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Analysis of a novel stator construction employing steel wire in place of laminations

Nick Stannard, Richard Martin, Glynn Atkinson

Abstract— This paper examines a novel method of constructing large diameter generators using many layers of steel wire in place of laminations. The stator coreback is formed by winding thin steel wire around the outside of the armature coils and then encapsulating the structure in epoxy. This technique simplifies the manufacturing process by removing the requirement to build a large support structure to carry the laminations. The electromagnetic behavior of a wire coreback is very different from traditional laminations, however, and produces abrupt changes in flux density across its thickness. The material is difficult to model using conventional FEA techniques due to the large number of elements required to mesh the small diameter wire. This paper examines two alternative modelling approaches. Method 1 uses 2D FEA to model the steel wire as a lamination oriented in the ‘wrong’ direction. Method 2 uses a quasi-analytic approach based on detailed 3D FE analysis of a small section of the generator to capture the flux density profile in the airgap. The two models are benchmarked against a prototype generator tested in the laboratory, and it is shown that the quasi-analytical technique gives the most accurate prediction of performance.

Index Terms— PM generator, large diameter generator, wound-iron composite, wound coreback, wire coreback, wire stator, wire rotor, tidal stream generator,

I. NOMENCLATURE

A	Coil area, m ²	p	Pole pairs
B	Flux density, T	t	Thickness, m
B _r	Remnant flux density, T	w	Magnet width, m
I	Phase current, A	α	Steinmetz exponent
L	Inductance, H	β	Steinmetz exponent
M	Mass of stator, kg	σ	Electrical angle, rad
N	Turns per armature coil	μ_r	Relative permeability
P	Power, W	λ	Flux linkage, Wb
R	Radius, m	ρ	Density, kg/m ³
R _{ph}	Phase resistance, Ω	ω	Electrical frequency, rad/s
V	Voltage, V	Subscripts	
X _{ph}	Phase reactance, Ω	S	stator
f	Frequency, Hz	R	rotor
c	Running clearance, m	a	air gap
k	Flux reduction factor	c	coil
k _e	Steinmetz coefficient	cu	copper loss
k _h	Steinmetz coefficient	i	inner
l	Axial length of machine, m	iron	iron loss
n	Turns per unit length, m ⁻¹	o	outer
n _p	Coils in parallel	m	magnet
n _s	Coils in series	mech	mechanical loss

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II. INTRODUCTION

THERE are many applications that require electrical machines with large air gap diameters. Examples include certain designs of wind turbine generators [1]-[4], wave energy converter [5]-[6], tidal stream turbines employing rim generators [7]-[12], and marine propulsion motors [13]-[14]. These machines typically employ Permanent Magnets (PM) with a high pole numbers and thin corebacks. Manufacturing the corebacks from traditional laminations can be costly since the stator and rotor both require large diameter support structures to be constructed to carry the laminations.

This paper investigates an alternative approach for manufacturing the corebacks of large diameter machines, which has been called Wound-Iron Composite (WIC), and represents a significantly simpler manufacturing route than using traditional laminations. A WIC structure is formed by winding thin wire – made from high grade electrical steel – directly onto the machine over multiple layers to form the corebacks in place of laminations. The wire is encapsulated in epoxy during construction and backed with Glass Fiber Reinforce Plastic (GFRP) to form a strong, load bearing component, as shown schematically in Fig. 1 and as a prototype in Fig. 15-Fig. 18. Stator and rotor teeth are not used in this type of construction, and this increases the effective air gap, leading to a higher magnet mass in the machine. However, large diameter machines generally require a larger air gap to accommodate manufacturing tolerances and the flexibility of the structure, and so the marginal increase in magnet mass is less significant than for smaller machines.

For large machines in the 100kW-MW range a detailed structural analysis would be required to manage the strong magnet forces in the machine. These forces produce an ‘ovalizing’ stress in the stator which can encroach on the air gap if the structure is not sufficiently stiff. To prevent ovalization, carbon fiber bracing members can be embedded within the GFRP coreback to stiffen the structure in a similar manner to the construction of wind turbine blades, which use a combination GFRP and carbon fiber for stiffness and cost.

The electromagnetic behavior of a WIC structure is very different from laminations, as it produces sharp discontinuities in flux density across the coreback (this is described in more detail in section A). This paper will examine two methods of modelling this behavior with the aim of finding the most accurate method of predicting machine performance.

Fig. 1. Schematic of prototype WIC generator construction

A. Electromagnetic behavior of WIC

Wire cores have been used in the past for electromagnetic applications, most notably in the early development of transformers in the Ganz factory, Hungary (c. 1886) before they were overtaken by the development of laminations from the 1890s [15]. Iron wire could be used for transformer applications because the magnetic flux travels around a closed magnetic circuit and does not jump between wire strands, producing an even distribution of flux within the core. If wire cores are used for rotating machines, however, flux must pass between strands as it enters and exits the core from the radial direction, and this produces abrupt changes of flux density across the coreback.

This phenomenon is depicted in Fig. 2 and Fig. 3, which show the flux density distribution across a single pole pitch of a PM machine. The stator has been constructed from 800 strands of 0.45mm diameter 50JN350 electrical steel wire (the armature coils are not shown). Fig. 2 has been created with JMAG v14 finite element software and gives a pictorial representation of the flux density contours.

Fig. 2. Flux density in a wire coreback

Magnetic flux in the stator coreback will follow the lowest reluctance path, and this leads to the wire strands closest to the air gap becoming saturated. Once this occurs flux moves to the next closest non-saturated strand, which is located further out in the radial direction. This strand now offers the lowest reluctance path and so will attract excess flux until it too becomes saturated. This process continues until all the magnet flux is taken up by a relatively small number of highly saturated wire strands located closest to the air gap. In reality this process is not time dependent and the redistribution of

flux within the core occurs instantaneously, leading to the strands closest to the air gap becoming highly saturated while the peripheral strands carry zero flux. Unlike a traditional lamination, increasing the thickness of the coreback will not reduce the flux density in the material but will simply add unnecessary mass.

The flux distribution is shown graphically in Fig. 3, which compares the flux density profile across the center of the stator coreback of Fig. 2 with a traditional 0.35mm laminated coreback.

Fig. 3. Comparison of flux density in the stator coreback for wire and standard laminations

Fig. 3 shows there is an abrupt discontinuity in the flux density profile of the WIC material at approximately 9mm across the coreback. This feature is not present for a traditional laminated machine and occurs because the flux tends to bunch in the lowest reluctance strands located closest to the air gap. Due to this saturation effect, a WIC machine may have slightly higher iron losses than a traditional PM machine, which tend to operate at or below the knee of the saturation curve.

B. Selection of steel wire for WIC prototype

This section describes material tests that were performed on five different types of steel wire in order to find a suitable material for constructing a WIC prototype. Due to the nature of WIC corebacks described in Section A, the maximum saturation level of the steel wire and its loss characteristics are particularly important for producing a functional machine.

Five steel wires of different grades and diameters were tested for their peak saturation level and loss characteristics using a set-up similar to an Epstein frame. In addition, an electrical steel with known properties was also tested to benchmark the procedure. Details of the materials tested are given in Table I.

TABLE I
MATERIALS TESTED

Size	Notes
0.30mm diameter	Galvanized wire rope (DiN EN 10264-2)
0.45mm diameter	Bright mild steel, 1006
0.65mm diameter	Galvanized mild steel, Q195
0.95mm diameter	Galvanized mild steel, Q235
1.45mm diameter	Galvanized mild steel, 1006
0.35mm lamination	CRGO-M4

Each test wire was wound on to a plastic bobbin, shown in

Fig. 4 (a), with a known number of turns. The bobbin was then wound toroidally with two copper coils (Fig. 4 (b)) to allow the B-H curve and loss characteristic to be measured.

Fig. 4. Iron loss test set up showing (a) steel wire wound on a plastic bobbin and (b) after a double toroidal winding

A sinusoidal current was passed through the primary coil, while the induced EMF was measured on the secondary. B-H curves can then be constructed by numerically integrating the induced EMF over an electrical cycle, accounting for the average path length and cross-sectional area of the core. Loss curves can be constructed by subtracting the copper loss in the primary coil from the total real power consumed. B-H curves for the 5 materials are shown in Fig. 5 along with a benchmarked laminated core, while loss curves are shown in Fig. 6. For convenience, a grain-oriented steel core has been used for the benchmarking process as this type of core is readily available ‘off-the-shelf’. Rotating machines use non-grain oriented steel which have a slightly lower saturation level, as discussed below.

Fig. 5. B-H curves for materials tested

Fig. 5 shows measured B-H characteristic of the laminated core agree to within 1.7% of data sheet values, giving confidence in the test procedure. The figure also shows that the steel wires exhibit quite different properties. The 0.95mm wire saturates at 0.29 T (@ 6000 A/m) while the 0.45mm wire saturate at 1.39T, almost 5 times higher. This is probably due to differences in the grade of steel and heat treatment of the wire. All the wires tested saturate at a significantly lower level than standard non grain-oriented electrical steel, which has a saturation ‘knee’ at approximately 1.5-1.6T. The mild steel wires therefore have lower flux carrying capability, as would

be expected from a non-silicon based steel, and such a material would not be used for a commercial machine.

The loss characteristics of the wires tested at 50Hz are shown in Fig. 6 (data points), along with curve fits to the data (continuous lines) which have been created using the Steinmetz equation (1).

Fig. 6. Iron loss at 50 Hz

By inspection of Fig. 5 and Fig. 6 it is clear that the 0.45mm bright drawn mild steel wire has the best combination of properties, with a high saturation point and low loss (comparatively). This wire was chosen for the prototype.

The measured Steinmetz coefficients for the 0.45mm wire are shown in Table II, and are used for the iron loss model throughout the remainder of the paper. Due to the low frequency of the generator and small diameter wire, only the hysteresis loss is considered in the analysis.

$$P_{iron} = k_h B^\alpha f^\beta + k_e B^\gamma f^\delta \quad [\text{W/kg}] \quad [1]$$

TABLE II
STEINMETZ PARAMETERS

k_h	0.3
α	1.6
β	1.0
k_e	0.0

III. METHODOLOGY

A WIC structure is difficult to model using a standard 3D FEA approach due to the extremely fine mesh that is required to capture the thin steel wire strands. Two alternative approaches are examined in this paper:

1) Method 1

Method 1 treats the thin wire as a lamination with identical material properties and fill factor to the wire, but laminated in the tangential direction (like a strip-wound clock spring) rather than in the axial direction (like a stack). Treating the WIC structure as a strip-wound lamination allows the generator to be analyzed using standard 2D FEA methodology, which is highly attractive due to its speed and simplicity.

The model assumes that flux entering the stator will see the plane of the lamination rather than the edge, as shown in Fig. 7 (a). As with the wire coreback, flux will tend towards the lowest reluctance path by travelling in the lamination closest to the air gap. When this lamination becomes saturated the flux is forced into the next outer lamination, in a similar

fashion to the wire coreback. This phenomenon also occurs near the end-windings of large machines where stray flux can penetrate a lamination stack from the axial direction, and can therefore be modelled with most FEA packages.

Method 1 tends to underestimate the level of saturation in the steel. Any magnetic flux entering a lamination from the planar direction will set up an eddy current in lamination that is not present in the wire, and which tends to shield the lamination from high flux densities. In addition, the wire strands can axially concentrate flux from the magnet (see Fig. 7 (b)) in a way that is not available to the strip-wound lamination. These effects tend to increase the level of saturation in the wire, as depicted in Fig. 8-9.

Fig. 7. (a) Method 1 model - flux enter the planar lamination, setting up an eddy current; (b) WIC structure showing axial collection of flux which tends to increase the saturation of the wire

Fig. 8 gives a visual illustration of the difference in flux density distribution between the lamination model and the wire model. Fig. 9 compares the flux density profile across the center of each coreback shown in Fig. 8.

Fig. 8. Contour plot of flux density in the stator coreback showing (a) 0.45mm steel (WIC), and (b) a strip-wound lamination (Method 1 model)

Fig. 9. Flux density across the center of the stator coreback in Fig. 8, showing 0.45mm iron wire, 0.45mm strip-wound laminations (Method 1 model), and a lumped flux model (Method 2 model)

Fig. 8-9 show how the strip-wound lamination model captures the overall shape of the flux density distribution in the coreback but does not reproduce the high level of saturation in the wire. The wire coreback is significantly more saturated than the strip-wound lamination model prediction.

2) Method 2

Method 2 uses 3D FEA to model a small section of the generator in detail – including individual wire strands – to obtain a 2D profile of the flux density waveform in the air gap. This profile can then be used to calculate machine parameters and performance using analytical expressions, which are developed in the remainder of this section. Due to the large air gap inherent with large diameter machines, the armature reaction is relatively small and is not included in order to simplify the 3D FEA model. In addition, the model must be axially short to minimize the large mesh required to capture individual wire strands, as shown in Fig. 10.

Fig. 10. Method 2 model: Axially short, quasi-2D model of the WIC coreback, in which individual wire strands are modelled

Fig. 11 shows the 2D air gap flux density profile generated by the 3D FEA shown in Fig. 10. The Method 2 model uses analytical expressions based on a sinusoidal distribution of flux density in the air gap, which is of equal magnitude to the 3D FEA, as shown in Fig. 11. For comparison, the flux density profile from the strip-wound lamination model (Method 1) is also shown.

Fig. 11. Flux density profile in the air gap across a single pole pitch

The Method 2 model assumes the flux travels within a thin section of the WIC coreback closest to the air gap, as depicted

in Fig 12. In these regions it is assumed that the wire is uniformly saturated at the peak level calculated by the detailed FEA model. This flux density distribution is depicted in Fig. 9.

Fig. 12. Method 2 lumped flux model

It is clear that not all the flux emerging from the magnets will find its way into the stator coreback, and a global flux reduction factor, k , is used to account for the leakage. k is computed by comparing the peak air gap flux density from the detailed 3D FEA model with an idealized magnetic circuit that assumes no leakage. The value of k can then be used to calculate the iron loss in the generator (see Section II.2). For an idealized magnetic circuit with no leakage, the peak air gap flux density is given by:

$$\hat{B}_a = \frac{\frac{B_r t_m}{\mu_r - \mu_{mag}} \frac{1}{2} \sigma_m \left(\frac{R_o B_s}{\mu_r - \mu_{stat}} + \frac{R_i B_r}{\mu_r - \mu_{rot}} \right)}{\frac{t_m}{\mu_r - \mu_{mag}} + (t_c + c)} \quad [2]$$

where the flux density and permeability of the rotor and stator corebacks are found from FEA.

The air gap flux density distribution around the machine is then given by:

$$B_a(\theta) = k \frac{\hat{B}_a}{\pi} \sin\left(\frac{\sigma_m}{2}\right) \sin(p\theta) \quad [3]$$

The flux reduction factor, k , is calculated to ensure the peak flux density in the air gap of the analytical model equals the value obtained from the detailed 3D FEA model. For the prototype machine, k was calculated to be 0.72.

3) Back EMF calculation

The flux density profile across an armature coil is shown in Fig. 13. The turns distribution of the coil linking the magnet flux is given by:

$$N(\theta) = \frac{N\left(\theta - \frac{\sigma_o}{2}\right)}{\frac{\sigma_o - \sigma_i}{2} - \frac{\sigma_i}{2}} \quad [4]$$

The total flux linking the coil can then be found from:

$$\hat{\lambda}_{total} = (\hat{\lambda}_{bore} + \hat{\lambda}_{coil}) \quad [5]$$

where:

$$\hat{\lambda}_{bore} = \frac{2RN\hat{B}_{FEA}}{p} \sin\left(p \frac{\sigma_i}{2}\right) \quad [6]$$

$$\hat{\lambda}_{coil} = \frac{RN\hat{B}_{FEA}}{2} \left[\frac{1}{p} \left(\frac{\sigma_i}{2} - \frac{\sigma_o}{2} \right) \sin\left(p \frac{\sigma_i}{2}\right) + \frac{\sigma_i^2}{8} \cos\left(p \frac{\sigma_i}{2}\right) - \frac{\sigma_o^2}{8} \cos\left(p \frac{\sigma_o}{2}\right) \right]$$

The back EMF can then be calculated from: [7]

$$EMF_{phase}(t) = 2\pi f n_s \hat{\lambda}_{total} \cos(\omega t) \quad [8]$$

Fig. 13. Flux density across coil

The predictions of back EMF from the two models are compared to measurements taken on a laboratory prototype in Fig. 19.

4) Iron loss models

The two modelling approaches treat the iron loss in the machine in different ways. The strip-wound lamination model (Method 1) calculates iron loss within the FEA package by using the Steinmetz loss coefficients measured in Section II.B. This is simple to implement and allows for an easy calculation but does not capture the high saturation inside the steel wire.

The detailed 3D FEA model (Method 2) does capture the high saturation inside the coreback. The modelling approach assumes the flux travels in a small cross-sectional area of the coreback close to the air gap, as shown schematically in Fig 12. The stator is therefore assumed to have a small effective radial thickness, and this increases the flux density in the material. In addition, the model assumes all the iron loss occurs in the stator rather than the rotor and that, due to the small wire size, there are no eddy currents present.

The radial thickness of the stator coreback is calculated by assuming conservation of flux around the magnetic circuit and applying the flux reduction factor obtained in Section II.1. Since the flux from the magnet splits in two across coreback, the effective stator thickness can be calculated by:

$$t_s = \frac{k\hat{B}_a w}{2\hat{B}_s} \quad [9]$$

where the peak value of flux density in the stator coreback, $\hat{B}_s$, is found from FEA. For the prototype machine, $\hat{B}_s = 1.335$ T.

The total iron loss in the stator coreback can be calculated using an amended form of the Steinmetz equation:

$$P_{iron} = M_s k_h f \hat{B}_s^\alpha \quad [10]$$

where the mass of saturated stator corback:

$$M_s = 2\pi R_s t_s l \rho_s \quad [11]$$

The two loss models are compared to measurements taken on the prototype machine in Fig. 22.

5) Inductance and Resistance

A reasonable estimation of phase inductance can be made by considering the armature coil to be sitting between two infinitely permeable iron boundaries, as shown in Fig. 14 (a). The flux density generated at the coil center, B_a , is assumed to be distributed in a linear fashion across the coil turns, as shown in Fig. 14 (b). A flux correction factor can then be included to account for leakage.

Fig 14. Inductance model showing (a) schematic of the coil between two iron boundaries and (b) the flux distribution across the coil.

The air gap flux distribution is given by:

$$B_a(\theta) = \hat{B}_a \left(\frac{\theta - \frac{\sigma_o}{2}}{\frac{\sigma_i - \sigma_o}{2}} \right) \quad [12]$$

where the peak air gap flux is given by:

$$\hat{B}_a = \frac{\mu_0 N i}{g} \quad [13]$$

Area across the coil distribution is given by:

$$A(\theta) = R_c \theta l \quad [14]$$

Turns distribution is given by:

$$n = \frac{N}{\frac{\sigma_o - \sigma_i}{2}} \quad [15]$$

Flux linkage across the coil can be evaluated by:

$$\lambda_{coil} = \int_{\sigma_i/2}^{\sigma_o/2} B_a(\theta) A(\theta) n d\theta \quad [16]$$

$$L_{coil} = \frac{\lambda}{i} = k_L \frac{\mu_0 N^2 R_c l}{g \left(\frac{\sigma_o - \sigma_i}{2} \right)^2} \left(\frac{\sigma_o^3}{48} + \frac{\sigma_i^3}{24} - \frac{\sigma_i^2 \sigma_o}{16} \right) \quad [17]$$

where k_L is the flux increase factor to account for leakage effects and is assumed to be 1.2 [16]. The phase inductance can be calculated from:

$$L_{phase} = L_{coil} \frac{n_s}{n_p} \quad [18]$$

The inductance calculation assumes there is no difference between the d and q axis inductances, and in practice this is a good approximation since the q-axis flux travels through the same saturated wire strands as the d-axis flux. A comparison between measured and calculated values from the two models is shown in Table III. Phase resistance can be calculated by considering the coil geometry and fill factor and is not discussed in detail here.

TABLE III
COMPARISON OF PHASE RESISTANCE AND INDUCTANCE

	METHOD 1	METHOD 2	MEASURED
Inductance, d-axis (mH)	32.7	13.5	15.5
Inductance, q-axis (mH)	33.6	13.5	15.9
Phase resistance (Ω)	5.1	5.1	5.6

6) Terminal voltage, power and efficiency

The Method 2 model assumes power is delivered to a resistive load at unity power factor. Given this condition, the terminal voltage, power and efficiency can be calculated by:

$$V = \sqrt{(EMF)^2 - (IX_{ph})^2} - IR_{ph} \quad [19]$$

$$P = 3VI \quad [20]$$

$$\eta = \frac{P_{out}}{P_{out} + P_{cu} + P_{iron} + P_{mech}} \quad [21]$$

IV. PROTOTYPE GENERATOR

A list of parameters for the prototype generator is given in Table IV; construction of the prototype WIC generator is depicted in Fig. 16-Fig. 18. The armature coils were held in a former (Fig. 16 (b)) and then wound with 0.45mm steel wire around the outer side of the coils (Fig. 18). An epoxy resin was painted onto the wire as it was laid down. Glass fiber matting was then added to build up the bulk material around the outside of the wire to form the full load-bearing composite structure, which can be seen in Fig. 17. The generator is shown on the test rig in Fig. 18.

TABLE IV
MACHINE PARAMETERS

Power (W)	2000
Nominal speed (rpm)	200
Pole number	24
Coil number	18
Nominal frequency (Hz)	50
Magnet grade	NdFeB N35
Magnet axial length (mm)	100
Magnet tangential width (mm)	40
Magnet thickness (mm)	10
Magnet pitch/pole pitch ratio	0.62
Rotor OD (mm)	514
Stator ID (mm)	520
Running clearance (mm)	3.0
Stator coreback thickness (mm)	15.0
Stator coreback fill factor	0.70
Coil height (mm)	8.0
Coil width (mm)	25.0
Copper wire diameter	0.75
Number of turns	230
Armature fill factor	0.68
Machine OD (mm)	635
Machine axial length (mm)	154
Air gap diameter (mm)	517

Fig. 15. Generator construction showing (a) magnets being place on the rotor, and (b) stator coils on a former before winding

Fig 16. Coreback being formed by winding steel wire with epoxy

Fig. 17. Completed stator showing wire coreback

Fig. 18. Generator on test bed

V. RESULTS

The generator was tested in the laboratory, feeding a 3-phase resistive load bank at unity power factor. Power output and torque were measured using a PPA4500 3-phase power analyzer and Magtrol 100Nm torque transducer. Voltage waveforms were captured using 4 channel TDS 2024 oscilloscope.

A comparison between the measured back EMF and the predictions from the two models is shown in Fig. 19.

Fig. 19. Phase back EMF at 75 rpm

The generator was tested at three nominal operating speeds: 84rpm, 133rpm and 214rpm. Predictions of power output and voltage are compared to measured values in Fig. 20 and Fig. 21.

Fig. 20. Power output

Fig. 22 compares predictions of no-load iron loss with measurements taken across the speed range. A dummy rotor was used to separate the bearing and windage loss from the iron loss. Only the iron loss is present in Fig. 22.

A comparison between measured and predicted values of efficiency across the power range is shown in Fig. 23. The measured mechanical loss was added to the model predictions of copper and iron loss in order to create a meaningful comparison.

VI. DISCUSSION

It is clear from Fig. 19 that modelling a WIC structure using 2D FEA – as a strip-wound lamination (Method 1) – is less accurate than using an analytical approach based on a 3D FE analysis of a small section of the generator (Method 2). The difference in accuracy between the two models can be seen clearly in the predictions of terminal voltage and output power across the speed range, Fig. 20 and Fig. 21. At rated speed the Method 1 model over estimates terminal voltage by 23% and the power by 32% compared to an error of 3.5% and 3.6% respectively for the Method 2 model.

These over-estimations arise because the strip-wound lamination model (Method 1) assumes the stator coreback is less saturated than it actually is. This leads to a prediction of higher air gap flux density (as shown in Fig. 11) and a higher resulting back EMF. Indeed, when the measured back EMF is compared to prediction in Fig. 19, the Method 1 model overestimates the peak back EMF by 11.4%, compared to an error of only 2.6% for Method 2.

Fig. 22 shows the Method 2 model gives an accurate prediction of iron loss in the wire coreback with an error of 1.1% at rated speed, while the Method 1 model underestimates the loss by 9.0%. The flux density profiles across the stator coreback for the two models are shown in Fig. 9. The Method 2 model assumes the coreback has a small effective radial thickness which is highly saturated, while the simple 2D FEA (Method 1) assumes the flux is more evenly distributed across the full width of the coreback but at a lower saturation level. Although both models give reasonably accurate predictions of iron loss through the speed range, Method 2 is likely to be more accurate if a high grade electrical steel is used in place of mild steel. Fig. 5 shows that the B-H characteristic of electrical steel has a much sharper ‘knee’ than mild steel and this leads to a sharper discontinuity of flux density across the coreback, as shown in Fig. 2 and Fig. 3. Method 2 assumes that all the flux is carried in a narrow band of saturated wire strands close to the air gap, and this is more likely to be accurate if an electrical steel is used.

The efficiency of the generator is shown at rated speed in Fig. 23. Since the Method 1 model underestimates the loss in the generator, it also overestimates its efficiency. The efficiency at the rated power point was measured at 82.0%, compared to a predicted efficiency of 84.4% from the Method 1 model and 81.8% from the Method 2 model. Method 1 is less accurate because it does not capture the highly saturated nature of the stator coreback, as described above. Although the measured efficiency of 82.0% is not particularly high for a PM machine, the total iron loss expressed as a percentage of the output power (at max power) is 6.4%. This value could be reduced significantly if an electrical steel were used in place of mild steel for the construction of the WIC coreback. However, the measured iron loss is not excessively high given the stator is constructed from mild steel, and this is testimony to the careful testing procedure and wire selection outlined in section II.B.

VII. CONCLUSION

This paper has examined two alternative approaches for modelling wound-iron composite structures: a 2D FEA model

which treats the wire as a strip-wound lamination (Method 1), and a quasi-analytical model based on modelling a small section of the generator in detail (Method 2). It is clear from the test data that the Method 2 approach provides a much more accurate prediction of performance. Although this model is more accurate it is also significantly more time consuming, since a small section of the wire coreback must be simulated in detail using 3D FEA for each design.

An interesting further area of study would replace the mild steel wire with an electrical steel wire. The two modelling approaches could then be benchmarked against a prototype that was much closer to a commercial machine.

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