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Thermal Conductivity Evaluation of Fractional-Slot Concentrated Winding Machines

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***Abstract* –The use of Fractional-Slot Concentrated Windings (FSCW) in electrical machines allows more compact, efficient and reliable design with respect to machines equipped with distributed windings. However, an electromagnetic design linked to a thermal analysis of the electrical machine is mandatory to achieve the desired performance and to fulfill the requirements of efficiency and reliability. One of the most critical issues in thermal design of electrical machines is to assign fair values for the input parameters of the thermal simulation models, particularly those related to the stator winding insulation system. This paper deals with the assessment of the equivalent thermal conductivity of the insulation system of FSCW machines. For this purpose, three FSCW electrical machines for different applications were evaluated via an experimental method based on a *dc* thermal transient test. Whereas the investigated machines present different characteristics among themselves, different approaches were required to properly estimate the thermal conductivity.**

I. Introduction

Fractional-Slot Concentrated-Winding (FSCW) machines have been increasingly used in a wide range of applications, such as electric and hybrid vehicles [1], home appliances [2], refrigeration compressors [3] and aircraft more electric engine (MEE) starter/generator [4], [5]. Their diffusion is supported by several advantages compared to Distributed Winding (DW); in particular, FSCW presents shorter end-winding length and higher fill factor with respect to their counterpart. These features, especially when coupled to segmented stator structures, allows designing both more efficient and high torque density machines. Furthermore, as electrical, magnetic and thermal separation of the phases, the concentrated winding layout is associated to a higher fault tolerance. Their use also allows improving the constant power speed range in Surface Mounted Permanent Magnet (SMPM) machines [6].

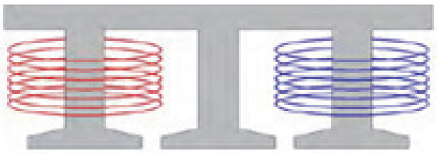
Despite this, during the design stage of the machine both thermal and electromagnetic performance have to be taken into account in order to fulfill the requirements of efficiency and reliability. In this regard, many simulation tools have been developed to assist the thermal design with the purpose of accurately predicting the temperature distribution in the electrical machine. Thermal simulation models are mainly based on Lumped-Parameter Thermal-Networks (LPTN) [7] or the Finite Element Method (FEM) [8], [9].

Regardless the adopted approach, reasonable temperature predictions are obtained only whether reliable input data values are used. These values depend on materials properties and manufacturing processes used in the construction of the electrical machine. In the last years, several papers have been published in the literature concerning methods to accurately estimate thermal resistances and capacitances of electrical machine components [10]-[13]. In particular, as stator windings are very sensitive to thermal issues, the thermal characterization of the stator winding insulation system is of major importance. However, this is not a trivial task because this component of the machine is actually a random distributed composite (combination of the electric insulation itself, impregnation and air), and the adoption of pure material properties as input data for the thermal models returns inaccurate temperature predictions. Hence, a reasonable approach consists in determining equivalent properties. In [13], an experimental technique has been proposed to estimate the winding equivalent thermal conductivity by means of measurements based on cuboidal winding samples. The approach leads to interesting results, but it requires the use of dedicated laboratory samples and a complex thermal test using a heat flow meter. In [14], a method has been proposed to calculate the equivalent thermal conductivity of the winding insulation system of DW induction machines. The fully experimental approach is based on a fast transient *dc* thermal test combined with theoretical heat conduction relations. Measurements were carried out on the electrical machines instead of adopting cuboidal winding samples, as in [13].

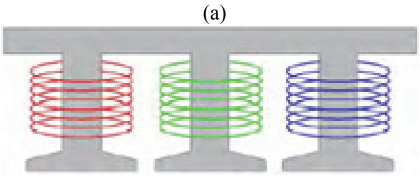
In this paper, the procedure for obtaining the equivalent thermal parameters presented in [14] is applied for three FSCW machines. Although all the machines have concentrated windings, they present different characteristics among themselves and therefore different approaches are required to properly estimate the thermal conductivity. The theoretical method proposed in [14] has been adapted to account the different types of winding arrangement into the slots of FSCW machines. Finally, the obtained results are compared and discussed with those obtained for DW machines [14].

II. Concentrated Winding Topology

Fractional-slot concentrated winding layouts are typically adopted for permanent magnet machine topology. According to the arrangement of the windings into the slots, FSCW can be classified in two types: Single Layer (SL) or Double Layer (DL). In SL winding configurations, each stator slot is occupied by the coil side of a single stator phase as depicted in Fig. 1a, whereas in DL, each slot contains the coil side of two phases as shown in Fig. 1b. In the SL type, the phases are intrinsically insulated from the electric, magnetic and thermal point of view, which leads to high fault-tolerance and high overload torque capabilities [15]. The DL type presents a lower MMF harmonic content resulting in lower torque ripple and reduced rotor losses [15]. These differences in the windings accommodation into the slots also affect the thermal modelling of the stator windings. In this regard, [16] and [17] evaluated the thermal parameters of FSCW segmented stator structures from measurements conducted on dedicated motorette. In the following sections, the thermal parameters are investigated by means of an experimental activity conducted directly on three electrical machines.



(a)



(b)

Fig. 1. Fractional-slot concentrated winding layout:
(a) Single-layer, (b) Double-layer.

III. Investigated Electrical Machines

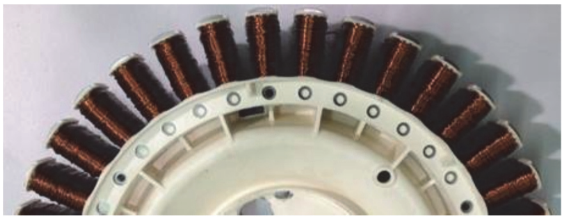
In this research activity, three surface mounted permanent magnet electrical machines equipped with concentrated windings have been investigated. For simplicity, they are referred herein as sample A, sample B and sample C, respectively. Sample A is a 8 kW mini-wind generator prototype which three-phase DL windings have been produced with stranded wires, see in Fig. 2a. Sample B is a 10 kW outer rotor integrated generator prototype for aeronautical application [18]. The machine, which stator is shown in Fig. 2b, has been designed with double three-phase SL windings. It is important to highlight that Litz wires have been used for manufacturing the winding turns. Sample C, shown in Fig. 2c, is a 0.3 kW outer rotor machine used for home appliances. While sample A and sample B are laboratory prototypes, sample C comes from line production and the three-phase DL windings are produced with stranded wires. The main features of the investigated machines are listed in Table I.

Table I
Main Features of the Investigated Electrical Machines

	Sample A	***Sample B***	***Sample C***
Application	Wind generation	Aeronautical	Home appliances
Machine topology	SMPM inner rotor	SMPM outer rotor	SMPM outer rotor
Rated power	8 kW	10 kW	0.3 kW
Number of phases	3	3+3	3
Winding layout	Double layer	Single layer	Double layer
Cooling	Natural ventilation	Forced ventilation	Natural ventilation

(a)

(b)



(c)

Fig. 2. Investigated electrical machines:
(a) Sample A, (b) Sample B, (c) Sample C.

IV. Thermal Model and Parameters Determination

A. Procedure

In order to evaluate the thermal parameters of the stator windings, the experimental approach proposed in [12] has been adopted. The winding system is modeled as a first-order thermal network illustrated in Fig. 3, where R_{eq} and C_{eq} are the winding equivalent thermal resistance and the equivalent thermal capacitance, respectively. The simplified thermal model is based on the assumption of an isothermal back iron during the short-time thermal transient. The experimental procedure to evaluate the thermal parameters is presented in detail in [12]; however, for sake of completeness a brief description is hereafter reported.

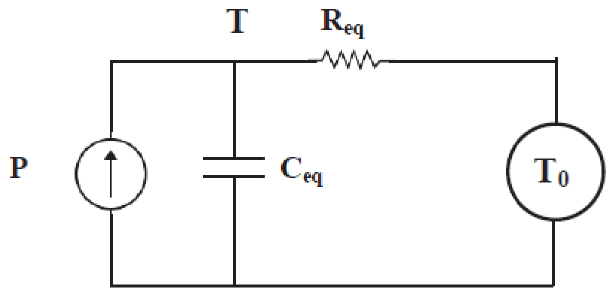


Fig. 3. First-order winding thermal network.

The method consists on warming the stator windings of the machine by the injection of a *dc* current. In this way the heat source is localized in the windings, while iron and mechanical losses are not generated. In order to avoid damage to the magnets during the test, the current in each phase must not exceed the rated one. At the beginning of the test, the machine has to be at the room temperature while the test terminates when the stator lamination temperature is 1 °C higher with respect to the initial one. During the *dc* test voltage and current at the phase machine terminals have to be acquired with a sampling frequency in the range of 1-5 Hz. Finally, the winding temperature is assessed from the winding resistance variation, following the well-known relationship:

$$T = \frac{R_T}{R_0}\left(234.5 + T_0\right) - 234.5 \qquad (1)$$

where:

R_0 is the winding resistance at the temperature T_0;
R_T is the winding resistance at the temperature T;
234.5 is the copper temperature coefficient.

Once the time evolution of the winding temperature has been deducted, the thermal capacitance of the winding system can be computed by means of the electrical energy provided during the test ($W = v_{dc}(t) \cdot i_{dc}(t) \cdot t$). In particular, fitting the energy in function of the winding temperature variation with a linear regression, the value of the thermal capacitance C_{eq} can be directly obtained by (2).

$$C_{eq} = \frac{dW}{dT} \qquad (2)$$

Since all the variables are known, the equivalent thermal resistance R_{eq} can be determined from the mathematical representation of the first-order thermal network (3). Where P is the power loss and the indices k and 0 refer to time instant and initial condition, respectively.

The procedure for finding the optimal value of R_{eq} is based on minimizing the squared deviation between the measured temperature and the one obtained by means of the mathematical model (3).

$$T_k = T_{k-1} + \left(T_0 + R_{eq}P_{k-1} - T_{k-1}\right)\left(1 - e^{-\frac{t_k - t_{k-1}}{R_{eq}C_{eq}}}\right) \qquad (3)$$

B. *Remarks*

The procedure described above reports the general rules for the thermal parameters assessment; however, some practical remarks are necessary. The first point to be clarified concerns the connection of the stator phases during the *dc* test; if all the phase terminals are available, they can be connected in series or in parallel. Series connection is suggested because it guarantees the same current in each phase. In case of Y connection with inaccessible neutral point, the test can be performed suppling two phases. The obtained results have to be opportunely scaled, as only two phases are used. In particular, the thermal capacitance and thermal resistance must be multiplied by 3/2 and 2/3, respectively. The second point regards the duration of the test to ensure the isothermal condition of the back iron. As described above, this assumption is mandatory for modeling the windings system with a first-order thermal network, neglecting the other parts of the machine. The authors consider the back iron isothermal condition if its temperature do not increase more than 1 °C in relation to the initial temperature. However, this value is not critical and it is conservative for a good parameters determination. In electrical machines where the back iron temperature cannot be measured, a long thermal transient test (range of minutes depending on the machine size) can be performed. Nevertheless, only the initial values are useful for the thermal parameters assessment. In particular, the very initial samples (where the energy vs temperature is a straight line) must be considered for the thermal capacitance evaluation. While the time range to be used for fitting the measured temperature is in the range of the winding thermal time constant.

V. EXPERIMENTAL RESULTS

The procedure described above has been applied in order to evaluate the thermal parameters of the three machines. With the aim to compare the thermal parameters obtained with different phase connections, sample A and sample C have been tested connecting both three phase in series and two phase in series. The thermal parameters of the investigated machines are summarized in Table II.

TABLE II
WINDING EQUIVALENT THERMAL PARAMETERS OF THE THREE MACHINES

		2-phase connection	**2-phase to 3-phase connection (a)**	**3-phase connection (b)**	**(a-b)/b**
Sample A	R_{eq} °C/W	0.0448	0.0299	0.0346	-13%
	C_{eq} J/°C	4865	7297	7197	+1.4%
Sample B	R_{eq} °C/W	-	0.146	0.146	-
	C_{eq} J/°C	-	1545	1545	-
Sample C	R_{eq} °C/W	0.288	0.192	0.175	+10%
	C_{eq} J/°C	333	499	466	+7.1%

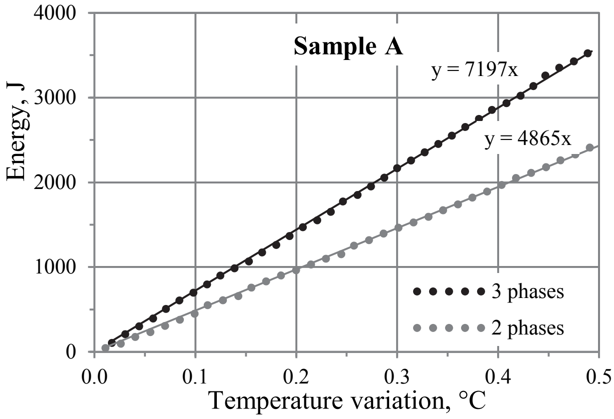


Fig. 4. Thermal energy vs. stator winding temperature increase for Sample A.

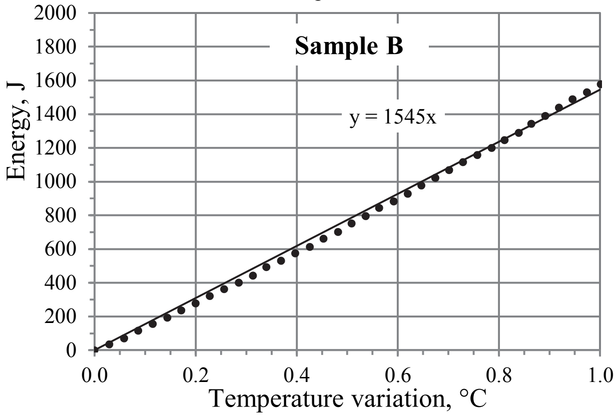


Fig. 5. Thermal energy vs. stator winding temperature increase for Sample B.

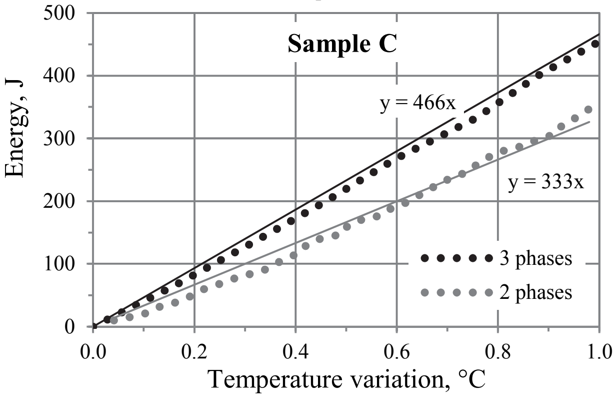


Fig. 6. Thermal energy vs. stator winding temperature increase for Sample C.

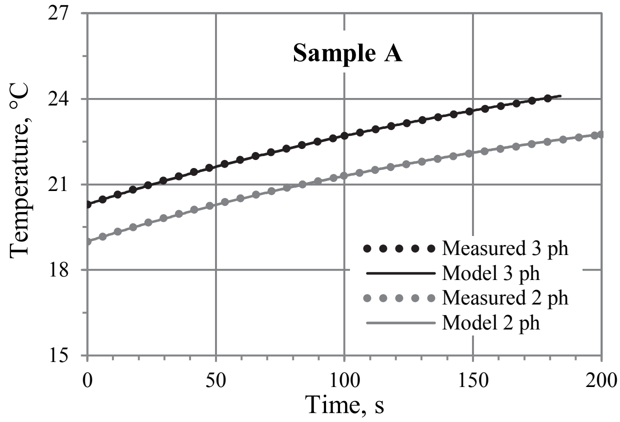


Fig. 7. Predicted and measured stator temperature for Sample A.

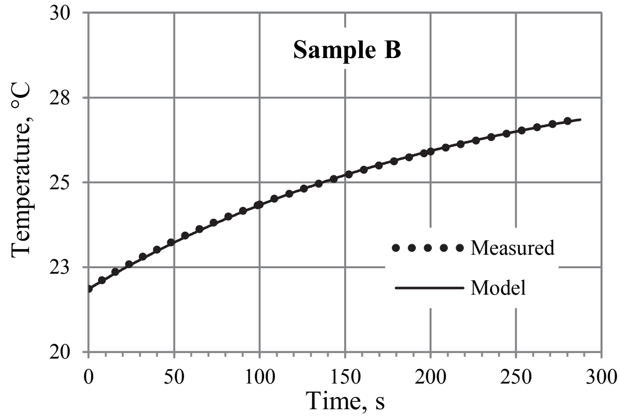


Fig. 8. Predicted and measured stator temperature for Sample B.

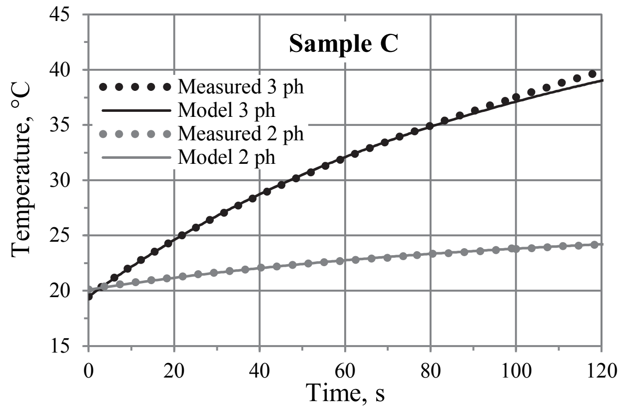


Fig. 9. Predicted and measured stator temperature for Sample C.

The thermal capacitance values obtained with 2-phase connection have been converted into an equivalent 3-phase connection multiplying by 3/2. Comparing the recomputed values with those obtained from the 3-phase connection measurement, the percentage error is equal to 1.4 % for sample A and 7.1 % for sample C, respectively. The energy versus the winding temperature for each machine sample is reported in Fig. 4, Fig. 5 and Fig. 6, respectively. For sample A and sample C are reported the results of both two-phase and three phase connection.

With regard to the thermal resistances, the values obtained from 2-phase connection measurements have been recomputed into an equivalent 3-phase multiplying by 2/3. In this case, the deviations using different approaches are equal to 13% and 10% for sample A and sample C, respectively. Such an outcome indicates that the thermal parameters can be evaluated from tests performed with a reduced number of phases and without affecting the results in a considerable manner. The comparison between the measured winding temperature and the predicted ones for each sample are reported in Fig. 7, Fig. 8 and Fig. 9. It is well evident the excellent agreement between predicted and measured temperatures confirming the validity of the proposed method for FSCW machines as well.

VI. Equivalent Thermal Conductivity Determination

The winding equivalent thermal conductivity is a fundamental input parameter in order to properly model the heat flow path between copper and the stator lamination. The equivalent thermal conductivity, k_{eq}, is mathematically defined by:

$$k_{eq} = \frac{l_c}{R_{eq} A_{hx}} \tag{4}$$

where R_{eq} is the equivalent thermal resistance, and in this case has been previously obtained from the experimental activity; A_{hx} is the cross-sectional area orthogonal to the heat transfer path (referred herein as heat transfer area) and l_c is the characteristic length, which is a parameter of difficult determination in complex geometries.

Since the investigated electrical machines present different winding accommodation layout inside the stator slots, two different approaches are proposed to properly estimate the equivalent thermal conductivity. Both are based on the equivalent thermal resistance and the stator geometrical data.

Case A: Slot homogeneously filled by the copper

In the simplified winding model proposed in [14], the copper is concentrated in the middle of the slot and it is surrounded by an equivalent impregnation and insulation system, see Fig. 10. Since the copper winding homogeneously fills the slot, then it is reasonable to model the characteristic length as the ratio between the cross-sectional area of the non-copper material in the slot (i.e. slot liner, impregnation and unfilled space), and the slot perimeter, l_{slot}:

$$l_{c,1} = \frac{A_{slot}\left(1 - k_{fill}\right)}{l_{slot}} \tag{5}$$

Since the heat transfer area depends on the slot perimeter, the active length of the lamination stack and the number of stator slots (A_{hx} = N_{slot} l_{slot} l_{stack}), the equivalent thermal conductivity is:

$$k_{eq,1} = \frac{A_{slot}\left(1 - k_{fill}\right)}{l_{slot} N_{slot} R_{eq} l_{slot} l_{stack}} \tag{6}$$

Case B: Slot non-homogeneously filled by the copper

The approach described in the previous case is suitable for geometries in which the stator slot is homogeneously filled by the copper winding. However, there are FSCW layouts in which the slot is not evenly filled by the copper, and an unfilled space is found in the middle of the slot, as shown in Fig. 11. For these machines, a different approach is proposed to assess the equivalent thermal conductivity. In this case, the simplified winding model presents the tooth surrounded by the copper while the equivalent impregnation and insulation system layer is interposed between copper and iron.

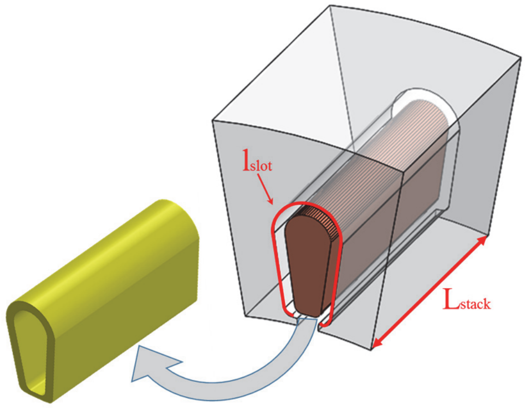


Fig. 10. Geometrical parameters used for Case A.

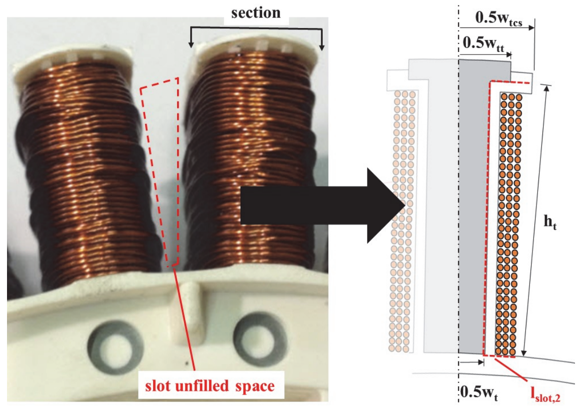


Fig. 11. Geometrical parameters used for Case B.

The method proposed herein to evaluate the equivalent thermal conductivity neglected the convection heat transfer between the copper winding and the surrounding air.

This is a reasonable assumption, since the heat transfer coefficients associated with natural convection are relatively low. The characteristic length is than approached as:

$$l_{c,2} = \frac{A_{slot,2} - A_{w,2}}{l_{slot,2}} = \frac{A_{slot,2}\left(1 - k_{fill,2}\right)}{w_{tcs} - w_t + h_t} \tag{7}$$

where $A_{slot,2}$ and $A_{w,2}$ are respectively the slot and winding cross-sectional areas defined according to this second approach. The slot perimeter, $l_{slot,2}$, is taken into account as a combination of the tooth height, h_t, and the widths of the tooth, w_t, and the coil support tip, w_{tcs}. The slot area is delimited by the tooth height and the width of the coil support at the tooth tip, whereas the winding cross-sectional area depends on the wire diameter, d, and the winding number of turns, N_{turns}. The filling factor is assessed as in the first approach ($k_{fill,2} = A_{w,2}/A_{slot,2}$), but using the updated definitions given by equations (8) and (9).

The FSCW machine geometrical parameters are illustrated in Fig. 11.

$$A_{slot,2} = 0.5\left(w_{tcs} - w_t\right)h_t \tag{8}$$

$$A_{Cu,2} = \pi \frac{d^2}{4} \cdot N_{turns} \quad (9)$$

Since the coil is wound around the tooth, the heat transfer area, $A_{hx,2}$, depends on the slot perimeter, the active length of the lamination stack and the tooth width, as written in (10)

$$A_{hx,2} = 2\left(w_{tt} l_{stack} - w_t l_{stack} + h_t l_{stack} + w_t h_t\right) \quad (10)$$

where w_{tt} denote the width of the tooth tip (Fig. 11).

Finally, the equivalent thermal conductivity between the winding and the lamination of a FSCW machine is estimated from:

$$k_{eq,2} = \frac{A_{slot,2}\left(1 - k_{fill,2}\right)}{l_{slot,2} N_{slot} R_{eq} A_{hx,2}} \quad (11)$$

VII. Analysis of Results

The equivalent thermal conductivities of the insulation systems of three FSCW machines were evaluated following the procedures described in the previous sections. Sample A and sample B have the slots homogeneously filled by the copper, so the procedure of *Case A* was used. For sample C, in which the unfilled space is concentrated in the middle of the slot, the *Case B* procedure was adopted.

The obtained values, reported in Fig. 12, are in the range of 0.18 to 0.20 W/(m°C). Comparing the results obtained for FSCW and those presented in [14] for DW, one notices that values of the former are 2 times higher with respect to the mean values of the latter. As a support, the values of equivalent thermal conductivity obtained for DW induction machines of different sizes are shown in Fig. 13. It can be noticed that the values obtained for nine-machines are in the range of 0.06-0.10 W/(m°C). The deviation between FSCW and DW is likely associated with the contact resistance among coil support, lamination and windings.

In FSCW machines, the coil support is attached to the lamination and the winding is tightly wound around the support. In distributed winding motors, a liner is positioned around the winding inside the slot, but it is not in tight contact with the lamination. Thus, a higher contact resistance with respect to the concentrated winding machines is expected between these two components, which reduces the equivalent thermal conductivity of the insulation system. The results obtained herein give clear evidence that the electrical insulation system of FSCW machines presents a better thermal performance than that found in DW machines.

VIII. Conclusions

In this paper, the equivalent thermal conductivities of the insulation system of three fractional-slot concentrated winding machines have been evaluated through an experimental approach. Two different procedures have been presented in order to take into account different accommodation layout of the winding inside the stator slots.

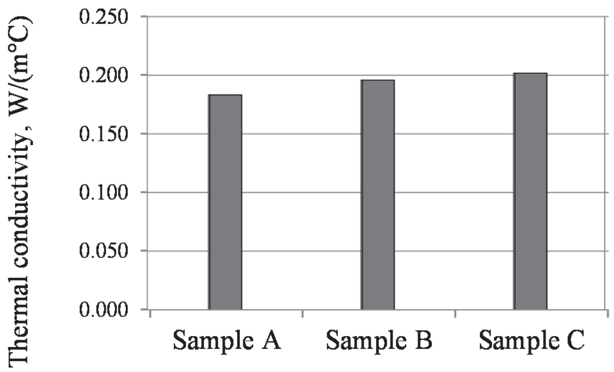


Fig. 12. Equivalent thermal conductivity of the insulation system of the investigated FSCW machines.

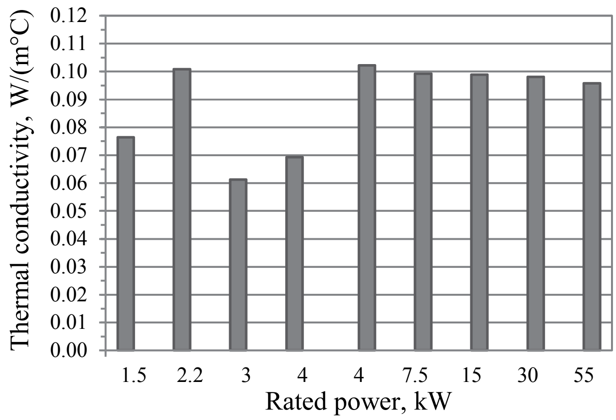


Fig. 13. Equivalent thermal conductivity of the insulation system for DW machines [14].

The obtained values of equivalent thermal conductivity for FSCW machines are two times higher when compared to those obtained for DW topology.

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