the electro-magnetically active material, but also because of the mechanical stiffness requirements that are needed to maintain small airgaps for high flux density [3]. For example, the Zephyros Z72 wind turbine with a 1.5MW output weighed 47.2 tonnes and had a nominal airgap of 4.5mm [4]. These airgap clearances will reduce due to the normal component of Maxwell stress. Fig.1 demonstrates the general types of airgap deflection.

![Various deflection mode for generators](image)

Fig. 1 Various deflection mode for generators [5] (a) Uniform closure (b) Closure and eccentricity (c) Closure and ovalisation (d) Closure and triangulation

The deflection could occur due to the distortion of rotor and/or stator. Keeping a small airgap clearance ensures a higher efficiency for generators, and the potential for using less expensive magnet material. It does however require heavy and expensive rotor and stator structures to withstand the radial force, which has ten times the magnitude of shear stress [4].

In a PM machine, there is currently no natural means for controlling the airgap force in real-time. Therefore, instead of finding the solution for mitigating the effects of the radial force through structural design, it would be desirable to be able to influence the radial force magnitude. The key determinant of radial force is the flux density that travels across the airgap length [4]. Flux-weakening (and strengthening) is an approach in machine control that could be employed. From a structural perspective, it has been shown that higher modes of deflection (such as (b)-(d) in Fig. 1) are more challenging to deal with. If one could vary the magnitude of the flux density locally – by shifting the stator current angle in one area of the machine relative to that in another area – then local control of attraction forces is achievable.

To enable this to happen, this paper assumes a machine with a stator winding made of several three-phase modules, each corresponding to a certain arc around the airgap and each with its own power converter. Shifting the current phase angle for a particular module affects the flux density across the airgap located next to the module. For modules located in areas with larger airgaps, the module will strengthen the airgap flux density; while modules will flux-weak where the airgap is smaller. The module number is a design choice, and is somewhat dependent on the expected shapes of airgap deformation, where more peaks of deformation need more modules.

2. Methodology

This paper looks at this problem at 3 different levels: at the machine, at a generic pole pair and at the module level.

2.1 Machine level model

The first perspective is machine level, where the model is concerned with the variation in the airgap clearance and the impact on the radial forces.
Fig. 2 Generator level with certain deformation

The airgap clearance, \( g(\theta_{\text{mech}}) \) is defined as the clearance from rotor magnets to stator surface and varies with mechanical angle \( \theta_{\text{mech}} \) of machine:

\[
g(\theta_{\text{mech}}) = g_0 - \bar{\delta} - \delta_\alpha \sin[n(\theta_{\text{mech}} + \varphi)]
\]

where \( g_0 \) is the designed airgap clearance of machine, \( \bar{\delta} \) is the mean value of airgap deformation, \( \delta_\alpha \) is the magnitude of higher order deformation variation component, \( n \) is the number of deformation peaks and \( \varphi \) indicates the position of first deformation peak corresponding to the machine reference axis.

In order to understand how this airgap clearance affects radial forces, one must consider reluctance (or its inverse, permeance) and the magnetomotive forces (MMFs). Although at the pole pair level there is variation in airgap reluctance, due to the presence of PMs, at the machine level the airgap reluctance is dominated by variation described in equation (1). [There is a difference in the \( d \)-axis reluctance, where the effective airgap clearance consists of both PMs and air, and the \( q \)-axis reluctance where there is less PM. For a typical surface mounted PM machine, the difference between these two reluctances is only around 2%. Therefore, when calculating airgap flux density later, these two reluctances will be assumed to be the same]. At the machine level, the permeance of airgap per unit area is

\[
P(\theta_{\text{mech}}) = \frac{\mu_0}{A} \frac{\mu_0}{g(\theta_{\text{mech}})} A = \frac{\mu_0}{g(\theta_{\text{mech}})} P(\theta_{\text{mech}}) \quad (2)
\]

where \( \mu_0 \) is the vacuum permeability and \( A \) is the surface area. The flux density \( B \) across the airgap is

\[
B = \frac{P(\theta_{\text{mech}})}{A} F \quad (3)
\]

where \( F \) is the MMF of the combined magnetic fields of the stator winding and rotor PMs. The radial stress \( \sigma \) is then

\[
\sigma(\theta_{\text{mech}}) = \frac{B(\theta_{\text{mech}})^2}{2\mu_0} \quad (4)
\]

It follows then that the radial force varies with machine angle if the airgap varies with machine angle, but the magnitudes of MMF are constant with respective to machine angle. If the MMF can be manipulated so that it varies with machine angle, then equations (3) and (4) indicate that the radial stress can be kept constant.

2.2 Pole pair level model

In a large direct-drive machine, there can be a large number of rotor poles, in excess of 100. While the deformation occurs around the whole airgap, the variation of airgap length for one pole pair of PMs can be neglected. Therefore, the airgap clearance for one pole pair is seen as a local constant \( g \).

Fig. 3 shows a simplified one slot per pole per phase winding layout. The three-phase current on stator winding corresponding to one pole pair of PMs on rotor is

\[
\begin{align*}
I_a &= I_{\text{max}} \cos(\theta_p) \\
I_b &= I_{\text{max}} \cos\left(\theta_p - \frac{2}{3}\pi\right) \\
I_c &= I_{\text{max}} \cos\left(\theta_p + \frac{2}{3}\pi\right)
\end{align*}
\]

where \( I_{\text{max}} \) is the magnitude of stator winding current, \( \theta_p \) is the phase angle of stator current (this usually includes a time varying component but is constant in this magneto-static study). The MMFs of three-phase currents are

\[
\begin{align*}
F_a &= N I_a \cos(\theta_p) \\
F_b &= N I_b \cos\left(\theta_p - \frac{2}{3}\pi\right) \\
F_c &= N I_c \cos\left(\theta_p + \frac{2}{3}\pi\right)
\end{align*}
\]

where \( N \) is the number of turns per coil per phase for one pole pair, and \( \theta_p \) is an electrical angle, where a pole pair can be described as \( 0 \leq \theta_p \leq 2\pi \).

Assuming the machine is static in time and that the rotor has turned \( \theta \), the Park transformation matrix of the axis of dq-reference is aligned with phase a

\[
\begin{bmatrix}
F_{\text{stator,d}} \\
F_{\text{stator,q}}
\end{bmatrix} =
\begin{bmatrix}
1 & -\frac{1}{\sqrt{3}} \\
\frac{1}{\sqrt{3}} & \frac{1}{\sqrt{3}}
\end{bmatrix}
\begin{bmatrix}
F_a \\
F_b \\
F_c
\end{bmatrix} \quad (7)
\]

Substituting equations (5) and (6) into (7), the stator winding MMF in dq-reference is

\[
\begin{align*}
F_{\text{stator,d}} &= \frac{1}{2} N I_{\text{max}} \cos(\theta_p + \theta_e) \\
F_{\text{stator,q}} &= -\frac{1}{2} N I_{\text{max}} \sin(\theta_p + \theta_e)
\end{align*}
\]

At the moment in time that this study focuses on, the stator current phase is at \( \theta \). The current angle can be manipulated for flux-weakening/strengthening purposes by introducing a phase shift angle \( \Delta \), and so the final \( \theta_e \) in this study becomes \( \Delta \).

On the rotor, and only considering the first harmonic of the rotor PM MMF, by definition this is fully aligned with the stator d-axis MMF:

\[
F_{PM} = \frac{B_{\text{h_m}} A}{\mu_0 \pi} \sin\left(\frac{\pi w_m}{2} \frac{\pi}{\tau_p}\right) \sin(\theta_p) = K_{PM} \sin(\theta_p) \quad (9)
\]
where \( B_r \) is remanent flux density of PMs, \( w_{in} \) is the rotor PM width, \( \tau_p \) is the pole pitch.

The d-axis MMF magnitude is then \( F_d \equiv F_{dPM} + F_{dstator,d} \). The q-axis MMF magnitude is then \( F_q \equiv F_{qstator,q} \), \( F_d \) and \( F_q \) are the magnitudes of fundamental harmonics of two waveforms of MMF which are orthogonal to one another. Adding these two waveforms together and then calculating for the total MMF of one pole pair \( F(\theta_p, \Delta) \):

\[
F(\Delta, \theta_p) = \sqrt{F_d(\Delta)^2 + F_q(\Delta)^2 \sin(\theta_p + \epsilon)}
\]

(10)

Where

\[
F_d(\Delta) = K_{PM} - \frac{\sqrt{2}}{2} N_{max} \sin \left( \Delta + \frac{\pi}{4} \right)
\]

\[
F_q(\Delta) = \frac{\sqrt{2}}{2} N_{max} \cos \left( \Delta + \frac{\pi}{4} \right)
\]

\[
\epsilon = \tan^{-1}\left( \frac{F_q(\Delta)}{F_d(\Delta)} \right)
\]

(11)

Eqns. (10) and (11) show that the MMF can be varied by changing the phase shift angle \( \Delta \).

### 2.3 Stator module level

The third perspective consists of modules with a number of pole pair models illustrated in Fig. 4. One stator module consists of a certain number of pole pairs which are subjected to the same current phase angle shift \( \Delta \). If the number of pole pairs is reasonably large compared to the whole machine, then the airgap clearance will vary noticeably from one part of the module to another part of the same module. Therefore, to determine the general effect of current phase shift of one stator module on the radial force, one can integrate (or sum) across all of the pole pairs for the radial force of one module \( F_e \).

\[
F_e = \sum_{n=1}^{k} \frac{\int_{\theta_p}^{\theta_p + \Delta} \left[ F_n(\Delta, \theta_p) \times \left( \frac{F}{F_n} \right)_n \right]^2 \times \frac{2\pi r}{p}}{2r_0}
\]

(12)

where \( k \) is the number of pole pairs for one stator module, \( l_i \) is the generator length, \( r \) is the radius to the middle of the airgap, and \( p \) is the number of pole pairs for machine.

The phase current shifts for each of the stator modules are independent and by manipulating current phase shift \( \Delta \), it is possible to reduce the difference between the radial force for each stator module, thus reaching equalization for airgap attraction force.

![Fig. 4 Stator module level diagram](image)

### 2.4 Validation

The machine level model which describes the airgap clearance deformation variation originates from a previous researcher’s work, and was verified in [5]. Utilizing a 2D finite element analysis will verify the variation in radial force, as well as show the effects on the shear stress and torque variation.

### 3 Results

#### 3.1 Machine level

This study focuses on a generator from [6] in Table 1 and used airgap deflections measured by Northern Power [7], given in Table 2.

Table 1 Generator dimensions

<table>
<thead>
<tr>
<th>Diameter name</th>
<th>Quantity</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of pole pairs</td>
<td>80</td>
</tr>
<tr>
<td>Stator radius(m)</td>
<td>2.5</td>
</tr>
<tr>
<td>Airgap clearance(mm)</td>
<td>4.5</td>
</tr>
<tr>
<td>Stator slot width(mm)</td>
<td>80</td>
</tr>
<tr>
<td>Stator tooth width(mm)</td>
<td>80</td>
</tr>
<tr>
<td>Stator height(mm)</td>
<td>80</td>
</tr>
<tr>
<td>Rotor yoke height(mm)</td>
<td>40</td>
</tr>
<tr>
<td>Magnet height(mm)</td>
<td>15</td>
</tr>
<tr>
<td>Rotor pole width(mm)</td>
<td>79</td>
</tr>
</tbody>
</table>

Table 2 Airgap clearance data

<table>
<thead>
<tr>
<th>Position</th>
<th>Upwind (mm)</th>
<th>Downwind (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°</td>
<td>3.94</td>
<td>5.21</td>
</tr>
<tr>
<td>45°</td>
<td>2.79</td>
<td>4.70</td>
</tr>
<tr>
<td>90°</td>
<td>2.92</td>
<td>3.43</td>
</tr>
<tr>
<td>135°</td>
<td>4.06</td>
<td>3.81</td>
</tr>
<tr>
<td>180°</td>
<td>4.06</td>
<td>4.57</td>
</tr>
<tr>
<td>225°</td>
<td>3.68</td>
<td>3.94</td>
</tr>
<tr>
<td>270°</td>
<td>2.67</td>
<td>2.41</td>
</tr>
<tr>
<td>315°</td>
<td>3.68</td>
<td>3.81</td>
</tr>
</tbody>
</table>

By averaging the upwind and downwind airgap clearances at each position, eqn. (1) can be used to describe the airgap shape under deflection, where \( g_0=4.5\)mm, \( \delta=0.99\)mm, \( \delta_s=0.66\)mm in this case:

\[
g(\theta_{mech}) = 3.51 - 0.66 \sin(2\theta_{mech})
\]

(13)

Based on equation (13), Fig.5 shows the approximation of the airgap clearance.

![Fig.5 Airgap clearance based on mechanical angle](image)

As the airgap clearance has four peaks, the minimum number of stator modules is 4. More stator modules can be used (and would be able to deal with higher order deflection modes). However, for this study the entire stator winding is divided into four stator modules each occupying 90° of the machine’s mechanical angle. Therefore, each stator module is the equivalent of 20 pole pairs of rotor magnets. With two modules focusing on weakening flux (M1 and M3) and the other two strengthening the flux (M2 and M4) we can infer from Fig. 5 that M1&M3 and M2&M4 can be operated as two pairs, with each pair experiencing different conditions.

#### 3.2 The pole pair level
Equation (13), can be used to take 80 points of local airgap clearance, one for each pole pair of the machine. The first stator module occupies the [0°, 90°] mechanical angle of the stator winding. Therefore, for the first pole pair model, the local airgap clearance is approximately constant as

\[ g_1 = \frac{\int_{0}^{\pi} [3.51 - 0.66 \sin(2\theta_{\text{mech}})] d\theta_{\text{mech}}}{\pi} = 3.46 \text{mm} \] (14)

At the pole pair level, the airgap flux and hence radial stress varies with the combined MMF. The estimated airgap flux density also changes in response to the current phase shift \( \Delta \). Changing this within [0°, 10°] is calculated by substituting \( g_1 \) into equation (12). Based on equation (4), the radial stress change across part of this pole pair is shown in Fig. 6.

![Fig. 6 Part of radial stress change corresponding to current phase shift of [0°, 10°]](image)

The dashed line in Fig. 6 represents the radial stress corresponding to one specific airgap clearance of 3.51mm and no current shift; the other lines have the nominal airgap clearance of 4.5mm and varying phase shift. Considering that the major cause of unequal radial force is due to the sine-term in equation (13), the stress with dashed line can be seen as the goal for adjusting the radial force. Fig. 6 shows that for this pole pair, a reasonably small change in \( \Delta \) can achieve the new radial stress for the average airgap clearance.

Table 3 gives the average radial stress above this pole pair under the influence of \( \Delta \). For this case, changing the phase shift angle from 0° to 10° reduces the radial stress by about 3%. It is likely that at the machine level, both weakening and strengthening will need to be used and that the phase shift may need to be increased beyond 10°.

Table 3 Average radial stress for the first pole pair

<table>
<thead>
<tr>
<th>Phase shift angle, ( \Delta )</th>
<th>0°</th>
<th>1°</th>
<th>2°</th>
<th>5°</th>
<th>10°</th>
</tr>
</thead>
<tbody>
<tr>
<td>Radial stress (10^5 Pa)</td>
<td>3.05</td>
<td>3.04</td>
<td>3.03</td>
<td>3.00</td>
<td>2.96</td>
</tr>
</tbody>
</table>

3.3 The stator module level

There are two sets of symmetrical stator modules in the stator winding, therefore it is possible to simplify the analysis by focusing only on Module 1 and 2. Based on the pole pair level calculations in §3.2, these can be repeated for each of the 20 pole pairs of those modules. The total attraction force in Module 1 and 2 can then be calculated by equation (12).

Considering that Module 1 intends to weaken the flux density and Module 2 intends to strengthen the flux, the phase shift for Module 1 is positive and negative for Module 2. The range of phase shift is assumed to be within [-20°, 20°].

![Fig. 7 (a) Stator module 1 with current phase shift [0°,20°]; (b) Stator module 2 with current phase shift [-20°, 0°]](image)

Fig. 7 (a) Stator module 1 with current phase shift [0°,20°]; (b) Stator module 2 with current phase shift [-20°, 0°]

The dashed line in Fig. 7 indicates the radial stress of one stator module with a constant effective airgap clearance 3.51mm mentioned in §3.1. In later parts, this is defined as the 'target' value for the average radial stress in a module. This average radial stress can be shown as a radial force by integrating across all pole pairs.

To determine how many degrees are needed to be shifted for one stator module, it is necessary to calculate the radial force
on a module. For Module 1&2, Fig. 8 shows the total radial force after shifting.

The ‘target’ accumulated radial force for one stator module is 1.201×10^6N. Based on the analytical model, to reach the equalization of accumulated radial force, M1 needs Δ=+15.8°, M2 needs Δ=−12.2°.

Table 4 Module 1 and 2 radial forces

<table>
<thead>
<tr>
<th>Stator Module</th>
<th>Initial total radial force (10^6N)</th>
<th>Shifted total radial force (10^6N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>M1</td>
<td>1.2578</td>
<td>1.2008</td>
</tr>
<tr>
<td>M2</td>
<td>1.1486</td>
<td>1.2005</td>
</tr>
</tbody>
</table>

The difference between the Module 1 and Module 2 radial forces has dropped from 1.092×10^6N to 300N. This demonstrates that the equalization of radial force is achievable by shifting Δ.

3.4 Validation and drawbacks

Comparing the radial force results between the analytical and finite element results can be instructive. Fig. 9 shows results for M1 and M2, from both analytical and FE, with M1 subject to Δ=+15.8°, and M2 subject to Δ=−12.2° in both cases.

The trend of results from analytical and FEMM modules are similar and of the same magnitude. There are differences in the baseline results (in blue), which are likely due to the analytical model ignoring slotting and saturation, as well as the simplification of using only the first harmonic of flux density [5]. (These are neglected in the analytical model but are included in the FE model). Hence the analytical model over-estimates the radial force by around 7%. Thereafter, the change in radial force is similar in magnitude (between the analytical and FE results) when the current is shifted. Because the analytical model was used to select Δ, the FE model over-corrected the radial-forces meaning that there remained an imbalance in the force (42.3% of the original magnitude) but in the opposite sense to previously. An improvement in the analytical model based on [5] or the use of FE results is necessary for the adequate selection of Δ, which would then deliver correct radial force equalization.

Fig.9 Comparison between Analytical model and FEMM

One side-effect of stator current phase shift is that the shear stress is affected. The torque produced by each stator module is different. As M1 torque decreased from 64.7kNm to 61.0kNm due to the change in Δ, whereas the M2 torque increased from 60.4kNm to 62.6kNm. The general effect of phase shift on the generator torque is relatively small, which in this case only 1.2%. (N.B. This would be reduced with corrected Δ). Reduction in torque would need to be compensated for by a change in the phase current and hence Joule losses. The phase current therefore increased 1.2% and leads to a 2.4% increase for the Joule losses. It would also lead to a change (in this case beneficial) in torque ripple with a n = 2 harmonic.

One concern associated with this flux-weakening/strengthening strategy is that it could make magnet demagnetization more likely. Demagnetization can occur, when the stator field magnitude is large and in opposition to the rotor magnetic field. If this drives the magnet flux density lower than its knee point, then there is irreversible demagnetization. This is more likely when the magnet is at higher temperature and the knee point is then at a higher flux density. With an “unshifted” stator field, this is less of a risk than when the stator field has been shifted so that some of the stator field is in opposition to the rotor field. Figure 10 shows flux density in the case where the stator field is unshifted. The analysis was also performed in the cases with the stator field shifted, and when the magnet is thinner due to a smaller design airgap. It shows that demagnetization is not a problem in normal conditions. That said, generally this concern might lead to requirements for magnet grades that are harder to demagnetize and are more resilient to elevated temperatures or greater magnet cooling effort. This could add further costs.

Fig.10 The flux density distribution of PMs. Red circles highlight regions of lowest flux density in PMs.

Preliminary demagnetization analysis shows that the flux density patterns hardly changed comparing the distribution before and after the stator current phase shift. The lowest flux density points (at an arbitrary rotor position) are highlighted in Fig.10. For the normal size airgap, they are 0.796T (North pole), 0.790T (South pole) before and 0.813T, 0.806T after shifting. With a smaller airgap design, they maintain the same pattern with 0.773T, 0.758T before shifting and 0.803T, 0.747T after. The knee point flux density for this PM material (N50M) is 0.6T at 100°C [8].

Thus far, the modules have been treated ideally, without examining the effect of magnetic field manipulation in module on another module. This is likely to be noticeable if there are significant number of modules and hence many interfaces between separate modules. One further area worthy of
attention is the change in the iron flux density patterns and hence changes to iron losses.

4 Conclusions

In a large PM machine with varying airgap clearance (as a function of machine mechanical angle), distributed machine modules with their own controllable power converters can be used to produce locally varying airgap flux densities and hence lead to the equalization of radial forces. This would then allow eccentricities and ovalisation (and other) deformation patterns to be dealt with in real-time and potentially allow for smaller airgap sizes and/or less stiff and lighter machine structures.

5 References