Coupled Electromagnetic and Thermal Design-Optimisation of a Traction IPM Machine with High-Torque Overload Capability

Haipeng Liu, Rafal Wrobel, Sabrina Ayat, Chengning Zhang

Abstract -- This paper presents a design exercise of an interior permanent magnet (IPM) machine with high-torque overload capability. The machine is intended for traction applications, where the intermediate transient operation is one of the key design requirements, alongside with the more common specification for the continuous operating duty. The design approach frequently adopted, when considering the transient overload, focuses solely on the continuous operation with appropriate design margins. Consequently, such method is often unreliable and requires a degree of experience or multiple design iterations need to be performed. In contrast, this work proposes a technique accommodating for the transient overload from the onset of the design process. The initial machine sizing is based on a coupled electromagnetic and thermal analysis combined with an optimisation routine. The developed design-optimisation system employs computationally efficient and accurate models informed from thermal tests on representative hardware subassemblies. Further to the design challenges associated with the specific operating duty, the materials and processes used in fabrication of the machine should assure here a cost-effective design solution. The theoretical findings show that proposed method is effective for designing machines for more demanding transient overload events, where both transient specific output and its duration are simultaneously considered.

Index Terms—Design methodology, coupled electromagnetic and thermal analysis, overload capability, interior permanent magnet machine, transient operation

I. INTRODUCTION

The ‘more electric’ technologies in application to clean and efficient transportation have been experiencing dynamic development, which is particularly evident when analysing evolution of the automotive industry [1]. The key enabling technology for the next generation of vehicles is an electric drive train with a traction electric motor being at the heart of the propulsion system. The electric motor must meet a series of frequently conflicting design requirements, some of which include: high power- or torque-density, high-efficiency, wide speed range, compact and lightweight format, low-cost, and others [1]. The permanent magnet (PM) AC machine topology is the most frequently chosen due its attributes enabling most of the above-mentioned

Fig. 1. An example of torque-speed envelope for a traction motor with continuous and transient operating duty.

design targets to be satisfied [2], [3]. It is important to note that other machines types have also been successfully implemented in commercial vehicle applications [4]. Nevertheless, in this paper the research focus has been placed on the PM machine technology. Fig. 1 presents an example of torque-speed envelope with two operating regimes, continuous and transient. Such design targets are frequently used when considering variety of traction applications.

There is a wide body of work related to design of electrical machines for the continuous operating duty, with variety of design methods employed [5]–[7]. However, machine design accounting for both continuous and transient operations has not had much attention. This is primarily due to challenges associated with a reliable analysis of thermal transients required to derive machine design as yet [8], [9]. It is important to mention that the peak transient torque overload is frequently required for vehicles operating in harsh/extreme conditions, e.g. off-road operation including the hill-start, high-incline ascent and others.

The maximum allowable winding temperature rise during an overload operation is the main limiting factor for the transient peak torque output capability. This is when neglecting current limit of the power inverter and saturation of the machine’s magnetic circuit. The design requirements describing specific torque overload targets usually include the transient peak torque and transient duty [10]. The combined machine’s continuous and intermediate thermal capabilities are frequently related to the characteristic conductor/winding current density, which defines the winding power loss for a pre-set limit for the winding temperature rise. To provide an insight into the relationship between the characteristic conductor current density and duration of transient overload, a simplified, example thermal analysis has been carried out, the detail of which is presented in appendix. It has been assumed here that the winding heat capacity, which was experimentally derived from tests on a representative stator-winding hardware, is sufficient to define the thermal transient. Also, the winding power loss assumes low-frequency (DC) loss component only. Fig. 2 shows results from the analysis with selected operating points, A and B, indicated. For a conventional machine built

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Haipeng Liu and Chengning Zhang are with the National Engineering Laboratory for Electric Vehicles, Beijing Institute of Technology, Beijing 100081, China (email: liuhpbit@gmail.com; mrcnzhang@gmail.com)

Rafal Wrobel is with the Centre for Advanced Electrical Drives, Newcastle University, Newcastle upon Tyne NE1 7RU, UK (email: rafal.wrobel@newcastle.ac.uk)

Sabrina Ayat is with the Electrical Energy Management Group, University of Bristol, Bristol BS8 1UB, UK (email: s.ayat@bristol.ac.uk)

Fig. 2. Variation of the characteristic conductor current density versus duration of transient overload for the averaged winding temperature rise equal to 95°C with water jacket temperature equal to 70°C.

with active heat extraction using a water jacket housing, the characteristic conductor current density is usually equal to approximately $8 A_{rms}/mm^2$. The results suggest that ‘short’ transient high-torque overload is relatively easy to achieve, e.g. intermediate torque increase by factor of 3 is sustainable for 30 s and requires $24 A_{rms}/mm^2$ (operating point A). This may be one of the reasons why ‘short’ transient high-torque overload capability has not had much attention when it comes to designing of electrical machines for traction applications. However, if ‘long’ transient high-torque overload is required, e.g. intermediate torque increase by factor of 3 for 60 s (operating point B), then the winding would exceed the insulation rated temperature and consequently the characteristic current density is reduced here to $17 A_{rms}/mm^2$. The reduced current density for operating point B corresponds to approximately 30% lower torque overload as compared with operating point A. This simple example highlights the challenge associated with designing of high specific output density electrical machines with ‘long’ transient high-torque overload capability.

This paper presents a coupled electromagnetic and thermal design-optimisation exercise of a traction IPM machine. The main research objective was to develop a design approach allowing to meet the demanding high-torque overload design targets while satisfying other requirements like high specific output density, low-cost and low-format. A vital element of the work was careful selection of materials and processes, which are essential for cost-effective in-volume manufacture. To assure a reliable thermal design, some of the key thermal parameters, including the winding equivalent thermal conductivity, stator-to-winding equivalent contact thermal conductivity and winding volume-specific heat capacity have been informed from hardware thermal tests. Outcomes from the design-optimisation are discussed together with a more detailed analysis of a down selected machine variant for alternative overload scenarios.

II. DESIGN REQUIREMENTS

A set of design requirements for the traction motor is listed in Table I. The main feature of the motor is to provide a high-torque transient overload, which needs to be sustained for a relatively long duration. The overload requirements assume that the machine is capable of operating at a peak transient working point for 60 s starting from a reference temperature, which is equal to the water-jacket temperature, 70 °C. The average allowable winding temperature is set to be 165 °C, which gives a 15 °C temperature margin to accommodate for the winding hot spot assuming class H (180 °C) electrical insulation system [21]. Further design requirements include a partially fixed mechanical space

TABLE I
DESIGN REQUIREMENTS OF THE TRACTION MOTOR

Stator outer radius	90 mm
Maximum rotation speed	11,000 rpm
Continuous torque	62 Nm (0-4,600 rpm)
Continuous power	30 kW (4,600 ~11,000 rpm)
Transient peak torque	183 Nm (0~1,900 rpm)
Transient peak power	36 kW (1,900 ~11,000 rpm)
Transient duration	60 s
DC link voltage	340 V
Cooling type	Water jacket (Active)
Coolant temperature	70 °C
Electrical insulation class	H (180 °C)

envelope with the stator outer diameter equal to 180 mm. The motor design should aim for low-volume compact package utilising common active materials allowing for low-cost solution.

III. DESIGN METHODOLOGY

As discussed in the previous section, the main design challenge is to meet the high-torque overload design target while satisfying other performance measures in the smallest possible package. In order to account for the multiple design requirements a design-optimisation system has been constructed, a top-level schematic of which is presented in Fig. 3. The aims and variables in the coupled electromagnetic and thermal optimisation procedure are carefully selected to accelerate the optimisation process. The active length of the machine is not considered in the optimisation process. An optimal machine’s active length has been found iteratively. It is assumed here that the initial active length of the machine is equal to 145 mm, which is determined based on a simplified sizing equations. This allows for a complicated fitness function with many weight coefficients to be avoided in the optimisation [11]. The focus of the optimisation process is placed on the machine’s cross-section geometry for a selected operating duty. Here, the most demanding operating point is considered, peak torque at 1900 rpm, see Fig. 1. Details of the optimisation procedure are discussed in section VI. Since the winding turns number does not influence much on the low speed performance, the winding turns number is assumed to be 1 in the optimisation procedure and can be adjusted to satisfy the torque-speed envelope and supply voltage requirements at a later stage.

The thermal analysis is informed with experimental data from tests on representative hardware, which uses materials and processes intended for the complete machine assembly. The hardware fabricated and tested for the purpose of this

Fig. 2. A top-level schematic of the proposed coupled electromagnetic and thermal design methodology.

Fig. 3. Layout of the analysed IPM machine topology

Fig. 4. A varnish impregnated winding material sample with wire packing factor, $pf = 62\%$.

work includes the impregnated winding material samples and sectors of stator-winding subassembly, details of which are discussed later in the paper.

After the initial optimisation of the machine's geometry, a more detailed analysis is carried out, where number of turns are adjusted to satisfy the torque-speed envelope and supply voltage requirements.

IV. MACHINE TOPOLOGY AND MATERIAL

A 10-pole 12-slot IPM AC machine topology with double-layer fractional-slot concentrated winding and rectangular I-shape rotor has been selected for this design exercise, Fig. 4. Some of the advantages of this topology include relatively simple construction of the stator and rotor subassemblies. The stator-winding topology enables segmentation resulting in a simple and repeatable winding process with high conductor fill factor. The IPM rotor core pack acts as a retention system for the PM array, which uses rectangular PM blocks. Such rotor construction is also relatively simple to manufacture. Further to these the selected machine topology benefits from short end-windings and wide speed range capability.

The materials selected for the machine design include: electrical steel (SiFe, M270-35A) for the stator and rotor laminated core packs, mid-grade PMs (SmCo 26/10) and copper wire ($\phi = 0.8\text{mm}$ with class H Polyamide-Imide coating) forming a multi-stranded bundle of conductors per turn and electrical insulation system (Nomex 410 slot liner and appropriate varnish). Clearly the listed materials and components are standard and commonly used in construction of electrical machines and consequently allow for a cost-effective design. However, some of the drawbacks include here: higher power loss for SmCo as compared with equivalent NdFeB, issues with repeatable conductor lay for windings employing multi-stranded bundle and relatively poor thermal performance of the electrical insulation system.

V. THERMAL PARAMETERS

An accurate thermal data is the basis for a reliable motor thermal design and analysis. The majority of winding power loss is dissipated through the slot to the stator back iron and then to the actively cooled machine housing. There are three important thermal parameters related with winding heat transfer and winding heat storage, which can be derived from hardware thermal tests. The following subsections describe the individual parameters and procedures used to derive them.

A. Impregnated Winding Equivalent Thermal Conductivity across Conductors

To inform the design-optimisation process, three winding

TABLE II
MEASURED EQUIVALENT THERMAL CONDUCTIVITY OF
IMPREGNATED WINDING ($pf = 60\%$)

Impregnated winding, IM-I	1.56 W/m·K
Impregnated winding, IM-II	1.63 W/m·K
Impregnated winding, IM-III	2.04 W/m·K

TABLE III
SELECTED VARNISH MANUFACTURE DATA

Property	IM-I	IM-II	IM-III
Varnish type	Ultimég 2000-380	Elmotharm 073-1010	ELAN-protect up 142
Voltage rating,	-	1000V 190°C	1000V 209°C
dielectrical strength	>166 kV/mm	>80kV/mm 155°C	>235kV/mm 155°C
Solvent	Yes	Yes	No
Insulation class	H (180°C)	H (180°C)	H (180°C)
Curing temperature/time	4 hrs @ 130°C	4 hrs @ 160°C	1 hrs @ 150°C

samples impregnated using alternative varnishes have been manufactured and tested, Fig. 5. The winding equivalent thermal conductivity across conductors has been measured using a custom built heat flux meter following testing procedure described in [9]. The measured data supplemented with the selected manufacturer information for varnishes used is provided in Table II and Table III.

B. Stator-to-Winding Equivalent Contact Thermal Conductivity

The experimental work with impregnated winding samples has been supplemented with tests on stator-winding sectors (motorettes). Three stator-segments impregnated using alternative varnishes have been manufactured for that purpose. A multiple impregnation, up to 4 times, has also been considered here [20]. The motorettes are manufactured using materials and processes intended for the complete machine assembly as described in section IV. Fig. 6 shows an impregnated motorette instrumented with several type-K thermocouples, which are placed in key points of the assembly. Although, the motorette geometry used in the experimentation is different to that obtained from the design-optimisation exercise, the measured thermal data is transferable to a degree and allows to inform design process before the complete machine assembly is prototyped [9], [12]. The multiple impregnation, investigated in this work, influences both winding thermal conductivity and stator-to-winding contact thermal conductivity simultaneously. It would be rather difficult to separate these two effects experimentally in a simple manner. Therefore, it has been assumed that the winding equivalent thermal conductivity remains unchanged after multiple impregnations, with the stator-to-winding equivalent thermal contact conductivity being affected alone. The approach analogues to that presented in [13] has been employed in this paper.

Fig. 6. An impregnated motorette assembly instrumented with a set of thermocouples.

C. Winding Volume-Specific Heat Capacity

To inform transient thermal analysis required for the design-optimisation process, an ‘in-situ’ volume-specific heat capacity has been derived for the motorettes impregnated with alternative varnishes and using multiple impregnations. The experimental setup and testing procedure is similar to that presented in [14]. The thermal testing involves logging sets of transient data, which is then curve fitted to a low-order thermal model allowing for the volume-specific heat capacity to be derived. The low-order thermal model is described by following formulae:

$$T_w^{k+1} = T_w^k + \frac{\Delta t}{C_w} \left(P_k - \frac{T_w^k - T_{s,measured}^k}{R_{ws}} \right) \quad (1)$$

$$P_k = I_k^2 R_{dc|T_0} (1 + \alpha(T_w^k - T_0)) \quad (2)$$

where: T_w^k is the average winding temperature at k-th time step, $T_{s,measured}$ is the measured average stator core temperature, P is the winding power loss, $R_{dc|T_0}$ is the winding DC resistance at initial temperature T_0 , α is the temperature coefficient of electrical resistance, Δt is the measurement time step for current and temperature, C_w is the winding equivalent heat capacity, R_{ws} is the winding-stator equivalent thermal resistance.

TABLE IV
EQUIVALENT THERMAL PARAMETERS FROM HARDWARE THERMAL TESTS

Varnish thermal conductivity	1.1700 W/m·K
Varnish volume-specific heat capacity	0.8830 MJ/m ³
Winding equivalent thermal conductivity across conductors (pf = 60%)	2.0400 W/m·K
Stator-to-winding equivalent contact thermal conductivity	0.0344 W/m·K
Winding volume-specific heat capacity (pf = 60%)	2.2300 MJ/m ³

Based on the initial analysis with alternative varnish materials and multiple impregnation process, the non-solvent varnish (ELAN-protect UP142) together with double impregnation have been selected for the design-optimisation process [20]. The equivalent thermal parameters derived from hardware thermal tests are listed in Table IV.

VI. DESIGN OPTIMISATION PROCEDURE

A particle swarm optimisation (PSO) routine has been employed here to find an optimal geometry of the IPM traction motor, which satisfies all the design targets discussed earlier. The automated optimisation process adopted here requires a large number of design candidates to be evaluated in a time efficient manner. Consequently, the models employed to derive electromagnetic and thermal performance need to assure both ‘good’ accuracy and ‘low’ solving time. This requires some initial tuning of the optimisation process and individual models to find the right balance.

A. Electromagnetic Model

A two-dimensional (2D) parametric magnetostatic finite element (FE) solver has been used here to inform the electromagnetic design of the IPM traction machine. To accelerate solving time required for a single design candidate, the FE model definition accounts for machine periodic symmetry, and derivation of maximum torque per

Ampere (MTPA) involves solving limited number of individual FE. Here, only 6 individual FE analysis used to derive MTPA. A more detailed description of the adopted approach is presented in [14]. Also, to accommodate for the PM material operating at an elevated temperature, the PM temperature used in the analysis was set to 100 °C.

B. Power Loss Model

The power loss considered in the coupled electromagnetic and thermal design-optimisation includes the winding loss and stator core power loss. The rotor core and permanent magnet power losses are not considered at this design stage. Also, the mechanical power loss components are neglected. The AC winding effects need to be accounted for in the design of the traction motor as the winding ac power loss is expected here to be considerable. This is due to the chosen winding arrangement and operating fundamental frequency, which is in excess of 900Hz. For the purpose of the initial design-optimisation, the ac power loss contribution has been assumed based on the authors’ previous experience[15],

-winding active region,

$$(R_{ac}/R_{dc})|_{T_0} = 6(f/f_{max})^2 + 1 \quad (3)$$

-winding end region,

$$(R_{ac}/R_{dc})|_{T_0} = 2(f/f_{max})^2 + 1 \quad (4)$$

The $(R_{ac}/R_{dc})|_{T_0}$ ratio represents increase of the winding resistance at ac operation at reference temperature T_0 . The stator core power loss is derived using the Steinmetz’s equation with flux density obtained from static electromagnetic FEA.

C. Thermal resistance network model

A custom three-dimensional (3D) parametric lumped-parameter thermal network for the machine design has been developed. The model is based on the cuboidal and arc general elements, which allow for a simple model definition accounting for the internal heat generation and thermal anisotropy [16], [17]. A simplified schematic of the model is shown in Fig.7. The active and end regions of the winding are modelled using the homogenized winding thermal properties in Table IV. The stator-to-winding contact region is represented as an equivalent thermal resistance, see Table IV. The thermal properties of other materials, including laminated stator and rotor core packs, permanent magnet, shaft (Stainless Steel), end cap (Aluminium) and water jacket (Aluminium) are obtained from the manufacturers data sheets and available literature [21].

Fig. 7. A lumped-parameter thermal network of the IPM machine.

D. Optimisation Problem Definition

Fig. 8. A cross-section of the IPM motor together with geometrical parameters.

TABLE V

LIST OF VARIABLES USED IN THE DESIGN-OPTIMISATION

Stator yoke height	h_{sy}
Tooth width	w_t
Stator inner radius	r_{si}
PM width	w_{pm}
PM height	h_{pm}
Magnet inset	m_i
Magnet web	m_w

Fig. 8 presents a cross-section of the IPM machine with geometrical parameters. Table V includes the complete list of variables used in the design-optimisation. Other geometric parameters were set arbitrary, based on authors' design experience.

I_{min} and I_{max} in Fig. 9 are derived based on the power loss and thermal models, together with a set of supplementary assumptions. I_{min} is derived based on the assumption that all the winding power loss generated during the transient operation is stored in the winding, see Appendix. I_{max} is derived based on the assumption that the stator core power loss is zero during the transient operation. These two cases considered here, represent the worst and best winding heat transfer situations, respectively. Combined with the winding thermal limit, these two currents can be found, and the final allowed current should be within the range from I_{min} to I_{max} . The transient torque and transient winding temperature corresponding to a set of current values between I_{min} and I_{max} , $I_1, I_2, \dots, I_{k-1}, I_k$, are evaluated in this routine and the maximum transient torque within winding thermal limit can be derived. Four current values, $I_1 = I_{min}$, $I_2 = \frac{2}{3}I_{min} + \frac{1}{3}I_{max}$, $I_3 = \frac{1}{3}I_{min} + \frac{2}{3}I_{max}$, $I_4 = I_{max}$, are used in the optimisation routine based on initial results from the individual models. Such approach allows for a good balance between the model accuracy and computing time.

The fitness function takes the transient peak torque density and quantity of PM material into consideration simultaneously. This is to achieve a high torque density machine design with a small volume of PM material to reduce the cost.

$$Fitness = C_1 \frac{T_{eref}}{T_{emax}} + C_2 \frac{S_{pm}}{S_{pmref}} \quad (5)$$

Where: T_{eref} and S_{pmref} are the reference values of maximum torque within thermal limit and permanent magnet cross sectional area of one pole, respectively. T_{eref} is set to be the design goal, 183 Nm. S_{pmref} is set to be an estimated value from a rough design, 124 mm^2 . C_1 and C_2 are the

weight coefficients.

Fig. 9. Detailed evaluation routine for a single particle in the optimisation.

VII. RESULTS AND DISCUSSION

Several optimisation runs with different combinations of weight coefficients have been performed. Table VI list selected data for the alternative, optimised machine designs. All machine variants meet the initial design requirements regarding the transient overload (183 Nm, 1900 rpm, 60s) and the use of PM material.

TABLE VI

KEY RESULTS WITH DIFFERENT MOTOR VERSIONS

Motor Version	Weight coefficients	Active length	PM volume
Motor I	$C_1 = 1.5, C_2 = 0$	129 mm	311.9 cm^3
Motor II	$C_1 = 1.5, C_2 = 0.4$	144 mm	183.3 cm^3
Motor III	$C_1 = 1.5, C_2 = 0.8$	158 mm	149.3 cm^3
Motor IV	Initial design	171 mm	178.5 cm^3

Fig. 10 presents an example cross-section geometry of the optimised machine Motor II. This design variant has been selected to be analysed in a more detailed manner, as it assures good balance between the output torque and PM material utilisation. When compared with the initial design, Motor II has 15% shorter active length with moderate increase in PM volume.

The next step of the design process is to perform detailed analysis of the optimised machine, where the machine's performances over entire torque speed envelope are evaluated. A detailed power loss data is derived for the stator, rotor and PM array using the power loss mapping methods [18]. The transient and continuous torque speed envelope of the motor within winding thermal limit has been derived using the envelope derivation method presented in [20]. Fig. 11 presents the transient and continuous torque speed envelopes of the machine with 11 turns per coil and 2 parallel branches. It can be observed that the machine is capable of providing the required transient torque. This demonstrates the effectiveness of the proposed design optimisation

Fig. 10. Cross-section comparison of the coupled electromagnetic and thermal optimised Motor II (left) and the initial design (right)

Fig. 11. Transient and continuous torque speed envelope of the machine.

Fig. 12. Transient and continuous torque speed envelope of the machine when the PM power loss is reduced to one third.

Fig. 13. Transient torque speed envelope for different transient durations.

Fig. 14. Efficiency map of the electrical machine with the non-segmented PMs.

procedure. Also, the machine assures the required continuous torque at low speed operation. However, the continuous torque capability at high speed range does not meet the requirements. The PM power loss is a heavy burden at high speed operation for the thermal management system. When the PM power loss is set to be one third of the original PM power loss, predictions of the continuous torque capability at high speed improve, Fig. 12. It is important to note that the original PM power loss refers here to non-segmented PM array. However, the excessive PM power loss can be significantly reduced by employing appropriate segmentation of the PM array [19].

The torque speed envelopes corresponding to different transient requirements is shown in Fig. 13. With the increase of the overload duration, the transient torque speed envelope moves downward. Eventually, the intermediate operating envelope will approach the continuous envelope, which can be regarded as a ‘transient’ with an infinite duration. The conductor current density for zero speed maximum transient torque corresponding to 180 s is $12.7 A_{rms}/mm^2$, which is

larger than $9.8 A_{rms}/mm^2$ predicted by (10), see Appendix. The main reason is that for a transient peak operation with a relatively long duration, the winding power loss transferred from winding to other part have a non-negligible influence on the winding temperature rise. Fig. 14 presents the efficiency map of the machine. It has been assumed here that all the machine active components are at fixed temperature of 120 °C. Also, it is important to note that the winding power loss accounts for the ac winding effects by the use of (3) and (4). However, the initial assumption regarding the ac winding power loss would require further research to be well assessed, e.g. tests on appropriate hardware exemplars [15]. The assumption regarding winding ac power loss does not have significant impact on the continuous and transient torque output within low speed range since the frequency is relatively low for low speed operating, 158 Hz at 1,900 rpm and 383 Hz at 4,600 rpm. From this prospective, the transient torque over full speed range and continuous torque capability within low speed range can be guaranteed with the proposed design optimisation methodology.

VIII. CONCLUSIONS

This paper presents a coupled electromagnetic and thermal design optimisation of a traction IPM machine with the focus placed on the high torque overload requirement. The machine topology, active materials and electrical insulation materials have been carefully chosen to achieve a cost-effective solution, when it comes to material and processes used in the machine volume manufacture. Three key thermal parameters, including winding equivalent thermal conductivity, stator-to-winding equivalent contact thermal conductivity and winding volume-specific heat capacity have been informed from hardware thermal tests and applied in this design. The developed optimisation system allows for accurate and computationally efficient design of the IPM machines.

The full performance analysis of the optimised design has shown that the machine is capable of achieving the desired transient torque-speed envelope. The analysis also indicates a non-negligible influence of the winding heat transfer from winding to other parts of the machine assembly. This is particularly evident when analysing winding temperature transients for a relatively long overload duration, such as 3 minutes. The results suggest that in order to achieve a high overload capability together with long overload duration, the winding heat capacity together with winding equivalent thermal conductivity and stator-to-winding equivalent contact thermal conductivity need to be considered.

Further research is required to address two following matters. The first one relates to the fabrication and testing of motorettes following outcomes of the design-optimisation. This would be particularly useful to infer the ac power loss data. The second issue is associated with the PM power loss analysis, which requires more work, e.g. adequate PM segmentation to reduce the rotor power loss.

IX. APPENDIX

It is assumed here that all the winding power loss generated in the transient overload duration is totally stored in winding heat capacity. Only the DC copper loss is considered here, which is reasonable when the frequency is

low.

$$J_{rms}^2 \rho_{T_0} (1 + \alpha(T_w(t) - T_0)) V_{cu} = C_{vw} V_w \frac{dT_w(t)}{dt} \quad (6)$$

$$T_w(0) = T_0 \quad (7)$$

Where: J_{rms} is the conductor current density. C_{vw} is the winding volume-specific heat capacity. V_w and V_{cu} are the winding volume and conductor volume, respectively. α is temperature coefficient of electrical resistivity. ρ_{T_0} is the conductor electrical resistivity at temperature T_0 . T_w is winding temperature.

The solution of the above differential equation is

$$T_w(t) = \frac{1}{\alpha} \left(e^{\frac{V_{cu} J_{rms}^2 \rho_{T_0} \alpha t}{V_w C_{vw}}} - 1 \right) + T_0 \quad (8)$$

When the duration is t_{tran} , and the winding thermal limit is

$$T_w(t_{tran}) \leq T_{wmax} \quad (9)$$

Where: T_{wmax} is the allowed maximum winding temperature.

Then a characteristic current density for transient operation can be derived:

$$J_{rms-ch} = \sqrt{\frac{(V_w/V_{cu}) C_{vw}}{\rho_{T_0} \alpha t_{tran}} \times \ln(1 + \alpha(T_{wmax} - T_0))} \quad (10)$$

A curve showing the relationship between the characteristic current density and transient duration is presented in Fig.2. In this figure, conductor material is copper. V_w/V_{cu} and C_{vw} are assigned the data shown in Table IV. $T_0 = 70^\circ\text{C}$. $T_{wmax} = 165^\circ\text{C}$.

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XI. BIBLIOGRAPHIES

Haipeng Liu received the B.Eng. degree in engineering mechanics from Beijing Institute of Technology, Beijing, China, in 2013, where he is currently working toward the Ph.D. Eng. degree in electric vehicles. Since 2016, he has been a Visiting PhD student with the Electrical Energy Management Group, University of Bristol, Bristol, U.K. His research interests include the electromagnetic and thermal design-analysis of electrical machines for electric vehicle applications.

Rafal Wrobel (SM'13) received the M.Sc.Eng. degree from the Opole University of Technology, Opole, Poland, in 1998; the Ph.D. degree from the Lodz University of Technology, Lodz, Poland, in 2000; and the habilitation degree from Opole University of Technology, Opole, Poland, in 2013. He is a Senior Research Associate at the Centre for Advanced Electrical Drives, Newcastle University, Newcastle upon Tyne, UK. Rafal's research interests include multidisciplinary 'design for application' of electrical machines, transformers and wound passive components.

Sabrina Ayat (M'15) obtained a M.Sc. degree in Electrical Engineering from the Ecole Nationale Supérieure d'Electricité et de Mécanique (ENSEM) of Nancy, France, in 2013, and from the Georgia Institute of Technology of Atlanta, United States in 2014. She is currently a PhD candidate at the University of Bristol and a Marie Curie Fellow (ADEPT Project). Since 2017, she is also a Research Engineer in high power density electric machines at Safran Tech, Paris-Saclay, France. Her research interest includes the electromagnetic and thermal analysis of electric machines for 'more-electric' automotive and aerospace applications.

Chengning Zhang received the M.E. degree in control theory and control engineering and the Ph.D. degree in vehicle engineering from the Beijing Institute of Technology, Beijing, China, in 1989 and 2001, respectively. He is currently a Professor and the Vice Director of the National Engineering Laboratory for Electric Vehicles, Beijing Institute of Technology. His research interest includes the design and control of electrical machines for electric vehicle and electric aircraft applications.