A modular and cost-effective high-temperature superconducting generator for large direct-drive wind turbines

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Abstract
High temperature superconductors (HTS) enable very compact electric machines with high power density. The aim of this paper is to study and improve an HTS power generator based on the authors’ previous 10 MW double claw pole design. An additional stator was added to the machine, increasing the copper volume in the generator, resulting in a large increase in power density. Furthermore, with the additional inner stator, the overall current density in the individual stators can be reduced, leading to an increase in efficiency. The modularity of the generator is further improved through the addition of the third stator, further increasing its fault tolerance. The efficiency of the machine can be increased from 94.5% to 95% at a power output of 11.5 MW or through a further reduction of the current density in the stators, an efficiency of 95.6% can be reached while maintaining a power output of 10 MW. Hence, the newly proposed design offers a potential increase in power density, efficiency and modularity while maintaining the same machine diameter and low HTS tape requirements, making the design a very cost-effective option to enable 10 MW direct-drive wind turbines.

1 INTRODUCTION

One of the biggest challenges the offshore wind energy sector faces is to reduce the cost of energy. The cost of energy is strongly affected by the installation and foundation costs and downtimes due to faults [1]. Offshore renewable energy devices typically deal with large forces moving at a low speed, making conventional high speed and low force generators unsuitable. These generators require a gearbox or other speed conversion mechanisms, which can introduce additional risks of failure, maintenance requirements and unwanted downtimes [2, 3]. Direct-drive (DD) generators eliminate the need for a gearbox; however, they are very large and heavy, which increases their cost and complicates their transportation. Hence, for offshore applications, a compact, robust and modular generator, which can continue operating even under certain fault conditions, is desirable. With this objective, the double claw pole machine was designed.

It is a superconducting axial flux machine. The high temperature superconducting (HTS) field winding allows for a very high current density, which increases the magnetic loading of the generator making it very compact. Unlike the majority of other superconducting generator designs the field winding is stationary [4–6]. This eliminates the need for cryocouplers and brushes, which increases the reliability of the machine.

In addition, the machine is highly modular. It features two stators on each side, which can operate independently in case of a fault. The field winding can be wound into separate loops with separate cryostats, hence even if one of the field windings has a fault, the generator can continue to operate under partial load [7]. Furthermore, the design only requires 2.4 km of HTS tape at an operating temperature of 65 K, which is a fraction of other 10 MW HTS generator designs [8–10].

The original design claw pole concept was extensively tested with a linear prototype to confirm its electromagnetic performance [11].

While the generator design is believed to be very cost-effective and reliable, due to its low HTS requirements and high modularity, it is heavier than other HTS generator designs because of its iron-cored structure and it suffers from a lower efficiency.
FIGURE 1 Concept of the double claw pole machine

TABLE 1 Original double claw pole generator design [7]

| Parameter               | Value         |
|-------------------------|---------------|
| Rated power             | 10 MW         |
| Rated speed             | 10 RPM        |
| Outer diameter          | 6.63 m        |
| Axial length            | 1.38 m        |
| Number of poles         | 88 /          |
| Number of coils         | 66 /          |
| Electrical frequency    | 7.33 Hz       |
| Operating temperature   | 65 K          |
| Efficiency              | 94.5 %        |
| Length of SC wire       | 2.4 km        |
| Active mass             | 57.9 tonnes   |
| Total mass              | 184.2 tonnes  |

In this paper, the original design of the double claw pole machine is further improved and the main issues of the generator are addressed. The newly introduced design offers a higher power density at an increased efficiency, offering the potential to further reduce the levelized cost of energy (LCOE) of large DD wind turbines. A schematic of the original design of the double claw pole machine is shown in Figure 1 [7].

A summary of the generator parameters is given in Table 1.

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FIGURE 2 FEM model of the original double claw pole generator

FIGURE 3 z-direction flux density distribution in the inner air gap

The machine design features conventional copper stators, hence leading to the design having a similar electrical loading as conventional machines. The electrical loading of rotating electrical machines can essentially be determined by the space, that is how much copper can be fitted into the stator, and the cooling system, that is how much heat can be removed to maintain safe operation [12]. When examining the structure of the double claw pole machine, it becomes clear that a novel design approach can be introduced to reduce the mass per kW of the machine. The field core in between the small claw poles is required in order to give mechanical support and access to the stationary superconducting field winding, it does not serve any other purpose. However, a homopolar field crosses the field core similar to the field in the homopolar machine described in [14].

Figure 2 highlights the homopolar field crossing the field core. It can be seen that the flux density in between the small claw poles is close to 0 T and where the small claw poles overlap it is above 1 T.

Figure 3 shows a more detailed analysis of the flux density in the inner air gap. The figure shows the z-direction flux density...
distribution, that is out of the claw poles and into the field core over the mechanical angle of the machine.

It can be clearly seen that a homopolar field is present in the air gap. The minimum flux density does not reach 0 T due to the flux leakage that is present between claw poles and the fringing flux inside of the field core. The flux density peaks in the middle since the small claw pole is perfectly aligned with the adjacent outer stator teeth as shown in Figure 2. The peak flux density for the neighbouring small claw poles is slightly lower since they only partially overlap with the outer stator teeth.

From investigating the flux density in the inner air gap, it becomes clear that additional power can be extracted from the generator by replacing the field core as highlighted in Figure 1, with copper coils and hence increasing the electric loading of the machine. Figure 4 shows a representation of the machine with the inner stator.

The small claw poles overlap the inner stator coils on either side equally, leading to the forces acting on the coils in the axial direction being balanced and hence there is no net force acting in either direction. Similar to another design of an axial flux machine, the C-GEN concept, the stator coils can be immersed in epoxy resin, which are then held in place by aluminium bands for mechanical support [15]. Similar to the outer stators, the new inner stator also consists of 66 coils and the stator teeth of all three stators are aligned with each other. This ensures that the overlapping area between the small claw poles and the stator teeth is always equal for all four air gaps, hence leading to the air gap closing forces acting on the claw poles still being balanced for the outer and inner air gaps as well.

To investigate the potential increase in power output, a reluctance network model is applied to the design in the next section.

3 RELUCTANCE NETWORK MODEL

In order to explore the benefits of the added inner stator, a reluctance network of the double claw pole machine was developed in MATLAB [17, 18]. The reluctance network is shown in Figure 5.

Each component of the machine is represented by several reluctances. Each reluctance is calculated according to its geometry and material properties using Equation (1).

\[ R = \frac{l_e}{\mu_0 \mu_r A} \]  

where \( l_e \) is the equivalent length through the component, \( \mu_0 \) is the permeability of air, \( \mu_r \) is the relative permeability of iron and \( A \) is the cross-sectional area of the component.

In addition, all major leakage paths were considered such as, leakage flux between the claw poles and zig-zag leakage flux between the stator teeth.

It was decided to use Vacoflux50 for the active mass due to its high saturation limit of 2.28 T at 16 kAm\(^{-1}\) [19]. It is a cobalt iron alloy, which is relatively expensive in comparison to other electrical steels. However, it allows high air gap flux densities without using an excess amount of superconducting tape as compared to air-cored superconducting generator designs [17]. In addition, Vacoflux50 can be manufactured in laminations. Hence, even large machine components can be assembled.

A Newton–Raphson type algorithm is used to solve the nonlinear flux equations. A detailed explanation for the algorithm that was used can be found in [17]. In addition to the reluctance network model, 3D finite element analysis using the simulation software MagNet was performed. For both MATLAB and MagNet, the \( \mu_r-B \) curve of Vacoflux50 is used to calculate flux densities in the generator. The results are then compared for validation in the next section.

To calculate the output power, the stator tooth flux for each stator is obtained from the reluctance network model. The power output of each stator can then be calculated according to Equation (2).

\[ P_{out} = N_{coil} \times (E_{RMS} \times I_{RMS} - I_{RMS}^2 \times R_{coil}) \]  

where \( N_{coil} \) is number of coils per stator, \( E_{RMS} \) is the induced voltage for one coil, \( I_{RMS} \) is the stator current and \( R_{coil} \) is the resistance of a stator coil.

\( E_{RMS} \) can be calculated according to Faraday’s law using (3).

\[ E_{RMS} = \frac{N_{turn} \times \phi_{peak} \times f}{\sqrt{2}} \]  

where \( N_{turn} \) is the number of turns per coil, \( \phi_{peak} \) is the peak stator tooth flux and \( f \) is the electrical frequency.

\( I_{RMS} \) is calculated using (4).

\[ I_{RMS} = J_{RMS} \times A_{wire} \]  

where \( J_{RMS} \) is the current density of copper, which will be addressed in further detail later on, and \( A_{wire} \) is the coil wire diameter.

With the reluctance network set up, the machine dimensions need to be defined. It was decided to maintain the majority of the machine dimensions equivalent to the original design, allowing to make a meaningful comparison between the original design and the new concept with the additional inner
The reluctances $R$ are calculated according to Equation (1). The Kirchhoff's voltage law is applied to the magnetic circuit resulting in the flux loops $\Phi_1$ to $\Phi_5$. FC1 is the source and represents the superconducting field winding [16].

The reluctance network model, the peak flux for the outer stator teeth was hence calculated to be 57.8 mWb and for the inner stator teeth 81.3 mWb. In [20], the stator tooth flux for the original design was given as 57.08 mWb, which is in very good correlation with the flux calculated through the reluctance network model. The calculated stator tooth flux for either design was expected to be the same since the reluctance path throughout the machine components was essentially maintained through the alignment of all three stators. Figure 6 shows the flux variation for the outer and inner stators with the initial assumption made that the flux variation is perfectly sinusoidal.

Stator. Moreover, the original design was optimized for the lowest active mass through the use of a genetic algorithm [7], hence taking over the original machine's dimensions is expected to also result in a low active mass without the need to re-optimize the machine. In particular, the machine diameter, the number of poles and the number of coils per stator were maintained and only the field core was adjusted as was discussed in the previous section of this paper.

Starting with the first approach, the current density in all stators is set to be equivalent to each other. Taking the same number of turns for the inner stator coils as for the outer stator coils, that is 96 turns, since they both have roughly the same dimensions, the induced RMS voltages for the outer stator coils and inner stator coils can be calculated to be 180.9 and 128 V respectively. To find the rated power output, the rated current needs to be defined, which is dependent on the stator coil dimensions. Figure 7 depicts a drawing of a stator coil. The available coil area is defined by the stator design through the stator tooth length and the coil pitch with the tooth to coil ratio. For the outer stator coils, the total coil area as defined in Figure 7, can be calculated to be 11,223 mm², giving a rated RMS current of 438 A for 96 turns, assuming a current density of $5 \cdot 10^6$ A/m².

It can be seen that the flux variation for the inner stator is homopolar since it only varies between 0 and $\phi_{\text{max}}$. In the reluctance network, the leakage flux between the inner stator teeth is ignored, hence the minimum flux is assumed to be equal to be 0 T.

To determine the power output and efficiency, two different approaches exist. The first approach is to assume the same current density for the inner stator as for the outer stators, which results in the highest potential power density that can be achieved for the given machine dimensions. Another approach would be to maintain the power output at 10 MW but make use of the additional copper volume available to reduce the current density per stator, leading to reduced copper losses and hence an increased efficiency.

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From the induced voltage and the rated current, the power output for the inner stator can hence be calculated to be equivalent to 4 MW using Equations (2)–(4). This is assuming that the rotational speed is kept at 10 rpm to allow for a meaningful comparison between the new design and the original concept. Hence, an additional 4 MW of output power can be achieved.
while maintaining essentially the same active mass, raising the total output power to 14 MW. The field winding is identical to the original design hence the required length of the superconducting tape is unchanged.

One disadvantage of increasing the electrical loading is that it also leads to an increase in copper losses, and hence reducing the overall efficiency. The copper losses are calculated according to the dimensions of the copper wire and the number of turns for each coil. Due to the very low operating frequency, the copper losses can essentially be determined solely from the DC resistance of the coils and the rated current squared. To find the efficiency with the addition of the new stator, the coil resistance of the inner stator has to be calculated. From the coil dimensions, the outer stator coil resistance $R_{out}$ can be calculated to be $0.02 \, \Omega$ and the inner stator coil resistance $R_{in}$ is $0.0167 \, \Omega$. The copper resistivity was assumed to be equivalent to $1.68 \times 10^{-8} \, \Omega \cdot m$. Table 2 summarizes the coil design for both stators.

For the original 10 MW design, the copper losses are equivalent to 510 kW. With the addition of the inner stator the total copper losses are increased to 873 kW. When only considering the copper loss in the machine, the efficiency can be calculated to be 95.13% for the original design and 94.19% with the inner stator.

The big drop in efficiency can be attributed to the homopolar field of the inner stator. The induced voltage for the inner stator is significantly lower than for the outer stators, hence the produced power output is much lower as well. The additional copper losses in combination with the relatively low additional power output results in an overall drop in efficiency.

Hence for this design approach, a significant increase in power density can be achieved but at the cost of reduced efficiency. However, efficiency is a critical parameter when designing electrical machines. While a lighter generator can reduce the levelized cost of energy of the wind turbine due to the potentially reduced capital cost and ease of installation, a lower efficiency could completely negate this benefit.

With the addition of the new inner stator, the available copper volume in the machine was increased. The first approach to take advantage of this was to assume the same current density for the inner stator and for the outer stators, hence achieving a significantly increased power density, while ending up with a lower efficiency. Another approach that can be taken, is to reduce the overall current density in the stators while still maintaining the target 10 MW power output. The reduced current per coil, reduces the overall copper losses and hence a higher efficiency can be achieved. Through testing of different combinations of stator currents, the lowest possible copper losses while still maintaining a power output of 10 MW was found to be 392 kW with a rated outer stator current of 340 A and an inner stator current of 270 A.

Hence, when only considering the copper losses the efficiency was increased to 96.2% at 10 MW. To calculate the overall efficiency, the copper losses, iron losses, cooling power for the superconducting winding and air blowers for the armature were considered. The iron losses are calculated using the Vacoflux50 datasheet and using the operating frequency in the machine, which is 7.33 Hz at 10 rpm. The required cooling for the stators was adapted from [7] with some minor changes. The required power input for the air blowers to cool the stator coils was estimated to be 40 kW for 510 kW of heat produced in the stators. Since the copper loss was reduced by approximately 23%, the required input power for the air blowers was reduced down to 31 kW.

In [7], the thermal budget for the cryostat was estimated to be approximately 157 W, additional to this, the superconducting field winding loss due to the magnetic field environment was calculated to be 16 W [22]. Hence, the field winding can be cooled with four 50 W cryocoolers, requiring a total input power of 24 kW. The overall efficiency is still dominated by the copper losses in the stators. With all losses considered the overall efficiency for this design approach can be calculated to be 95.63%. When taking 95% as the target efficiency, to achieve an increase in power density as well as a higher efficiency than the original machine, the electric loading can be further increased. A power output of 11.5 MW can be achieved at an efficiency of 95% with a rated RMS current of 400 A for the outer stators and 310 A for the inner stator. A comparison between the

![Figure 7](image_url)  
**Figure 7** Stator coil geometry with coil area $A_{coil}$ and mean turn length $l_{mean}$ defined.

**Table 2** Stator coil design

| Parameter               | Outer stator coil | Inner stator coil |
|-------------------------|-------------------|-------------------|
| Turns                   | 96                | 96                |
| Wire length (m)         | 104               | 118               |
| Coil total area (mm²)   | 11,223            | 14,200            |
| Wire area (mm²)         | 87.67             | 110.93            |
| Coil resistance (Ω)     | 0.02              | 0.0167            |
| Rated current (A)       | 438               | 554               |
original design and the designs with an additional stator is shown in Table 3.

For the design iterations with a higher power output, the structural mass was recalculated to account for the new rated torque using an analytical model. The mechanical structure to maintain the air gap is assumed to consist of the following components. The stator structure features five torque arms on either side and cylindrical shells that connect the two stator sides to each other. Through an optimization process done by Zavvos, the optimal number of torque arms was found to be five, for lowest structural mass. The analytical model calculates the radial bending of the cylinder shells and the axial deflection of the stator due to the normal stress $q$ that acts on the generator components in the air gap. A detailed description of the analytical model can be found in [23].

At 14 MW and 10 RPM, the rated torque can be calculated to be 13.27 MNm. Applying the new torque with the analytical model, the new structural mass for the generator was calculated to be 61.37 tonnes for the rotor and 119.15 tonnes for the stators, giving a total structural mass of 180.5 tonnes. For 11.5 MW, the rated torque can be calculated to be 10.98 MNm, giving a structural mass of 49.3 tonnes for the rotor and 95.9 tonnes for the stators. The active mass was recalculated to be 56 tonnes, which is slightly lower than for the original design. This is due to the copper partially replacing the solid iron of the field core.

While a significant increase in power density can be achieved in terms of mass, it should be noted that the outer diameter and axial length of the machine are identical to the original design. Hence, a substantial increase in power density can be achieved when considering the machine volume as well.

As was discussed in the first section, the size of the machine, in addition to the weight, plays a crucial role in the transportation and installation of the generator.

In addition to the increased power density, the modularity of the machine was even further improved. For the original design, the two outer stators can operate independently from each other, if there is a fault the generator can continue operating under partial load. Similarly, the inner stator can also operate independently, hence further increasing the modularity of the design.

Since according to the reluctance network, an increase in power density and efficiency can be achieved, it is necessary to verify the results using finite element analysis and it is important to investigate the machine performance in further detail such as the voltage and torque characteristics. The design iteration meeting the 95% target efficiency was taken for the more detailed analysis and the FEA results will be addressed in the next section.

4 VALIDATION USING FINITE ELEMENT ANALYSIS

4.1 Magnetostatic analysis

In order to verify the results, a 3D FEA analysis using the simulation software MagNet, was performed.

Firstly, a magnetostatic analysis was done to verify the magnetic flux density distribution throughout the machine. For this case, the stator coils were omitted. Figure 8 shows the flux density distribution for the original machine when the small claw poles are aligned with stator teeth for the original design.

Figure 9 shows the flux density distribution for the same scenario but with the field core split into stator teeth. It can be seen that the overall flux density distribution throughout the machine is very similar to the flux density distribution in the original design of the double claw pole machine. This is a good indication that the original flux path throughout the new machine was maintained as compared to the original design, as was the assumption when setting up the reluctance network model.
FIGURE 9  Flux density distribution for the small claw poles aligned with stator teeth for the new design [24]  

FIGURE 10  \( \zeta \)-direction stator tooth flux density (i.e. into and out of the stator tooth) when the small claw pole is aligned with the middle tooth for a) the outer stator and b) for the inner stator

4.2 Transient analysis

With the flux densities in the machine verified using the reluctance network model and the magnetostatic FEA, it is important to further analyse the machine performance when the rotor is in motion to study the induced voltages and torque characteristics. A remesh region is defined in the air gaps between the claw poles and the stators. A very fine mesh is used within the remesh region. The solution mesh has approximately 850,000 elements. The simulations are run over one period, which is approximately 136 ms with 2 ms time steps.

Figure 10 shows the flux density distributions midway through the stator coils for the inner and outer stators when the small claw pole is aligned with the middle tooth. It can be seen that the flux direction is in the positive as well as negative direction for the outer stator, while the flux for the inner stator is only into one direction. Furthermore, the flux density in the inner stator is higher than that for the outer stators, agreeing well with the reluctance network model.

Figure 11 shows the flux linkage over one period for one outer stator coil and an adjacent inner stator coil. From the figure, it can be seen that the peak stator tooth flux for the outer stator, depicted as OS, is approximately \(-59\) mWb, which is well in agreement with the calculated flux shown in Section 3. For the inner stator tooth flux, depicted as IS, it can be seen that the peak flux is approximately \(93.5\) mWb. This is higher than the calculated flux in Section 3, which was given as \(81.3\) mWb. However, the minimum flux for the inner stator tooth is approximately \(-14\) mWb as opposed to 0 mWb, which was assumed in the reluctance network model. Taking the difference between the maximum and minimum flux for the inner stator results in a flux variation of \(79.5\) mWb, which again is well in agreement with the results of the reluctance network model.

To investigate the open-circuit voltages, a very large resistive load is connected to the stator coils. The resultant open-circuit voltage is shown in Figure 12. For each phase, two waveforms are shown. One waveform corresponds to the induced voltage of the outer stator depicted as ‘OS’ and the other waveform to the induced voltage of the inner stator depicted as ‘IS’. For the outer stator the RMS voltage of a single coil was calculated.
to be equal to 182 V, which agrees well with the result from the reluctance network model shown in Section 3. The RMS voltage of the inner stator coils can be calculated to be 125 V, which again is well in agreement with the reluctance network results.

In addition to the voltage characteristics it is important to investigate the torque characteristics. The cogging torque leads to vibrations, which should be minimized to maintain smooth operation of the generator [25]. The cogging torque comparison between the original design and the design with an inner stator is shown in Figure 13.

From the figure, it can be seen that the cogging torque was increased as compared to the original design. For the original design, the interaction between the claw poles and the field core did not lead to any cogging torque, since the field core was a continuous solid piece of iron. The only source of cogging torque came from the interaction of the claw poles with the outer stator teeth. With the introduction of the inner stator, which has additional iron stator teeth, a further increase in cogging torque is to be expected since the small claw poles now interact with the inner stator teeth as the rotor is in motion. The peak-to-peak cogging torque for the original design is approximately 96 kNm, which is equivalent to 1 % of the rated torque. With the inner stator the peak-to-peak cogging torque was increased to 146 kNm, which is 1.33 % of the rated torque at 11.5 MW. Overall, the additional inner stator had only minor effects on the torque characteristics of the generator. In general, the cogging torque is relatively high, for both the original design and the newly introduced design in this paper. This is due to the combination of pole and slot numbers. With 88 poles and 66 slots, this combination has a large greatest common divider, which allows it to only model a small section of the machine due to rotational symmetry, this, however, also results in a larger cogging torque [26, 27]. Hence, the cogging torque can be further reduced through selecting a more appropriate number of poles and slots, at the cost of increased computational time. Additionally, the option of skewing the stator teeth is explored in [28], which also results in improved torque characteristics.

When designing a superconducting coil, two parameters in particular are important, the operating temperature and the electromagnetic environment the coil operates in. The operating temperature influences the critical current density, which dictates the current carrying ability of the superconductor. A lower operating temperature results in a higher critical current, which allows to minimize the length of HTS tape required. However, since it becomes significantly more challenging to remove heat at a lower temperature, the required input power to the cooling system becomes much larger at lower operating temperatures such as 20 K as compared to higher temperatures. To maintain a higher efficiency and a simplified cooling system, an operating temperature of 65 K was chosen, at the cost of increased HTS requirements.

The critical current of a superconductor is also very strongly influenced by its flux density distribution. The perpendicular flux distribution to its long surface in particular has the biggest impact on its current-carrying ability.

For the design of the field winding, the 4 mm wide GdBCO-coated conductor, FYSC-SCH04, manufactured by Fujikura is used. The HTS tape has a critical current of 469 A at 65 K. The winding consists of three coils placed next to each other as shown in Figure 14. The outer coils experience the highest perpendicular flux density, which reduces their critical current. Due to this, the outer coils have a rated current of 210 A using 55 turns. The inner coil has a rated current of 329 A using 33 turns. A safety margin of 30 % is applied to all coils to protect against quenching. The resultant winding window of the field winding is 32 mm (height) by 15 mm (width), this accounts for the insulation thickness and gaps between turns and layers. Further details on the field winding design can be found in [22].

Furthermore, when designing a superconducting coil, the losses exhibited by it need to be known to adequately calculate the required cooling power. Superconducting field windings in the electromagnetic environment of a rotating machine exhibit dynamic loss [22, 29]. This is due to the armature reaction and the rotation of the superconducting field windings, or in this case rotating claw poles. Also for dynamic loss, it was shown that it is mainly the perpendicular field, which contributes towards

**FIGURE 13** Cogging torque comparison

**FIGURE 14** Leakage flux interacting with superconducting field winding

5 | SUPERCONDUCTING FIELD WINDING

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the loss [30]. Additionally, the induced loss is highly frequency dependent, a high frequency leads to higher losses [31, 32].

For the original design, the field winding is located at a relatively large distance from the stator coils, hence the main changing magnetic field could be attributed to the rotating claw poles around it. For the newly suggested design in this paper, stator coils were placed at a very close vicinity to the superconducting field winding, which could result in larger losses occurring.

However, due to the location of the superconducting coil, with respect to the inner coil, it is expected that the flux produced by the inner stator coils when carrying a current, mostly penetrates the superconducting tapes in a parallel manner. Additionally, since the superconducting winding is surrounded by iron, the field winding is only expected to be subject to small leakage flux due to the inner air gaps. The hypothesis is illustrated in Figure 14, which shows a cross-section schematic of the field winding and an inner stator coil located above it, surrounded by the small claw poles.

To confirm the effect of the inner stator coils on the magnetic field distribution on the tape, the inner stator coils are supplied with the rated current of 310 A, as stated in Section 3, and the flux density distribution along the tape is recorded. To increase the accuracy of the flux density distribution on the field winding, the mesh density for the winding was increased significantly.

Figure 15 shows the perpendicular and parallel to the tape flux density distributions for the case when the rated current is supplied to the inner stator coils and for the case when no current is applied.

The high variance that is present in the field distributions is due to the relatively small size of the field winding in comparison to the generator dimensions, leading to a very high aspect ratio, in turn leading to significant numerical noise. However, the main point of interest in regard to the flux density distribution is the change in magnitude.

Firstly, considering the perpendicular flux density distribution, it can be seen that the current in the inner stator coils has a negligible effect on the perpendicular flux density, both flux density distributions are nearly identical with only a very slight increase.

When considering the parallel flux density, it can be seen that it was approximately doubled when supplying the stator coils with a current. However, as was mentioned previously, a parallel field penetrating the superconducting tapes has little effect on the critical current density and induced losses, hence this effect is negligible as well.

Any minor reduction in critical current can be compensated by increasing the number of turns while still offering a very cost-effective design due to the initial very low HTS tape requirements. Additionally, due to the very low rotational speed of the generator, the operating frequency is very low as well, hence the field winding losses can always be expected to be relatively low.

6 1 GENERATOR COST

To simplify the comparison between the cost of the novel design introduced in this paper and the costs of other 10 MW DD generator designs, the 10 MW design with the increased efficiency as introduced in Table 3 will be used for the cost comparison. To estimate the cost of the generator, certain assumptions for the cost of the different components need to be made. Table 4 highlights the assumed cost basis for the material required, the respective material weights and the estimated total generator cost.

Due to the high cost of cobalt iron, it is worth minimizing the use of it throughout the machine design. Cobalt is trading at approximately 36k €/tonne and Vacoflux50 consists of 49% cobalt. Assuming an additional cost for manufacturing the material, the cost of Vacoflux50 is assumed to be approximately 30 €/kg. When investigating the flux density distributions throughout the machine, it can be seen that the highest flux density distribution occurs for the claw poles. Hence the use of cobalt iron can be limited to only the claw poles, resulting in approximately 23.8 tonnes of cobalt iron required.
The additional costs that were considered are for the electrical steel laminations, structural steel, copper, HTS tape, power electronics and the cooling system.

From Table 4, it can be seen that the total cost for the 10 MW generator design can be estimated to be 2.35 M €. In [35], the cost of a 10 MW radial flux superconducting generator was projected to be approximately 2.6 M €. The proposed generator in this paper is hence more cost-effective and additionally offers a simplified cooling system due to the stationary field winding and a lower total mass.

Comparing the design to a 10 MW permanent magnet (PM) direct-drive (DD) generator as introduced in [33], the cost of the HTS generator design is approximately 30% higher than the conventional machine. One key difference in the cost estimation comes from the assumed cost of the power converter. In this estimation, the cost was assumed to be 800 k € as given in [35]. For the 10 MW PM DD generator, the converter cost was assumed to be 400 k €. This leads to a big discrepancy between projected generator costs. Nevertheless, it can be concluded that a conventional DD PM generator remains cheaper than the introduced generator design in this paper.

However, this newly introduced design is significantly lighter at 182 tonnes as compared to 325 tonnes for the PM DD generator. Additionally, the introduced generator design offers the possibility of a further increased power density through increasing the electrical loading and offering potentially higher efficiencies.

In the future, the design can be further improved by investigating the combination of cobalt iron and traditional electrical steel elements to further reduce the cost of the generator and minimize the dependence on cobalt iron components.

Thus, the introduced generator design has the potential to offer a reduced LCOE in the future due to a lighter design and a higher efficiency.

7 | CONCLUSION

In this paper, an improved version of the original design of the 10 MW double claw pole generator was introduced, which offers a higher power density and efficiency. The field core in the original design was replaced with an inner stator, which increases the electric loading of the machine and hence the power density. The proposed design was modelled using a reluctance network and through finite element analysis, with both methods producing results which are well in agreement with each other. The additional power that can be generated from the inner stator was calculated to be equivalent to 4 MW, assuming the same rotational speed as for the original design. Hence, a 14 MW machine was created, using approximately the same active mass and covering the same machine volume. However, this method significantly reduced the machine efficiency due to increased copper losses. A second approach was applied, which takes advantage of the additional copper volume in the machine, by reducing the overall current density in the stators. With a target efficiency of 95%, a power output of 11.5 MW can be achieved, while again maintaining the same generator mass and volume. With the additional stator, the resultant cogging torque increased from approximately 1% of the rated torque to 1.33%. Another important aspect for machines employing superconducting field windings is the magnetic environment of the superconductor. However, the effect of the new stator coils, which are located in close vicinity of the field winding, was found to be negligible.

Overall, the inner stator increased the power density, efficiency and modularity of the double claw pole machine, while still only using approximately 2.4 km of HTS tape. It is believed that this makes the design even more competitive in the high-temperature superconducting generator market.

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