Analysis of spatial thermal field in a magnetic bearing

Abstract: This paper presents two mathematical models for temperature field analysis in a new hybrid magnetic bearing. Temperature distributions have been calculated using a three dimensional simulation and a two dimensional one. A physical model for temperature testing in the magnetic bearing has been developed. Some results obtained from computer simulations were compared with measurements.

Keywords: 3D and 2D mathematical models for temperature simulations, spatial distribution of the temperature field, physical model of hybrid magnetic bearing, testing of thermal field distribution

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1 Introduction

Simulation of the temperature distribution is part of the electrical device design process. This is very important when the nominal parameters of an electrical device are strongly increased to the limit. This kind of computation should also be applied when the temperature in the device affects the parameters of its parts. For example, permanent magnets are very sensitive to heating. The magnetic cores of electromagnetic devices are increasingly made from amorphous tape which is sensitive to overheating.

Hybrid magnetic bearings (HMB) with permanent magnets are very effective devices for magnetic suspensions because those magnets can replace the windings with the so called bias current giving the initial suspension. They can also be used in enclosures without a forced cooling system. Temperature calculation is especially important in the design of such bearings even when the devices do not have some external casing. Electrical machines with such bearings should also be designed using simulations of their temperature field.

Analysis of the steady state thermal field in a hybrid magnetic bearing with permanent magnets is presented in this study. Two-dimensional and three-dimensional finite element models have been prepared and used for determination of the temperature distribution. Analysis of such fields is very important for determination of the rated control current density in the windings.

Alternatively, such calculations of the temperature distribution can be used to ascertain the rated current intensity which could cause the overheating and destruction of the permanent magnets in the device.

2 Description of the investigated object

The axonometric projection of the hybrid bearing is given in Fig. 1. This is a six-pole HMB with permanent magnets. The prototype construction of such a magnetic bearing, has been installed in an aluminium housing.
The HMB consists of the stator and rotor. The stator includes three poles with permanent magnets and the three poles with windings. Each winding has 100 turns made of wire with cross-section of 0.7076 mm$^2$. As the materials used for the winding insulation were in the B temperature class, we did not focus on the overheating of the coil. The stator and rotor are made of non-oriented steel M530-50A. Three permanent magnets N38 of size 20 mm $\times$ 3 mm $\times$ 25 mm have been mounted in the stator. The permanent magnets are magnetized along their shortest edge. A detailed magnetic field analysis in the presented magnetic bearing is given in [1].

3 Mathematical model

We prepared two thermal models of the HMB. The first was two dimensional and the second was three dimensional. Femm software was used for the preparation and calculation of the temperature within the 2D modelling [2], while Opera 3D Tempo package was applied in the 3D model [3].

According to Fourier’s law for heat conduction, the heat flux density is proportional to the gradient of temperature $\nabla T$, [2]:

$$\vec{q} = -\kappa \nabla T$$  (1)

where $\kappa$ is the thermal conductivity. Heat flux density must comply with Gauss law, which states that the heat flux which is coming out any closed volume equals to the heat which is generated within that volume [2]:

$$\nabla \cdot \vec{p} = p$$  (2)

where $p$ is the power density generated by Joule’s heat.

Inserting equation (1) into (2), we obtain Poisson’s equation, with the function $T$ which is the temperature distribution being investigated:

$$\nabla \cdot (\kappa \nabla T) = -p$$  (3)

Thermal calculations are difficult to execute, mostly due to difficulties in ascertaining the material property data in the analysed area and in determining the boundary conditions. The HMB actuator is a device that consists of few components. Stator and rotor are made from isotropic silicon steel M530-50A, where the silicon content equals to 1.43%. The producer of the silicon steel does not provide information about the thermal conductivity of the stack of sheets. According to [3], the thermal conductivity of silicon steel can vary from 19 W/(m-K) to 55 W/(m-K) for different silicon contents (from 5% to 0%).

<table>
<thead>
<tr>
<th>Calculation area</th>
<th>Thermal conductivity $\kappa$ [W/(m·K)]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Surrounding environment</td>
<td>0.02619</td>
</tr>
<tr>
<td>Stator and rotor</td>
<td>33.0</td>
</tr>
<tr>
<td>Windings</td>
<td>0.1</td>
</tr>
<tr>
<td>Housing</td>
<td>164.0</td>
</tr>
<tr>
<td>Shaft</td>
<td>25.0</td>
</tr>
</tbody>
</table>

The thermal conductivity of silicon steel with 1.5% silicon content equals to 36 W/(m-K). Additionally, the thermal conductivity of the stator and rotor is an anisotropic quantity, and its axial component is a complex function of the clamping pressure, stacking factor, lamination thickness and lamination surface finish [4]. Unfortunately, there is still not enough information about the value of the thermal conductivity component across the laminated stack. In our calculation model, we assumed that the thermal conductivity of the stator and rotor are isotropic quantities and equal to 33 W/(m-K).

The next confusing issue is the determination of thermal conductivity in the winding volume. The winding is a complex object for thermal analysis because it is a heterogeneous structure, that consists of copper wires, wire insulation, slot insulation, impregnation substance and air. The thermal conductivity of the non-impregnated winding is equal to 0.1 W/(m-K). For the impregnated one, this value is almost double and equals 0.18 W/(m-K) [4]. In the calculation models, we assumed the equivalent thermal conductivity of the windings to be equal to 0.1 W/(m-K). The housing of the HMB is made of aluminium alloy PA6, with a thermal conductivity equal to 164 W/(m-K) at a temperature of 293.16K [2]. This value was used in the numerical model. The HMB is surrounded by air, with thermal conductivity 26.19 mW/(m·K) at a temperature of 300 K. Thermal parameters of the calculation model domains are presented in Table 1.

For the mathematical modelling, we made the following assumptions:

- description the gap between stator and rotor has thermal properties of air. Heat transfer between the stator and rotor exists only due to thermal conduction,
- the heat is generated only by the windings in the result of Joule’s heat,
- the losses generated in the cores of stator and rotor are omitted.

Such assumptions are acceptable, because the HMB is powered from a direct current source and the magnetic
field changes mostly in the rotor during its rotation. Additionally, the tests of the heating process were carried out in all windings powered by a direct current source. Thus, the value of the generated power density in the HMB is given by the equation:

\[ p = \frac{RI^2}{V_{\text{winding}}} \]  

(4)

where \( R \) is the winding resistance, \( I \) is the current intensity flowing through the winding and \( V_{\text{winding}} \) is the winding volume. The power density is identical for all three windings. Additionally, we assumed a constant value of the winding resistance in the operating temperature range.

Two linearized boundary conditions have been used in the calculation models. The first is Dirichlet’s boundary condition on the outer edges of calculation area, which defines constant ambient temperature. The second is Robin’s convective boundary condition, which describes the convection phenomenon on the HMB outer surfaces [5]:

\[ \kappa \nabla T \cdot \vec{n} + h_{\text{combined}} (T - T_{\text{ambient}}) = 0 \]  

(5)

where \( \vec{n} \) denotes the normal vector to the surface and the combined convection coefficient on the surface, which represents heat transfer by convection and radiation. The average value of the \( h_{\text{combined}} \) is described by the equation [6]:

\[ h_{\text{combined}} = h_{\text{conv}} + h_{\text{rad}} \]  

(6)

where \( h_{\text{conv}} \) denotes the heat transfer coefficient by convection and \( h_{\text{rad}} \) is the heat transfer coefficient by radiation. Both heat transfer coefficients were combined into one, because the Opera 3D Tempo software package does not allow the inclusion of heat transfer by radiation. Similarly, the Femm software only allows the setting of one type of heat transfer.

The convection heat transfer coefficient \( h_{\text{conv}} \) was calculated separately for each side of the housing and windings based on empirical correlations for the average Nusselt number for natural convection over the surface [2]. The values of \( h_{\text{conv}} \) were calculated for ambient temperature \( T_{\text{ambient}} \) equal to 294 K. The radiation heat transfer coefficient \( h_{\text{rad}} \) is described [5] by the equation:

\[ h_{\text{rad}} = 4\sigma e \left( \frac{T + T_{\text{ambient}}}{2} \right)^3 \]  

(7)

where \( \sigma \) is the Stefan–Boltzmann constant and \( e \) is the emissivity of the surface. Heat transfer by radiation of the aluminium housing was calculated for the emissivity \( e = 0.1 \), while heat transfer by radiation from the copper windings was calculated for the emissivity \( e = 0.2 \) [2]. Calculation of the heat transfer coefficient is a difficult task because coefficients are nonlinear functions of temperature. In order to reduce the complexity of solving the model, the combined convection coefficients were approximated by a linear function. Figure 2 shows the values of the combined convection coefficient calculated within a temperature range from 290 K to 400 K for different surfaces of the model.

In the 2D model, the convective boundary condition was fixed on the outer edges of the HMB construction, while Dirichlet’s boundary condition has been set at the distance of 40 cm from the HMB. In the 3D model the combined convection coefficient has been assumed on the front surface of the housing and windings in addition to the 2D model boundary conditions.

The 2D model contains 120980 elements and computation time is equal to 15 s. The 3D model contains 1724755 elements and computation time is equal to 157 s. Both calculations were carried out using a computer with two Intel Xeon E5620 2.4 Ghz processors and 32GB RAM.

![Figure 2: Values of the combined transfer coefficients](image)

4 Calculation results

The accuracy of the calculation models was verified by the comparison with measurement results. The temperature fields were compared with four specific test points. They were on the surface of the winding (TP1), on the surface of the stator (TP2) on the surface of the permanent magnet (TP3) and on the surface of the housing (TP4). Figures 3a and 3b present the temperature distribution for a current excitation of 2 A in all windings obtained from the 2D calculation.

The windings are the only heat source in the calculation model, so the highest temperature is observed in-
side them. The temperature in the midst of the winding cross-section is equal to 336.99 K, and it falls to 330.10 K in the proximity of the winding surface (test point TP1). The temperature of the stator is equal to 324.92 K (test point TP2), similarly, the temperature of the permanent magnet is equal to 325.06 K (test point TP3). The temperature of the rotor (326.57 K) is higher than the stator, because the rotor body exchanges heat with the stator only by conduction.

Figures 4a and 4b show the temperature distribution obtained from the 3D calculation model for the same excitation current values as assumed in the 2D model.

Similarly to the previous figures, the highest temperature is observed inside the windings. However, for the 3D model, the temperature in the midst of the winding cross-section is equal to 316.37 K, and falls to 310.46 K in the proximity of the winding surface (test point TP1). Generally, the temperature inside the rotor core is higher than in the stator.

Figure 5 shows the temperature distribution obtained for the steady state from an infrared thermograph for a current intensity of 2A in the HMB windings. The steady state of the temperature distribution was achieved after five and half hours. The measurements were carried out with a ThermoVision A320 thermal camera. Background
Table 2: Comparison of temperature value

<table>
<thead>
<tr>
<th>Test point</th>
<th>Value from measurement [K]</th>
<th>Value from 2D model [K]</th>
<th>Value from 3D model [K]</th>
</tr>
</thead>
<tbody>
<tr>
<td>TP1</td>
<td>310.8</td>
<td>330.10</td>
<td>310.46</td>
</tr>
<tr>
<td>TP2</td>
<td>304.5</td>
<td>324.92</td>
<td>301.99</td>
</tr>
<tr>
<td>TP3</td>
<td>304.3</td>
<td>325.06</td>
<td>302.07</td>
</tr>
<tr>
<td>TP4</td>
<td>302.4</td>
<td>319.01</td>
<td>301.65</td>
</tr>
</tbody>
</table>

The temperature was measured by a BM257s multimeter with an external thermocouple.

Table 2 presents a comparison of the temperature values at the test points obtained from the measurements, 2D model and 3D model for a current excitation of 2 A in the windings.

The results presented in table II indicate that the 2D model gives significantly higher temperatures in relation to the 3D model and the measurement results. The reason for these differences is that the 2D model omits convection from the front surface of the housing and windings. For the presented HMB, the 2D model is inaccurate and should not be used to perform thermal calculations.

Using the 3D model we calculated the temperature of the HMB for current intensities (excited in the windings) within the range from 2 A to 8 A. Figure 6 presents the temperature values for the testing points versus the current.

The windings of the windings of the stator have been calculated as equal to 5.3 A.

The presented thermal model can be used to obtain heating curves versus time. After completing the model with data like the specific heat capacity and density of the materials we can create a model of transient heating.

5 Conclusions

The paper presents two thermal finite element models of a hybrid magnetic bearing. The calculation models have been verified by measurement. It has been proved that the 2D model gives incorrect results because the sheet stack of the stator and rotor packages of the analysed object is relatively small in relation to its diameter. This causes significant heat dissipation on the front and back surface of the HMB. Based on the calculation results of the 3D model, the maximal value of the current which can be excited in

References