Improved thermal equivalent circuit element applied to an external rotor SPM machine

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Abstract—The performance of electrical machines is ultimately limited by thermal issues. To facilitate the modelling and speed up the time to market of novel designs, the accuracy, efficiency and reliability of thermal models has to be improved. In the literature, the majority of thermal models of electrical machines are based on lumped-parameter thermal networks, which are both efficient and simple to implement. Accuracy is the main concern related to this approach. Moreover, the outputs of these thermal models are average temperatures, which are not a suitable constraint for the machine design. In this work we introduce an improved lumped parameter configuration based on a novel structure: the Distributed Losses and Capacitance (DLC) element. The DLC is able to accurately estimate local temperatures at steady-state or during transients with a high Biot number or high aspect ratios. We validate the approach experimentally, creating a thermal model of an external-rotor Surface-mounted Permanent Magnet (SPM) machine. The error in the estimation of the end-windings temperature is within 4%.

Index Terms— electrical machines, LPTN, SPM machine, water-jacket.

I. INTRODUCTION

Thermal analysis is becoming standard practice in the design process of electrical machines, since the cooling system effectiveness ultimately defines the machine performance. A good understanding of the temperature rise in the critical components, such as the windings, the permanent magnets or the bearings, allows for an improved machine design, optimising dimensions and reducing costs. When designing a machine for a propulsion application, the analysis must consider a sequence of operating conditions with varying loads and rotational speeds (driving cycles). Thus, the computational burden increases. It is therefore necessary to rely on thermal models that can be both computationally efficient and reasonably accurate.

Models based on the discretisation of a machine with suitably fine meshes to solve the thermal problem using numerical methods, such as the Finite Element (FE) method, provide the best accuracy. However, this comes at a high computational cost that is unsuitable for transient simulations. Lumped Parameter Thermal Networks (LPTNs) are one solution to this issue. A LPTN represents a machine with a limited number of nodes connected together with thermal resistances, each node representing a machine component. The model output is limited to nodal temperatures, lacking information about

temperature distribution within the components. Moreover, the accuracy of LPTN is affected by a number of parameters, such as the Biot number or local internal heat generation. To overcome the LPTN accuracy limitations Qi [1, 2] and Simpson [3] proposed to highly refine the nodal discretisation in correspondence to critical areas. The main advantage of this is to account for non-uniformly distributed losses. We can also assess the temperature in a particular position, for instance where thermal sensors are located. However, some of the beauty of using a LPTN is lost, namely the simplicity of building a network with a very limited number of connections.

In this paper we propose a new network element, which we name the Distributed Loss and Capacitance (DLC) element. The DLC has the feature of providing accurate information about the element's central temperature, irrespectively to its aspect ratio, Biot number and internal heat generation. With this element we are able to create a thermal model of an electrical machine that provides higher accuracy with a limited number of nodes. In the next section we explore the limitation of the most common structures used to build a LPTN. Then, the DLC element is introduced. We conclude applying the novel discretisation approach to create a thermal model of an external-rotor Surface-Permanent-Magnet (SPM) machine.

II. FAST 2D THERMAL MODELLING

To estimate the temperatures within the machine solid domain we need to solve the conduction problem described by the heat equation

$$c_p \rho \frac{\partial T}{\partial t} = k \nabla^2 T + \dot{q} \quad (1)$$

where $\dot{q}$ is the internal heat generation, ρ and c_p the domain density and heat capacity respectively and k the thermal conductivity which we assume to be a constant in each direction. At the boundaries, Dirichlet or Robin conditions are usually applied

$$T_i = T_0, \quad \nabla T \cdot \mathbf{n} = h_i(T_i - T_\infty)$$

being h_i the heat transfer coefficient related to the i th surface at temperature T_i , $\mathbf{n}$ the unit vector normal to this surface and T_∞ the ambient or coolant temperature. The heat transfer coefficient h_i is usually derived from correlations available in the literature or, in case of complex geometries, estimated through CFD simulations or experimental tests [4]. This equation may be solved using finite difference or finite element technique. In a LPTN (1) is discretised using resistances and capacitances:

$$C \frac{\partial T}{\partial t} = \frac{\Delta T}{R_{cond}} + \dot{Q} \quad (2)$$

This work was supported by the European Union: FP7 ITN Project 607361 ADEPT

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Fig. 1. (a) Reference 2D thermal problem solved with LPTN approach using the (b) classic element and (c) T-network

where ΔT represents the thermal gradient between two neighbouring nodes and we define

$$R_{cond} = \frac{l}{kA}, \quad R_{conv} = \frac{1}{A_i h_i}, \quad C = \rho V c_p$$

to model conduction, convection and thermal capacity respectively, where V is the component volume, l the length and A the cross sectional area normal to l and A_i the i th external surface. As we can see (2) approximates the laplacian $\nabla^2 T$ in (1) as in a second-order finite-differences method. Finite differences and LPTN are therefore very similar apart from the fact that a LPTN uses a highly reduced number of nodes sitting on a non-uniform grid.

To assess the accuracy of a LPTN we analyse the simple problem depicted in fig. 1(a), where an internally heated rectangular geometry is exposed to convection on its lateral boundaries, with the upper and lower surfaces adiabatic. The heat transfer coefficient is assumed to have a fixed value on the 4 boundaries. Applying the classic LPTN approach to this 2D problem means expressing the rectangle with the 5-points network, as in fig. 1(b), where the unknown node is the central temperature. Here, the total internal heat generation $\dot{Q}$ and thermal capacitance C are both introduced in the central node. The resistances shown in fig. 1(b) have the value of $R_{W1} = W/2kA_L$, $R_{L1} = L/2kA_W$ and $R_{Conv} = 1/h_i A_i$ for the i th lateral boundary. In fact, the accuracy of these models is

strictly dependent on Bi which is a non dimensional parameter that defines how significant the temperature gradient within an element is:

$$Bi = \frac{L_{ref} h}{k} \quad (3)$$

where L_{ref} is the problem reference length, which is commonly computed as the ratio V/A_{ext} , where A_{ext} is the total surface exposed to convection. For low values of Bi , generally $Bi < 0.1$, the temperature distribution is almost uniform and the linear approximation is reasonable. Usually the output of the central node is said to be the element's average temperature but this is true only for low Bi when the difference between the local and average temperature is negligible.

To improve the solution for the case when $\dot{Q} \neq 0$, Perez [5] introduced the so-called T-network, fig. 1(c), where the classic network is implemented with a third resistance in each direction to account for the fact that, with internal heat generation, at steady-state the temperature distribution is quadratic. The output of the T-network is the element's average temperature. The third resistor's magnitude is $R_{W3} = -W/6kA_L$ and $R_{L3} = -L/6kA_W$ as explained in [6]. The T-network can be applied to run transient simulations over various geometries [6]–[8]. Example outputs of the two presented LPTN models are given in fig. 2, where the central node temperature of a rectangular geometry with Aspect Ratio $AR = L/W = 2$ is compared with the solution given by COMSOL Multiphysics v5.2. The temperatures and time are presented in a non dimensional form according to:

$$\Theta = \frac{T - T_\infty}{T_0 - T_\infty} \quad \text{if } \dot{Q} = 0$$

$$\Theta = \frac{T - T_\infty}{T_{ss} - T_\infty} \quad \text{if } \dot{Q} \neq 0$$

$$\tau = \frac{\alpha t}{L_{ref}}$$

where $\alpha = k/\rho c_p$, T_0 is the initial condition and T_{ss} is the steady-state solution given by the FE model. In fig. 2(a) we can see that for $Bi = 0.1$ the thermal gradient within the element is almost negligible, therefore the temperature at coordinates $(L/2; W/2)$ almost perfectly matches the average element temperature. Classic and T-network models provide very similarly accurate solutions. Keeping $\dot{Q} = 0$ and increasing the Biot number to $Bi = 5.0$, as in fig. 2(b), leads to a less uniform temperature distribution within the element. The T-network manages to provide a quite accurate solution, whereas the classic approach is not able to follow the mean nor central temperature. Introducing $\dot{Q} \neq 0$ with $Bi = 10.0$, from fig. 2(c) we can see that the internal gradient is increased and that the T-network again is able to give a better solution.

As far as the steady-state solutions are concerned, the problem without internal heat generation is trivial as the temperature distribution will uniformly settle at T_∞ . For the case of $\dot{Q} \neq 0$, the T-network always gives a better solution compared to the Classic element in terms of disagreement with the FE solution. However, average temperatures does not provide sufficient information for a proper design and control of a machine.

Fig. 2. Comparing the solution of the thermal problem in Fig. 1(a) using the classic LPTN (Fig. 9), T-network (Fig. 1(c)) and FEM (element central and mean temperature) for the cases of (a) $Bi = 0.1$ no internal generation, (b) $Bi = 1.0$ no internal generation and (c) $Bi = 10.0$ with internal heat generation

Therefore, in the next section we introduce a tool that can help to assess local hotspot temperatures and, accordingly, give a better insight about the temperature distribution within the elements.

III. INTRODUCING THE DLC ELEMENT

In this section we introduce a new approach to inject loss and capacitance into the classic element to improve its accuracy for $Bi > 0.1$ and $AR \neq 1$. Due to its characteristics we refer to it as the Distributed Loss and Capacitance (DLC) element.

A. Steady-state solution

We consider a steady-state problem with internal heat generation $\dot{Q}$. Gerling and Dajaku [9]–[11] showed that for a 1D problem with Dirichlet boundary conditions on both sides and internal heat generation, the heat introduced in the central node should be cut in half in order to match exactly the local steady-state temperature. This comes by the fact that the solution of this problem is quadratic and the heat flux changes linearly according to Fourier's law:

$$\dot{Q} = -kA \frac{\partial T}{\partial x} \quad (4)$$

However, when we use linear resistors we can correctly deal only with constant heat fluxes. Therefore, to estimate the exact central temperature of the 1D element the heat introduced in the central node should match the average heat flux flowing along the distance represented by the thermal resistor. For the case of the 1D problem this corresponds exactly to $\Lambda \dot{Q}$, where $\Lambda = 0.5$. However, this does not apply to 2D problems as for instance the mentioned rectangle exposed to convection of fig. 1(a), since the temperature distribution at steady-state is a quadratic surface. Making reference to fig. 3(a), following the approach by Gerling and Dajaku we should estimate the correct amount of loss to be introduced in the central node of a 2D square geometry by evaluating the average heat flux normal to the dashed cross section along the A-B path. Results from FE simulations show that for this problem the heat flowing in

Fig. 3. (a) Evaluating the heat flux and (b) assessing the loss to be injected in the central node

each of the 4 directions is $|\Phi_{AB}| \approx 0.1 \dot{Q}$, meaning that the loss ratio Λ to be injected into the model should be equal to $\Lambda = 0.4$. However, this leads to an underestimation of the central temperature due to the fact that we assumed the heat fluxes in the four directions to be independent, which is not the case for a 2D geometry. Using the Matlab built-in `fmincon` function and trying to minimise the cost function $|T_{FEM} - T_{DLC}|$, where T_{FEM} and T_{DLC} are both element central temperatures estimated with different methods, we can find that the correct amount of loss ratio Λ to be introduced in the central node should be equal to $\Lambda \approx 0.6$. In other words, to correctly account for 2D effects we should consider that the heat flowing in each of the four directions is equal to $|\Phi_{AB}| \approx 0.15 \dot{Q}$. In fig. 3(b) we compare the element central steady-state temperature obtained varying the injected loss in the middle node with the FE solution; the bars represents the ratios of the element's central temperatures T_{DLC}/T_{FEM} . We restore the heat balance by injecting the remaining loss, i.e. $0.4 \dot{Q}$, into the boundary nodes. In particular, to account for $AR \neq 1$ the loss introduced into the lateral nodes should be proportional to the exposed surface:

$$\dot{Q}_i = 0.4 \dot{Q} \frac{A_i}{A_{ext}} \quad (5)$$

where A_{ext} represents the total external surface. The proposed configuration with distributed loss and capacitance is sketched

Fig. 4. Representation of the DLC element: $C_c = 0.6 C$, $\dot{Q}_c = 0.6 \dot{Q}$, $C_i = 0.4 C \frac{A_i}{A_{ext}}$ and $\dot{Q}_i = 0.4 \dot{Q} \frac{A_i}{A_{ext}}$

Fig. 5. Accuracy of the DLC element and the T-network in estimating the central and average steady-state temperature for a thermal problem with internal heat generation and fixed heat transfer coefficient at the boundaries on a rectangular geometry for $AR = 1$ to 8 and $Bi = 0.1$ to 10

in fig. 4. The DLC element has been tested over a range of AR and Bi comparing central node solution with the steady-state temperature in the middle of the domain of the FE based model. We solved the reference problem with internal heat generation and fixed heat transfer coefficient on the lateral boundaries. The results, depicted in fig. 5, show that the DLC element ensures high accuracy; the accuracy is expressed as relative percentage error $\frac{|T_{DLC} - T_{FEM}|}{T_{FEM}}$. Very high accuracy is achieved even at high Bi for geometries with $AR < 2$. Similarly, the T-network has been evaluated in terms of percentage error in the estimation of element's mean temperature $\frac{|T_{T-network} - T_{FEM,mean}|}{T_{FEM,mean}}$.

B. Transient solution

The main problem related to transient simulations with the available LPTN approaches at high Bi with internal heat generation is that they give a poor approximation of the heat transfer at the boundaries. The boundary temperatures are estimated by a simple linear interpolation of the two known temperatures, i.e. central or mean and ambient temperatures. This impacts the solution in particular at the beginning of a transient, as shown in fig. 6(a). To address the problem we follow the same approach applied before, distributing the capacitance between the 5 available nodes following the same ratios:

$$C_c = 0.6 C, \quad C_i = 0.4 C \frac{A_i}{A_{ext}}$$

In fig. 6(b) and 6(c) transient solutions of a 2D reference thermal problem are presented without and with internal heat generation respectively. The graphs show the improved accuracy of the DLC element compared to the Classic element during the transients and at steady-state for high Bi and

Fig. 6. Evaluating the DLC element transient solution at (a) boundaries with $AR = 4$ and $\dot{Q} = 0$, (b) central node with $AR = 4$ and $\dot{Q} = 0$ and (c) central node with $AR = 2$ and $\dot{Q} \neq 0$

$AR \neq 1$.

C. Axisymmetric hollow cylinder

Similarly to what was achieved for a rectangular geometry, the DLC element can be applied to an axisymmetric hollow cylinder. This is an important geometry for modelling electrical machines as it can represent many parts of components such as the stator or the rotor. Making reference to fig. 7, the resistances for this geometry are defined in the case of the DLC element as:

$$R_{a1} = \frac{L}{2k\pi(r_o^2 - r_i^2)}, \quad R_{r1} = \frac{\ln(\frac{r_m}{r_i})}{2k\pi L}, \quad R_{r2} = \frac{\ln(\frac{r_o}{r_m})}{2k\pi L}$$

where r_i , r_o and r_m represent respectively the inner, outer and middle radii, where we use $r_m = (r_o + r_i)/2$, whereas L is the hollow cylinder axial length. The definition of the resistors for the T-network are provided in [12]. As for the case of the rectangular geometry, in fig. 8 we compare the

Fig. 7. Elements for an axisymmetric hollow cylinder, DLC element at the top and T-network at the bottom

Fig. 8. Evaluating the DLC element and T-network steady-state solutions for a hollow cylinder and $\dot{Q} \neq 0$ at different ratios $\frac{r_o}{r_i}$ and $0.125 \leq AR \leq 8$

accuracy, in terms of percentage error, of these two models with the solution given by the FE model over a range of AR and Bi in estimating the steady-state solutions for the central and average temperatures. The plot is presented with a log scale on the y-axis to consider a complete range of interesting aspect ratios, defined as $AR = L/(r_o - r_i)$; for low AR the geometry represents a hollow disk. We also considered the effect of the ratio r_o/r_i since it has a strong effect on the location of the hotspot. For low r_o/r_i the geometry is similar to the rectangular problem previously analysed and in fact the accuracy map matches the results shown before. At high r_o/r_i , the hotspot moves towards the symmetry axis and in general the accuracy of the DLC element is still high. It is important to point out the DLC element is again good at dealing with high Bi for geometries with $0.5 < AR < 2$, whereas the T-network seems to be more affected by the ratio r_o/r_i .

TABLE I
NOMINAL RATING DATA OF THE EXTERNAL-ROTOR SPM MACHINE

Efficiency	90 %
Power	60 kW
Torque	500 Nm
Speed	1200 rpm

IV. EXPERIMENTAL VALIDATION

For validation purposes we tested the proposed approach by creating a thermal model of an external-rotor radial flux SPM machine, fig. 9(a). The machine has been developed for in-wheel direct vehicle propulsion; the machine's ratings are provided in table I. This particular application requires that the machine mainly functions at high torque and relatively low speed, which represent challenging working conditions from a thermal point of view. In accordance with [13] air-cooling is not sufficient to remove the amount of internal heat generation. Thus, the machine is cooled through a water jacket that runs tangentially in four parallel rectangular ducts on the stator side of the machine using a water - glycol 50% mixture.

A. External-rotor SPM machine model discretisation

To create the thermal model of the SPM, axisimmetry is assumed and only one stator slot is considered. The nodal discretisation is presented in fig. 9(b), where only a slice of the machine of half its axial length is depicted. The DLC element is denoted by the 5-points network. For the elements with a 3-point discretisation we put half the capacitance in the central node according to [9] and the remaining part is equally split in the other two nodes. When nodes of two adjacent elements overlap, the capacitance and loss contributions are superimposed. The dashed lines connecting the nodes represent the allowed heat paths along which resistors are placed. The division of the "Yoke" into 2 sub-elements allows for a more accurate estimation of the heat flux flowing to the water-jacket from the windings and the teeth respectively [1].

The conduction resistors are created based on geometry and material data. The thermal resistors deriving from the manufacturing process, i.e. contact resistance between rotor and external frame and between windings and teeth/yoke, have low impact on the model since thermal paste ($k_{paste} = 6 \text{ W/mK}$ with equivalent length of $l_{paste} = 1 \times 10^{-4} \text{ m}$) is used and the windings are potted with highly conductive epoxy. These significantly improve the thermal performance of the machine as they minimise the mentioned contact resistances.

The airgap heat transfer is modelled according to Bouafia's correlation for slotted rotors [14] and to account for the coolant convective heat transfer the correlation given in [15] for turbulent flow was used. The inlet temperature of the water jacket is assumed to be constant; the tangential temperature rise in the coolant can be estimated with

$$\Delta T = \frac{\dot{Q}}{\dot{m}C_p} \quad (6)$$

where $\dot{Q}$ represents the heat flux removed by the coolant in the slice, whereas $\dot{m}$ and C_p are the coolant mass flow and

Fig. 9. (a) Analysed external rotor SPM machine and (b) relevant LPTN using the DLC element

heat capacity respectively. Assuming a linear temperature rise in each slot equal to (6) calculated in correspondence to the water-jacket inlet section, we can estimate the temperatures of the motor at different tangential positions, running the LPTN of fig. 9(b) with a suitable coolant inlet temperature.

B. Results

For the validation of the external-rotor SPM machine LPTN, multiple DC tests were run at Brembo facilities. Two phases of the machine were excited with 140 A DC current, while the coolant flow rate was set to 6, 11 and 16 ℓ/min . During the tests the external rotor was stationary. We assumed a constant heat transfer coefficient towards the ambient of $h_{amb} = 25 \text{ W/m}^2\text{K}$ accounting for natural convection with 30 $^\circ\text{C}$ room temperature. Before starting any test, the machine was allowed to cool down to coolant temperature (between 18 and 20 $^\circ\text{C}$). Fig. 10 refers to 6 ℓ/min cooling flow rate; the results are expressed in terms of end-windings temperature. The NTC sensors were placed in the middle of the end-windings domain and embedded in the potting. In fig. 10(a) we

Fig. 10. Comparison between measured and simulated temperatures for the 140 A DC test with coolant flow at 6 ℓ/min in (a) end-windings at 0° and 270° mechanical degrees and (b) inlet-outlet sections of the water jacket

TABLE II
COMPARISON BETWEEN MEASUREMENTS AND SIMULATION RESULTS IN TERMS OF RELATIVE ERROR $|T_{sim} - T_{meas}|/T_{meas}$

Test case	Tangential location	
	0° - % error	270° - % error
6 ℓ/min at 342 s	3.2%	3.8%
11 ℓ/min at 370 s	3.1%	3.1%
16 ℓ/min at 370 s	3.3%	2.0%

compare the thermal measurement at 0° and 270° mechanical degrees following the water flow, the former referring to the water-jacket inlet section. The model is able to correctly follow the temperature rise in the end windings, and also gives an accurate understanding of the tangential thermal gradient. In fig. 10(b) we show the model estimates of average temperature of the coolant at the outlet. The model showed to provide very good agreement with the local temperature measurements in the centre of the end-windings, where the sensors were placed.

In fig. 11 we present the results referred to the test cases with 11 and 16 ℓ/min coolant flow rate. Again the model can follow quite accurately the temperature rise in the end-windings. In table II we present the percentage error between the measured and estimated temperature in the end-windings at the end of the loading condition, $|T_{sim} - T_{meas}|/T_{meas}$. The error always falls within 4%. This confirms the ability of the proposed approach to provide high accuracy independently of the thermal boundary conditions.

V. CONCLUSIONS

In this paper an analysis of the limitations of LPTN modelling approaches commonly applied for electrical machines thermal modelling is given. In particular, we highlighted

Fig. 11. Comparison between measured and simulated temperatures of the windings at 0° and 270° mechanical degrees for the 140 A DC test with coolant flow at (a) 11 l/min and (b) 16 l/min

the impact of Biot number, aspect ratio and internal heat generation. A new modelling approach is proposed, based on the Distributed Loss and Capacitance element, that improves the accuracy of a LPTN. The higher accuracy of the DLC based modelling is checked numerically with simple thermal problems involving a single component with constant thermal boundary conditions. The proposed technique is then tested creating a thermal model of an external-rotor SPM machine. The nodal discretisation, based on the DLC approach, provides a good understanding of the temperature distribution in correspondence to a machine slot and at different tangential position. The discretisation level, around 60 nodes, is reasonably contained and able to run transient simulations in very limited computational time.

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BIOGRAPHIES

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