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Test Characterization of a High Performance Fault Tolerant Permanent Magnet Machine

Jason M. Yon, James L. Baker, Phil H. Mellor, Sam J. Williamson and Rafal Wrobel
Faculty of Engineering, University of Bristol, United Kingdom, jason.yon@bristol.ac.uk

Abstract— It is well understood that an electric machine’s output performance is limited by its losses and thermal behavior. For novel prototype machines, hardware testing processes are an important part of quantifying these parameters. For some machines, effective characterization may be accomplished using a series of static and simple prime-mover tests. The resulting data permits calibration of loss- and thermal-models. These can then be used to predict on-load performance. Fault-tolerant machines based on single layer winding arrangements are designed to minimize interaction between windings or module-groups. This paper demonstrates that, for such a machine, the losses measured during simple DC and primer-mover tests may be used to infer performance during both ‘healthy’ and ‘faulted’ operating modes. Under faulted conditions the total machine loss is expected to be a combination of module-specific and common losses, which can be directly deduced from hardware tests. This paper discusses the accuracy of loss superposition when applied to a 180 kW multi-channel, fault-tolerant aerospace machine. From observations following faulted and healthy dynamometry tests, there exists close correlation between full-load performance and estimates made from the superposition of losses under discrete operating modes.

Keywords— *Characterization of electrical machines, dynamometer evaluation, permanent-magnet machines, electrical fault-tolerance.*

I. INTRODUCTION

The aerospace industry is developing the More-Electric-Aircraft (MEA) where pneumatic, hydraulic and mechanical systems are being replaced with electric alternatives. This evolution is expected to offer efficiency improvements and reduce operating costs. Electric machines for aerospace applications need to have high specific outputs to minimise mass. Additionally, designs often have to meet strict reliability and fault-tolerant criteria. The application studied in this work is a helicopter electric tail rotor drive. As this is a safety-critical aerospace system, an architecture based around four independent power ‘channels’ was specified [1]. Consequently, the electric machine construction consists of electromagnetically isolated stator coils, grouped to form four independent three-phase modules around the stator annulus. A single-layer concentrated winding is employed to minimise interaction and fault-propagation between the modules [2], [3]. A winding of this nature provides magnetic and electrical isolation between phases, thereby allowing independent operation with negligible interaction. The stator-coils were

grouped into 3-phase sets which allowed conventional inverters to be used, thereby making the design easier to validate than fault-tolerant topologies requiring, poly-phase inverters or separate drives for each winding. A machine winding like the one presented here, which is grouped into multiple-phase sets is known as “machine modular” topology; whereby independent phase coils driven from separate inverters is known as “phase modular”.

The key design specification parameters of this fault-tolerant, Permanent Magnet (PM) machine are detailed in table I. Using these requirements, a joint electromagnetic and thermal design study was conducted and an illustration of the machine’s active components is presented in Fig. 1. The pertinent features of this design can be identified, which include an open-slot stator structure to ease manufacture and repair; a single-layer winding for fault-tolerance and minimal active material to maximise specific output.

TABLE I MACHINE SPECIFICATION PARAMETERS

Continuous rated power	180 kW
Rated speed (fixed)	1670 rpm
DC-link	270 VDC [4]
Cooling arrangement	Totally-enclosed, air-cooled
Stator design	Open-slot, single-layer concentrated winding configured into four “machine modular” groups
Synchronous Inductance	1 P.U.

Fig. 1. Cut-away view of the prototype’s active elements

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To validate the design, a full-scale prototype was constructed. This initial prototype utilises a simplified mechanical housing to facilitate test-stand integration. However, the prototype does incorporate axial fins to mimic the application's cooling regime. When most electrical machines are realized in physical form, 'build factors' cause the electromagnetic and thermal behaviour to deviate from original predictions. Often, the consequence of this is an increase in loss and a reduction in thermal capability. Therefore, for any novel machine topology, experimental evaluation is an essential process in determining performance.

II. EXPERIMENTAL REGIME

A. Existing Methods for Characterising Electrical Machines

A range of experimental processes and test methods may be employed during the development of an electric machine. These may include static electrical tests, dynamometry, calorimetry and sub-assembly evaluation [5], [6], [7]. Some experiments contribute directly to the validation of specification requirements; some develop confidence in the physical system, e.g. 'shake-down' and insulation tests; whereas others confirm or calibrate analysis developed during the design synthesis. Therefore, the particular test regime adopted needs to be appropriate for the machine topology, its speed or power rating, the regulatory framework relevant to the application and the product's Technology-Readiness-Level (TRL). The brief for this project mandated extensive dynamometer testing. However, this experimental platform allows a comparison between these conventional tests and lower-overhead characterisation techniques to be made.

Testing requirements and processes are well standardised for industrial induction motors and wound field synchronous machines, [8]. This is broadly due to these designs being mature and well-understood in industry. They are also mass-manufactured and undergo automated testing during production. Although the underlying principles of testing rotating machinery is transferrable, there are very limited published standards for the evaluation of PM machines. The recent IEEE document Trial-Use Guide for Testing Permanent Magnet Machines, [9], published in 2015, is an adaptation of [8] and specifically caters for PM machines. Although this guide is very well suited to production-line testing and proofing of designs, it readily acknowledges that it is not applicable to research environments. Furthermore, it is restricted in recommending characterization processes for PM machines which are particularly complex or unusual in design, such as the case study presented here.

In common with most rotating machinery, dynamometry underpins much of the characterization of PM topologies. Conventionally, the Machine Under Test (MUT) is spun under its own power and a controlled opposing torsional load is applied to the shaft via an appropriate load machine. This approach, known as "direct testing", deduces the performance and therefore efficiency of the machine via input and output power measurements. For full dynamometer characterization, the minimum rating envelope of the test system must therefore be equal to that of the test machine, plus or minus any losses

depending on whether the MUT is to be characterized as a generator or motor respectively.

"Indirect testing" is based on the principle that electric machine losses under rated operating conditions can be inferred through relatively simple tests. The standards IEC 60034-2 [10] and IEC 60034-29 [11] prescribe methods for the indirect testing of electric machines to determine efficiency and temperature rise under rated operating conditions. However, these standards are not currently applicable to PM machines where rotor-flux is constant. Further, in applications with very dynamic duty profiles, characterising the machine under rated operating conditions is not necessarily sufficient and a more comprehensive approach is required.

B. Proposed Experimental Method

It has been reported that some PM machines can be characterized by undertaking static DC and reduced-complexity prime-mover tests. DC tests involve passing a controlled DC-current through the machine coils and recording both the electrical power and internal temperature distribution at thermal equilibrium. Simple prime-move tests rotate the MUT with open-circuit terminal connections or short-circuit connections [5]. The mechanical power required to rotate the MUT under these conditions is recorded as a function of speed. Evaluations of this nature are significantly simpler than full dynamometer characterization, however they can still capture sufficient data to understand bulk machine losses and subsequent temperature rises. This recorded data permits model calibration and, in principal, allows machine performance to be predicted under any operating conditions

The suitability of single-layer concentrated windings for fault-tolerant applications has also been previously reported [2], [3]. This winding configuration is inherently well suited to minimising undesirable interactions between separate modules, or power-channels, during faulted operation. Further, the scale of thermal and electro-magnetic interaction between the windings can be examined by conducting these simple tests. The machine-modular 3-phase sets and inverter configurations employed within this prototype is Fig 2.

Fig. 2. Inverter connections to independent three-phase machine modules

The presented work intends to supplement existing research in the fields of loss-separation and fault-tolerance by exploring the validity of DC, short-circuit and open-circuit characterization to a novel, 4-channel, fault-tolerant machine. Other PM machines have been successfully characterized using simpler prime-mover-only techniques [5] where machine loss and thermal models are calibrated from static DC tests and several prime-mover based dynamometer measurements. It is proposed that, the machine's performance under faulted operation (one or more modules open-circuit or short-circuit) can be extrapolated from the superposition of 'common losses' and 'module-specific' losses measured during these simple tests. The unique attributes of single-layer windings suggest that tests upon individual phases, or machine-modules may be used to extract module-specific losses and therefore entire prototype performance, further simplifying the types of test required.

A key attraction of these simplified methods is that test-system ratings for open-circuit and short-circuit tests are much lower than that needed to support rated motoring conditions of the MUT. With the prototype's high power rating and four-channel system architecture, the development of a suitable conventional dynamometer system is a complex and resource-intensive exercise. Further, the reduced loading associated with prime-mover tests enables torque transducers with a lower rating to be employed, potentially enabling an overall improvement in measurement resolution and subsequently higher confidence in test-data accuracy. A comparison between test system requirements for full motoring tests and prime-mover-only tests is summarized in Table II.

TABLE II PER-UNIT ELECTRICAL AND MECHANICAL RATINGS FOR DIFFERENT TESTS ASSUMING >95% EFFICIENCY AT RATED OPERATION

Test type	Max torque (PU)	Max shaft power (PU)	Max current (PU)	Max electrical power (PU)
Open-circuit	< 0.05	< 0.05	0	0
Short-circuit	0.5	< 0.05	1	0
Conventional dynamometry	1	1	1	1

The simplified estimates presented in Table II illustrate that reduced-complexity tests are much less arduous, in terms of installed capability, than full dynamometry. During open-circuit evaluation the level of mechanical power is expected to be low. The dominating loss components in this scenario are that of the prototype's bearings, aerodynamic effects, stator laminations and rotor structure. For the prototype studied here, the maximum open-circuit losses are less than 5% of rated. In terms of short-circuit operation, the machine has been designed to have a synchronous inductance of 1 P.U. Therefore the short-circuit current is intrinsically limited to that of rated current, which is a prerequisite for fault-tolerance. This subsequently limits the torque under short-circuit conditions to no more than 50% of rated. A thorough breakdown of machine loss mechanisms and their relationships are given in Section III.

Unlike proof, or production-line testing, it is important to consider the risk of failure when investigating a unique

prototype machine. Often realizing a bespoke design is resource intensive and costly, therefore a test plan which systematically captures data is preferred. This allows the chief loss mechanisms and thermal behavior to be captured incrementally which directly informs or confirms the assumptions made during design synthesis. Should a failure occur, important parameters will have already been assessed, therefore reducing project risk. An indication of test complexity and associated risk of MUT failure is provided in Table III for common test-types.

TABLE III TEST COMPLEXITY AND RISK OF DAMAGE TO PROTOTYPE

Test	Relative test complexity	Risk of mechanical damage or failure	Risk of thermal damage or failure	Risk of electrical damage or failure
Static DC	Low	Low	Med	Low
Open-circuit	Med	Med	Low	Med
Short-circuit	Med	Med	High	Low
Conventional dynamometry	High	High	High	High

The relative testing complexity is attributed to the system requirements, or cost, and any (re)configuration work that needs to be addressed. For static measurements which can be conducted with simple laboratory apparatus, the complexity involved is relatively "low". However, for prime-mover tests, the installation and commissioning requirements to produce a suitably-rated evaluation platform is much higher. Both open- and short-circuit test regimes can utilize the same test-system, i.e. a prime-mover operating in speed-control and a suitably rated torque-transducer. These tests are therefore allocated a "medium" level of complexity. Conventional dynamometry requires not only a fully-rated prime mover and torque-transducer but also additional driving inverters for the MUT. Complexity rises further when considering test automation, safety interlocks and data capture requirements.

Risk levels associated with each test-type stem from the likelihood of a MUT failure. This probability is strongly linked with test-complexity and the stress-interaction between thermal, mechanical and electrical domains. For simple static measurements, the relative risk of damage is low. This is predominantly due to the test being simple to regulate and monitor. Thermal risk is medium due to the prototype being exercised in this domain. Rotating tests, by their nature, impart mechanical stress to the system. In terms of open-circuit evaluation, the windings may experience full terminal voltage due to back-EMF, although the resulting temperatures are not expected to be high. Conversely, for short-circuit tests, the stresses experienced are increased due to high thermal demands on the shorted stator windings. Full dynamometry provides the greatest risk to the MUT, even under part-load conditions. The interaction of stator and rotor magnetic fields produce high mechanical forces, which is exacerbated with increasing speed. Additional mechanical risks may be experienced due to resonant-modes of the stator and/or rotor being excited. Inverter

switching waveforms significantly increase the risk of electrical failure by stressing the winding's insulation and encouraging partial-discharge. Further, the potentially high stator currents and any rotor-loss lead to significant thermal stress. It is this multiple combination of contributing factors, along with their interaction in different domains which yield the highest danger of damage or failure.

C. Dynamometer Test Facility

In accordance with the project brief, a large-scale dynamometer facility (Fig. 3) was constructed to evaluate the fault-tolerant prototype at continuous, full-power operation and validate the presented research hypothesis. This platform is capable of performing the array of rotating tests previously outlined, along with continuous evaluation at 200 kW, 1.2 kNm. The test-setup comprises the following components: an inverter-driven prime-mover; the prototype under test; four independent 'MUT inverters' each of which is connected to a single machine-module; a bi-directional electromechanical DC supply to generate the prototype's 270V dc link; cooling fan and duct arrangement to mimic the application's cooling arrangements; and a multi-channel data acquisition system. The data acquisition system is a *Genesis GEN7tA*, capable of capturing mechanical power and four sets of three-phase current and voltage waveforms in synchronism and at very high sampling rates.

III. EXPERIMENTAL SEPARATION OF LOSS MECHANISMS

A. PM machine losses

PM machine losses tend to follow well-established analytical formulae in terms of their dependency towards temperature, speed and current [12], [13]. Empirical calibration of these formulae and any thermal models used during the design synthesis allows the aforementioned 'build factors' to be accounted for. This therefore allows accurate predictions of machine loss, temperature-rises and subsequent performance under most operating conditions. The machine topology studied here adds a complication to this process in that the four different modules may be operated in different states, either faulted-open-circuit, faulted-short-circuit or under normal inverter control. The basis of the presented analysis is an assumption that loss components can be separated into 'module-specific losses', directly attributable to each of the four machine segments, and 'common losses' not directly influenced by the individual module states. This approach relies on linear superposition of loss terms and assumes that first-order thermal and electromagnetic interactions between machine segments is negligible. A categorised identification of PM machine loss mechanisms is given in table III. Those mechanisms that can be considered module-specific are also indicated, along with the variables on which analytical formulae are based.

Fig. 3. Dynamometer installation, (centre); MUT coupled to load dynamometer machine, (bottom-left); Data acquisition system, (top-left); Individual inverters to power the 4 fault-tolerant MUT channels, (top-right)

TABLE IV ANALYTICAL REPRESENTATION OF INDIVIDUAL PM MACHINE LOSS MECHANISMS FOR THE PRESENTED PROTOTYPE

Loss Category	Loss Mechanism	Nomenclature	Common/ Module-Specific	Analytical Model Variables*
Mechanical loss	[I] Bearing	P_{mech}	Common	n
	[II] Windage		Common	n^3
Iron loss	[III] Main flux path, speed dependent	P_{FE1}	Module-Specific	$n, I_d, I_{ph}, \theta_{FE}, \theta_{PM}$
	[IV] Main flux path, speed squared dependent	P_{FE2}	Module-Specific	$n^2, I_d, I_{ph}, \theta_{FE}, \theta_{PM}$
	[V] Demagnetizing flux path	P_{FE3}	Module-Specific	$n^2, I_d, \theta_{FE}, \theta_{PM}$
Winding loss	[VI] DC ohmic	P_{CU1}	Module-Specific	I_{ph}, θ_{CU}
	[VII] Load dependent AC	P_{CU2}	Module-Specific	n^2, I_{ph}, θ_{CU}
	[VIII] Rotor induced AC	P_{CU3}	Common	$n^2, \theta_{PM}, \theta_{CU}$
Rotor loss	[IX] Magnet loss	P_{magnet}	Common	n^2, θ_{PM}

* θ_{FE} , = iron temperature; θ_{PM} = magnet temperature; θ_{CU} = winding temperature; n = speed; I_{ph} = phase current & I_d = d-axis current; it should be noted that the speed-dependent coefficient for field-weakening iron-loss was found to be negligible in this prototype and hence not considered.

Referring to the individual loss components of Table IV, mechanical losses, which predominantly consist of bearing-friction and aerodynamic drag may be represented as (1). These have been assumed to be unaffected by individual module ‘states’ and thus are deemed “common losses”.

$$P_{mech} = k_{mech}n + k_{aero}n^3 \quad (1)$$

where: k_{mech} is the bearing loss coefficient;
 k_{aero} is the bearing loss coefficient.

Iron losses are modelled using well-established techniques as described in [5], [12], [13] and are represented using the trends presented in (2). These losses are dependent upon the operating conditions of the module and therefore are termed “module-specific losses”.

$$\begin{aligned}
 P_{FE1} &= a_h n \sqrt{1 + 2 \left(\frac{I_d}{I_{sc}} \right) + \left(\frac{I_{ph}}{I_{sc}} \right)} \\
 P_{FE2} &= a_j n^2 \left(1 + 2 \left(\frac{I_d}{I_{sc}} \right) + \left(\frac{I_{ph}}{I_{sc}} \right)^2 \right) \\
 P_{FE3} &= b_j n^2 \left(\frac{I_d}{I_{sc}} \right)^2
 \end{aligned} \quad (2)$$

where: a_h is the loss coefficient for the hysteresis component of the magnetizing flux-path;
 a_j is the loss coefficient for the eddy-current component of the magnetizing flux-path;
 b_j is the loss coefficient for the eddy-current component of the demagnetizing flux-path.

To compute the total iron loss, (2) is calculated for each module and then summed across the four independent three-phase sets. Magnet and stator iron temperatures are also applied by accounted for by estimating the reduction in PM flux and increase in electrical resistivity. The loss-coefficients, a_h, a_j

and b_j are adjusted accordingly to compensate for these variations.

Winding losses consist of a dc-Ohmic component, load dependent AC losses caused by high-frequency phenomena such as skin effect and, a further eddy-current component induced in the winding by the alternating rotor field, (3). The Ohmic and load-dependent AC winding losses are module-specific however, the eddy-current component is independent of the operating state and is thus treated as a common loss

$$\begin{aligned}
 P_{CU1} &= I_{ph}^2 R_{dc} \\
 P_{CU2} &= k_{ac} I_{ph}^2 n^2 \\
 P_{CU3} &= k_{ri} n^2
 \end{aligned} \quad (3)$$

where: R_{dc} is the dc phase winding resistance, including end-turns;
 k_{ac} is the loss coefficient for winding ac effects;
 k_{ri} is the loss coefficient for rotor-induced ac winding loss.

In the winding loss model, (3), winding resistance is varied with temperature, using the temperature-dependant coefficient of resistivity for copper. Further, the rotor-temperature is used to vary the PM flux which influences the rotor-induced loss.

The final loss component which has been modelled is that of the rotor. During rotation, slotting effects and interactions from the stator field cause the PM flux density to vary and generate eddy-current loss. This effect can be represented using (4) and is assumed to be a common loss, unaffected by the module operating states. This assumption is valid due to the magnitude of PM loss being comparably small for this particular machine design.

$$P_{magnet} = k_{pm} n^2 \quad (4)$$

where: k_{pm} is the eddy-current component of magnet loss.

TABLE V LOSS MECHANISMS ACCOCIATED WITH DIFFERENT TEST TYPES

Loss Category	Loss Mechanism	Parameters requiring calibration	Test Type				
			(1) Static DC	(2) Dummy Rotor	(3) PM Rotor & No Windings	(4) Open circuit	(5) Short circuit/ Inductive load
Mechanical loss	[I] Bearing	k_{mech}		Y	Y	Y	Y
	[II] Windage	k_{aero}		Y	Y	Y	Y
Iron loss	[III] Main flux path, speed dependent	a_h			Y	Y	Y
	[IV] Main flux path, speed squared dependent	a_j			Y	Y	Y
	[V] Demagnetizing flux path	b_j					Y
Winding loss	[VI] DC ohmic	R_{dc}	Y				Y
	[VII] Load dependent AC	k_{ac}					Y
	[VIII] Rotor induced AC	k_{ri}				Y	Y
Rotor loss	[IX] Magnet loss	k_{pm}			Y	Y	Y

The equations (1) through (4) are used with a coupled thermal model to deduce performance figures at thermal equilibrium. The combination of common and module-specific models allows operating conditions under different module ‘states’ and rotational speeds to be expressed analytically.

B. Loss model calibration

The loss model is calibrated through a succession of tests, each of which introduces additional loss components. In addition to conventional DC, open-circuit and short-circuit tests, further no-load tests have been conducted to extend the loss characterisation of this machine. A ‘dummy rotor’ test was undertaken whereby a mechanically representative, but magnetically inert rotor was installed in the machine. During prototype manufacture, a further test was undertaken where the magnetised rotor was installed before any phase windings were fitted. The stator’s open-slot nature means that a significant portion of the windings are exposed to the rotor magnetic-field. As such, rotor induced AC losses in the winding were of interest. Prime-mover tests with and without windings enable this loss component to be directly deduced. A thin search coil was fitted in the base of the stator slot during this test which provided an EMF value allowing rotor temperature to be inferred. Table V shows the loss mechanisms measured through each of these tests.

With reference to Table V, measurement of the DC winding resistance via static DC tests (Test 1) is straightforward. This test also allows the stator reduced order thermal model to be calibrated through well-defined power loss generation and measured temperature-rise.

Dummy rotor tests (Test 2) directly measures mechanical losses as a function of speed. The bearing and aerodynamic loss

coefficients can be deduced from these through a least squares regression fit (1).

Calculation of the PM rotor loss coefficient, k_{pm} , is achieved by calibrating the reduced order thermal model of the machine following Test 3. As such, this test must be run to thermal-steady-state. This machine used SmCo magnets with a temperature coefficient of remanence of -0.035 %/°C, [14]. This factor allows rotor temperature to be inferred through the EMF, measured by the aforementioned search coil. Monitoring the rotor and case temperatures until the system reaches thermal steady state allows the thermal time constant of the rotor-to-case temperature rise to be deduced. Eddy current losses in the magnet material will vary proportionally with the square of inducing field and inversely with the material resistivity which is +0.1 %/°C, [15]. The resultant temperature coefficient of rotor eddy current loss, α_θ , is therefore -0.17 %/°C.

During the machine’s design synthesis, a reduced order thermal model was established which estimated the lumped thermal capacitance of the rotor [1]. Combining this data with the rotor loss temperature coefficient, the experimentally derived thermal time constant and the steady state temperature rise allows the lumped thermal resistance to be calculated and the rotor loss can then be estimated.

The remaining loss measured (mechanically) in Test 3 can then be attributed to main flux path losses dependant on speed and speed squared. The hysteresis (a_h) and eddy current (a_j) coefficients can then deduced from these remaining losses through a least squares regression fit (2).

The open circuit test (Test 4) adds a single additional loss component to Test 3. The open-slot design of this machine means the stator winding are directly exposed to the rotor flux. Confirmation of this loss element was therefore necessary to validate the associated modelling conducted in the design phase.

The short circuit test (Test 5) adds the final two loss mechanisms i.e. the load dependent AC winding loss and the demagnetizing path iron loss. These terms are both dependant on the square of speed and cant therefore be directly separated. Instead, the AC winding loss is inferred through the thermal behaviour of the coil. Having calibrated the stator thermal model via Test 1, it is possible to calculate the total winding loss under short circuit conditions via the temperate rise between the winding and stator iron. The remaining loss can therefore be attributed to the demagnetizing flux path iron losses.

C. Dynamometry Comparison

Tests on the prototype demonstrate good correlation with loss models presented above. Fig. 3 shows the results of a broad range of tests combining open-circuit, short-circuit and inverter-driven modes. The total calculated losses are then normalised to the dynamometer measured loss. Test cases 1 – 3 are open circuit and inductive load tests used to develop and calibrate the loss models. Tests 4 – 6 show faulted operation where three modules are inverter controlled and one module has an open circuit fault. Tests 7 – 9 show faulted operation where three modules are inverter controlled and one model has a short circuit fault.

Although the application is fixed-speed, the presented tests have been conducted over a wide speed-range to illustrate the modelling approach's robustness. Further, a variety of 'healthy' current magnitudes are demonstrated in faulted modes to illustrate modelling outcomes as a function of torque demand. The agreement between loss models and dynamometer measurements shows good correlation, not exceeding 5% of discrepancy over the entire dataset. Under faulted operation

however, the models tend to overestimate the loss at low stator current levels, e.g Tests 4 and 7. The converse is true at higher output torque, where predictions are slightly lower than the measured loss, e.g. Tests 6 and 9.

Throughout this investigation, it has been assumed that the machine's isolated stator design presents negligible interaction between modules. It is however possible that subtle interactions caused by asymmetric electromagnetic and thermal distributions manifest themselves as unforeseen, and difficult to model losses. The severity of these losses may worsen at higher electrical loadings due to flux-path distortion and interaction between neighbouring modules.

Another potential cause of inaccuracy is the empirically-calibrated thermal model used to deduce 'on-load' stator copper loss. Inaccuracies within this frequency-dependant loss model may be caused by errors in placement of thermocouples and the thermocouple's accuracy themselves.

A systematic capture of data has been achieved in terms of separating mechanical loss using a dummy-rotor and identifying rotor-induced winding loss. This prototype is complex and large and has been assembled and disassembled several times throughout the presented tests. Although strict controls have been enforced to ensure test repeatability, it is possible that bearing losses and other mechanical factors may not be constant across all evaluations. This may cause additional prediction errors within the mechanical and winding-loss models but it should be noted that these particular losses are not dominant.

Fig. 4. Bar graph showing calculated losses for several test cases normalised to the measured loss. The key for the loss components is provided in table III. The test configurations are as follows:

- TEST 1: All modules open-circuited, 1670 rpm;
- TEST 2: All modules connected to inductive loads, 1670 rpm;
- TEST 3: Module 1 open-circuited and all other modules connected to inductive loads, 1670 rpm;
- TEST 4: Single 'open circuit fault', three modules operating at 50 A_{rms} , 1000 rpm;
- TEST 5: Single 'open circuit fault', three modules operating at 75 A_{rms} , 1000 rpm;
- TEST 6: Single 'open circuit fault', three modules operating at 100 A_{rms} , 1000 rpm;
- TEST 7: Single 'short circuit fault', three modules operating at 50 A_{rms} , 1000 rpm;
- TEST 8: Single 'short circuit fault', three modules operating at 75 A_{rms} , 1000 rpm;
- TEST 9: Single 'short circuit fault', three modules operating at 100 A_{rms} .

IV. CONCLUSIONS

The presented results have confirmed that minimal prime-mover tests can provide a valid and accurate characterization of a multi-channel, fault-tolerant machine topology. From a set of relatively straightforward tests, the full in-operation performance of a prototype machine can be deduced. Further, for a given machine, the installed rating of the test equipment needed to perform the characterisation is a fraction of that required using more traditional test methods. While subtle interactions as a result of the topology's faulted behaviour will lead to some minor distortions in performance predictions, prototype tests have shown good agreement with the original research hypothesis, i.e. that total losses may be estimated by a superposition of module-specific losses and common losses, even under faulted conditions. Undertaking open-circuit and short-circuit evaluation is far less arduous than full dynamometer testing due to reduced system requirements. Therefore, the presented method demonstrates significant time and resource savings over traditional characterization methods. Further, these prime-mover tests are able to exercise the machine through mechanical, electromagnetic and thermal stress thereby providing practical confidence in the test machine.

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