Modeling and Design of a Medium-Frequency Transformer for High-Power DC-DC Converters

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Dual Active Bridge (DAB) converters are an interesting solution for the battery interfaces of storage systems used in traction applications. Due to the environmental conditions and space limitations, the design of the transformer and cooling system is crucial for achieving a high power density. Therefore, in this paper, a detailed design of a transformer with an integrated liquid cooling structure and high isolation voltage is presented. Analytic models for the design are presented and verified through FEM simulations and measurements on a prototype system.

Keywords: medium-frequency transformer, modeling, optimization, isolated DC-DC converter, battery storage

1. Introduction

High power DC-DC converters with galvanic isolation are a key element of many applications, as for example medium voltage DC (MVDC) grids (1)–(4), solid-state transformers and power supplies for traction (5)–(8), or more-electric ships (9). Another application are battery interfaces used in electric vehicles or traction systems (10)(11). In Fig. 1/Table 1 a possible setup and specifications of such a battery interface for a locomotive are given. The system enables to store recuperated energy during braking, what reduces the total energy consumption and enables reuse of the recuperated energy during the acceleration phase. Additionally, energy stored in the batteries can be used to drive the locomotive on non-electrified tracks without a diesel engine, avoiding CO₂ emissions (e.g. in shunt yards).

The considered battery interface is based on a dual active bridge (DAB) converter (12). The series connection of modules at the medium voltage secondary side allows to use switching devices with lower voltage ratings for all switches. It also makes the transformer isolation requirements more severe. A high efficiency and high power density is required due to the space limitations in the locomotive. The high power density is achieved by optimising the converter design and by pushing the switching frequency of the semiconductor devices to higher values under ZVS condition. Several examples for high power medium voltage DC-DC converters with galvanic isolation can be found in the literature (1)–(8). Figure 2 gives a comparison of some of these converter systems in the efficiency-power density plane based on data provided in the literature. Figure 2 gives a comparison of some of these converter systems in the efficiency-power density plane based on data provided in the literature. For achieving a high power density, the advanced cooling concepts (13) have to be used in transformer. Many designs employ a coaxially wound transformer (5)(14)(16). The cooling of such a transformer is usually achieved by using hollow inner conductors (5)(15)(16) through which the de-ionized water is pumped. Another investigated transformer structure is the shell type geometry. The cooling of such a structure can be achieved either with natural or forced convection (17)(19), using aluminium plates (8) or via heat pipes (20) to conduct the heat from the windings to the core-mounted heat sinks. Due to the switching frequencies in the kHz range, the eddy current losses induced in the structures with aluminium cooling plates lead to higher than predicted temperatures in the transformer. To cope with this issue a thermally conductive coil formers can be used in order to conduct heat from windings to

Table 1. Specifications of the battery interface consisting of 4 modules

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Primary side voltage ( V_p )</td>
<td>518 V, 835 V</td>
</tr>
<tr>
<td>Nominal primary voltage</td>
<td>700 V</td>
</tr>
<tr>
<td>Secondary side voltage ( V_s )</td>
<td>2800 V (4×700 V)</td>
</tr>
<tr>
<td>Current per battery, continuous</td>
<td>220 A</td>
</tr>
<tr>
<td>Current per battery, peak</td>
<td>280 A</td>
</tr>
<tr>
<td>System power, continuous</td>
<td>200 kW</td>
</tr>
<tr>
<td>Rated nominal withstand voltage</td>
<td>2.8 kW</td>
</tr>
<tr>
<td>Efficiency</td>
<td>&gt; 95 %</td>
</tr>
<tr>
<td>Power density</td>
<td>&gt; 5 kW/dm³</td>
</tr>
<tr>
<td>Ambient temperature</td>
<td>75 °C</td>
</tr>
<tr>
<td>Water temperature</td>
<td>60 °C</td>
</tr>
</tbody>
</table>
Fig. 2. Efficiency - power density comparison of the presented system to the previous state of the art solutions. All the results are given for a single module with full isolation rating of the transformer. For the design presented in [8], both calculated and measured efficiency - power density values are given, where the ⋄ represents the measured values. The presented design only shows the calculated efficiency - power density values.

In order to increase the power density, a transformer with integrated liquid cooling is presented in this paper. The cooling system can be operated with water/glycol as a cooling medium, despite the high nominal isolation voltage of $V_{\text{iso}} = 2.8$ kV. For designing the integrated cooling structure, analytical thermal models are presented and verified with FEM simulations and measurements. In section 2 first the short overview of the design procedure is outlined, followed with used models for the transformer design. There, special attention is dedicated to the thermal modeling of the integrated cooling structure with water channels. Results of FEM simulations for different design aspects are given in section 3. Finally, experimental measurements on a prototype system are presented in section 4, followed by conclusions.

2. Transformer Design

The system shown in Fig. 1 is used as battery interface in locomotives. Since the secondary side (DC link side) is connected in series, the nominal primary and secondary voltages are approximately equal (Table 1). This enables to use the same switching devices for both full bridges. For the presented system 1200 V SiC MOSFET devices are employed. Using SiC MOSFETs under ZVS conditions, enables higher switching frequencies.

In order to determine the parameters for the highest power density, an optimization of the converter modulation scheme and the transformer (Fig. 3) is performed for worst case input conditions. The modulation parameters of the DAB converter are the phase shift angle $\phi$, clamping interval of the primary side bridge $\delta_1$, the clamping interval of the secondary side bridge $\delta_2$ and frequency $f_s$. Illustrative explanation of the modulation parameters is shown in Fig. 4. More details about the used modulation schemes and the description of the implementation are given in [22]. The optimization is multi-objective, i.e. both the system volume and the total system losses are minimized. Starting with the system specifications (Table 1) at step 0 in Fig. 3, first the converter electrical model for the initial control parameters is derived in step 1, giving at the output the voltage-current waveforms and required leakage inductance ($L_{\sigma}$). The voltage-current waveforms (Fig. 4) are used to calculate the losses in the switching devices in step 2. In step 3, the calculated output power $P_{\text{out}}$ (defined by the control parameters) is compared to the required power $P_{\text{nom}}$. In the same step, semiconductor losses are calculated and verified to be below the maximum specified losses. If any of the constraints is not fulfilled, the control parameters are changed and the procedure restarts. In step 4, the transformer optimization loop is executed which determines a suitable core geometry and
winding arrangement that fulfills the following constraints: peak flux density \( B_{\text{max}} \leq B_{\text{sat}} \), leakage inductance \( L = L_{\text{leak}} \) and temperature rise \( T \leq T_{\text{max}} \). If any of the constraint is not met the calculation restarts with new control parameters. After all the feasible designs are obtained in step 5, the optimal design (step 6) is chosen at the knee point of the pareto curve. For the considered system, major challenges are the design of the transformer and the efficient heat removal. In order to improve the heat removal, foil conductors are used for the transformer windings. Using foil only slightly increases the eddy current losses in the windings compared to the litz wire \(^{21}\), but due to the large copper filling factor and surface area, foil windings are preferred from a thermal and a size point of view. In Fig. 5 the CAD drawing of the transformer with integrated cooling system is shown for the specifications given in Table 1. One of the specifications for the transformer and semiconductor cooling system is to use tap water. Therefore, the whole liquid cooling structure is at the ground potential. The cooling channels are used for cooling the switches and the transformer. The channel structure can be seen in Fig. 12. Because of the high isolation requirement between the transformer windings, the cooling channels are placed only on the outer core legs of the transformer. For removing the heat from the transformer primary winding, first an aluminium bar was considered as a part of the bottom cooling part (Fig. 14). The bar is placed between the winding and the transformer middle leg. Due to the induced eddy current losses, the aluminium bar was replaced with an aluminium nitride (AlN) bar (Fig. 15). Aluminium nitride is an electrical isolator which offers high thermal conductivity equivalent to thermal conductivity of aluminium (Table 2). For cooling the secondary winding and fulfilling the isolation requirement, the transformer is potted using a thermally conductive casting compound Wepesil VU 4675, which offers a wide operating temperature range and low hardness. For isolating the secondary side switches, an AlN plate is used to separate the secondary side cooling structure, on which the secondary side switches are mounted, and the grounded bottom cooling part. The fixation of the aluminium and AlN plate to the main structure is achieved with nylon glass filled bolts. By increasing the outer diameter and rounding the edges of the holes in the aluminium parts (see Fig. 6) it is possible to shape the e-field in order to fulfill the given isolation requirements. Table 2 lists the specifications of the designed medium frequency transformer with important parameters used for thermal modeling.

### 2.1 Loss Modeling

In this section, the models used for modeling the transformer core and winding losses are summarized.

#### 2.1.1 Winding Losses

Since foil windings are used for the design of the transformer, the method outlined in \(^{20}\) is used for calculating the skin and proximity effect losses. The method is based on an 1D approximation of the H-field in the windings, which results in the resistance factor expressed in the term of hyperbolic trigonometric functions of the skin penetration depth.

#### 2.1.2 Core Losses

For calculating the transformer core losses the Improved Generalized Steinmetz Equation (iGSE), presented in \(^{19}\), is used. This procedure takes the derivative of the flux waveform, as well as the peak-to-peak value of the flux into account in order to calculate the core loss. The method offers good precision with low complexity, which is advantageous for optimization problems.

Care must be taken when using manufacturer loss measurements for core materials. These measurements are usually performed on small toroidal cores and the losses might be significantly lower (2-3x) compared to the losses in the cores with different shapes (e.g. lare E-U-core). It is advisable to compare the losses of the actual used core, usually given just for a single frequency and flux point, to the same point on the

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**Table 2. Specifications of the medium frequency transformer**

<table>
<thead>
<tr>
<th>Element</th>
<th>Material</th>
<th>Thermal Conductivity (W/m K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core</td>
<td>N97</td>
<td>4</td>
</tr>
<tr>
<td>Winding isolation</td>
<td>Poly-Pad K10</td>
<td>0.85</td>
</tr>
<tr>
<td>Trafo cold plate</td>
<td>AlN &gt; 150</td>
<td>20</td>
</tr>
<tr>
<td>Switch cold plate</td>
<td>AlN 20.30</td>
<td>30</td>
</tr>
<tr>
<td>Potting</td>
<td>Wepesil VU 4675</td>
<td>1.2</td>
</tr>
</tbody>
</table>

Fig. 5. Exploded view CAD drawing of the designed transformer with integrated cooling structure

Fig. 6. Cut view of the fixation point between bottom cooling part and secondary side cooling structure
loss curves in the material datasheet, and scale the curves accordingly. The core material which is used during the design procedure is EPCOS N97 (Table 2).

2.2 Isolation The required isolation level translates into a minimum distance $d_{iso}$ between the conductors:

$$d_{iso, min} = \frac{V_{iso}}{k_{iso} E_{iso}} \tag{1}$$

where $E_{iso}$ is the dielectric strength of the isolation material extracted from the material manufacturer datasheet, $V_{iso}$ is the voltage required to be isolated, and $k_{iso} \in [0, 1]$ is the safety coefficient that is highly dependent on the transformer construction and the used isolating material. There are very few sources in literature that discuss selection of this parameter. Additionally, the given formula does not take into account the inhomogeneity of the electric field. Because of these reasons, for the presented design a very conservative value of 0.04 is used for the safety coefficient $k_{iso}$. For the chosen transformer winding geometry given in Fig. 7, the parameters $d_h$ and $d_{iso}$ must have a greater value than the value of $d_{iso, min}$.

2.3 Leakage Inductance Modeling The winding height in transformer design with relatively high isolation requirements is typically smaller than the core window height. In order to calculate the leakage inductance in such design, a formula that employs the Rogowski coefficient can be used:

$$L_r = \mu_0 N_p^2 \frac{\pi D_{mean} u_l}{\sqrt{h_{cap} \cdot h_{cap}^2}} \cdot \frac{k_r}{k_{iso}} \tag{2}$$

where $N_p$ is the number of primary turns, $D_{mean}$ is the mean diameter of the reduced leakage channel, $u_l$ is the width of the reduced leakage channel, $h_{cap}$ is the primary winding height, $h_{cap}$ is the secondary winding height, and $k_r$ is the Rogowski coefficient. A simplified illustration of the transformer winding structure is depicted in Fig. 7. The expressions for calculating the required parameters in the leakage inductance formula have been adapted for taking into account the non-circular shape of the windings:

$$u_l = \frac{d_p + d_s}{3} \tag{3}$$

$$k_r \approx 1 - \frac{d_p + d_s + d_l}{\pi \sqrt{h_{cap} \cdot h_{cap}^2}} \tag{4}$$

$$D_{mean} = D + d_p + d_L + d_s - \frac{d_s - d_p}{2} \frac{d_p + d_s + 4d_l}{d_p + d_s + 3d_l} \tag{5}$$

where $D$ is the equivalent internal diameter of the inner winding, and can be calculated as

$$D = 2 \sqrt{\frac{(h_c + d_p)(d_c + d_p)}{\pi}} \tag{6}$$

The given formulas for leakage inductance calculation are valid for cases where the heights of the primary and secondary winding are approximately the same. Larger differences in winding heights lead to larger deviations in the calculation, and the given formulas lose on their accuracy.

2.4 Thermal Modeling Two main types of heat transfer are considered in the thermal model - the conductive heat transfer and the convective heat transfer. Due to relatively small differences between surface body temperatures and ambient temperatures, the radiative heat transfer is neglected in the considered model. The expressions for calculating conductive heat transfer are very simple and can be found for example in [10]. In Fig. 8 the internal channel structure of the integrated cooling system is shown. For modeling the conductive flow in the channel of the integrated cooling structure, a basic channel model is shown in Fig. 9. There are two thermal resistances that are used for modeling the heat transfer through the channel. The conductive thermal resistance $R_{th,c}$ from the hot surface to the channel wall, and the convective thermal resistance $R_{th,d}$ from channel wall to the cooling medium temperature. These thermal resistances can be calculated using

$$R_{th,c} = \frac{a}{w L_{HS}} \tag{7}$$

$$R_{th,d} = \frac{1}{h L_{HS}} \tag{8}$$

where $w$ is the width of the channel, $a$ is the distance from hot surface to the center of the channel, $l$ is the length of the channel, $d_c = \sqrt{\pi}$ is the equivalent channel diameter that takes into account channels of non-circular shape, $A$ is the cross-sectional area of the channel, and $L_{HS}$ is the thermal conductivity of heat sink material. For the circular shape channels used in the presented design equivalent diameter $d_c$ is equal to the actual channel diameter $d$. Parameter $h$ is the heat transfer coefficient which can be calculated with

$$h = \frac{N_u A_{HS}}{d_c} \tag{9}$$

For calculating the heat transfer coefficient, it is necessary to know the Nusselt number ($N_u$). The Nusselt number is, in general, a function of the average ducted fluid velocity, duct geometry, and the fluids Prandtl number ($Pr$). The author of [27] has derived an analytical model for the generalized Nusselt number ($N_u_{max}$) that is suitable for the extruded channel.
model with arbitrary cross-section

\[ Nu \sqrt{A} = \left\{ C_2 C_3 \left( \frac{Re \sqrt{A}}{\rho} \right)^{\frac{4}{5}} + \left( C_1 \left( \frac{Re \sqrt{A}}{8 \sqrt{\pi} \rho} \right) \right)^{\frac{5}{8}} + \left( C_4 \frac{f(Pr)}{V^2} \right)^{\frac{m}{5}} \right\}^{\frac{1}{m}} \]  

(10)

with

\[ f(Pr) = \frac{0.564}{1 + (1.664 Pr^{1/6})^{2/3}} \]  

(11)

\[ fRe \sqrt{A} = \left( \frac{11.833 V}{l \cdot \nu} + (fRe_{fd})^{1/2} \right) \]  

(12)

\[ fRe_{fd} = \frac{12}{\sqrt{\pi}(1 + \epsilon) \left( 1 - \frac{192}{\pi^2} \tanh \left( \frac{\pi}{2L} \right) \right)} \]  

(13)

where \( \rho \) is the density of the channel fluid, \( V \) is the flow rate in [m³/s], \( \nu \) is the kinematic viscosity of the channel fluid, \( l' = l \cdot \nu / VPr \) is the dimensionless thermal duct length, \( \epsilon \) is the nominal aspect ratio that is defined as a ratio between vertical and horizontal dimension of the channel with arbitrary shape, and the parameters \( C_1, C_2, C_3, C_4, \gamma \) are defined in (27).

For uniform wall temperature problems as

\[ C_1 = 3.01 \quad C_2 = 3 \quad C_3 = 0.409 \quad C_4 = 2 \quad \gamma = \frac{1}{10} \]

The blending parameter \( m \) is defined by

\[ m = 2.27 + 1.65Pr^{1/3} \]  

(14)

With the presented models all thermal resistances in the system can be numerically evaluated. As a result, the thermal network of the presented transformer can be derived and used in the optimization procedure given in Fig. 3. For easier representation of the thermal network, the transformer from Fig. 5 is split in two 2D cut planes, each showing a cut view of the transformer with cooling channels as shown in Fig. 10.

In order to solve the thermal model of the transformer, additional information on the fluid is required. Due to the multiple parallel pipe structure shown in Fig. 11, the fluid flow in the different channels differs from the input fluid flow. In order to calculate the velocity of the fluid in the parallel channels, the following expressions are used (28):

\[ \Delta p_{tot} = \Delta p_{1} = \Delta p_{2} = \ldots = \Delta p_{i} \]  

(15)

\[ V = V_1 + V_2 + \ldots + V_i \]  

(16)

where \( \Delta p_{tot} \) is the total pressure loss of the system, and \( \Delta p_i \)
(i = 1, 2, ...) are the pressure losses in the individual channels. \( V \) and \( V_i \) (i = 1, 2, ...) designate the input flow rate and flow rate in the individual channels, respectively. For calculating the pressure losses in the individual channels, the Darcy-Weisbach relation can be used:\(^{(19)}\)

\[
\Delta p = f \cdot \frac{\rho}{2} \cdot \frac{l}{d} \cdot V^2 \nonumber \tag{17}
\]

where \( V \) is the fluid velocity, \( l \) is the channel length, \( d \) is the channel diameter, \( f \) is the channel friction factor, and \( \rho \) is the fluid density. Each channel has a quadratic parallel resistance, and the pressure loss is related to the total flow rate by

\[
\Delta p = \frac{V^2}{\left( \sum \sqrt{K_i/f_i} \right)} \nonumber \tag{18}
\]

where

\[
\dot{V} = V \cdot \frac{\pi d^2}{4} \quad K_i = \frac{\pi^2 \cdot d^2}{8 \rho l_i}
\]

In the general case, the channel friction factor \( f_i \) is a function of the Reynolds number and the roughness ratio. Since the Reynolds number varies with the fluid velocity, solving the set of equations must be done iteratively. In the first step, an arbitrary values of \( f_i \) are chosen and with them a first estimate of \( l_i \) is calculated. Then, the resulting flow rate estimate \( V_i \approx (K_i \Delta p/f_i)^{1/2} \) is obtained for each channel. Using these results, a new Reynolds number and a better estimate of \( f_i \) is calculated. Usually, a few iteration steps are sufficient to obtain a satisfactory solution. When the flow in the channel is laminar, a simple expression for \( f \), known as Darcy friction factor, can be used

\[
f = \frac{64}{Re} \nonumber \tag{19}
\]

where \( Re = V \cdot d/\nu \) is the Reynolds number. Finally, the obtained fluid flow rate is then used in expression Eq. (11) in order to calculate the thermal resistance of the water channel (Eqs. (7) and (8)). For the channels which have a turbulent flow, Haaland’s equation offers good approximation of the turbulent region of the Moody chart:\(^{(20)}\)

\[
f = \left[ -1.8 \log \left( \frac{6.9}{Re} + \frac{(\xi/d)^{1.11}}{3.7} \right) \right]^{-2} \nonumber \tag{20}
\]

### Table 3. Comparison between the values of the leakage inductance obtained with the analytical calculation and the 3D FEM simulation

<table>
<thead>
<tr>
<th>Calculation</th>
<th>Analytical</th>
<th>FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Leakage inductance ( L_w )</td>
<td>26.5 ( \mu )H</td>
<td>26.2 ( \mu )H</td>
</tr>
</tbody>
</table>

where \( \xi/d \) is the channel roughness ratio and \( \xi \) is the wall roughness height.

For designs with long channels and or slow fluid flows, the temperature of the fluid can increase along the axial direction. In order to calculate this temperature rise of the fluid, an 1D energy balance expression\(^{(20)}\) is used:

\[
q_r = \frac{V \rho C_p (T(x) - T_{in})}{x} \nonumber \tag{21}
\]

where \( q_r = q/l \) are the power losses per unit of length, \( q \) is the total power losses dissipated along the channel of length \( l \), \( \rho \) is the density of the fluid, \( C_p \) is the thermal capacity of fluid, and \( T_{in}, T(x) \) are the input temperature and temperature at point \( x \) in axial direction, respectively.

### 3. FEM Simulations

In this section, results of FEM simulations are presented for different design aspects.

#### 3.1 Leakage Inductance Simulation

For verifying the analytical model of the leakage inductance from section 2.3, a 3D magnetic field simulation is performed. For the simulation, an 1 A current excitation of the primary and secondary windings are assumed with opposite directions, and the resulting total magnetic energy is obtained. The leakage inductance is then calculated from the energy with

\[
L_{w} = \frac{2E_{tot}}{I^2} \nonumber \tag{22}
\]

The comparison of the calculated and simulated leakage inductance value is shown in Table 3.

#### 3.2 Heat Transfer and CFD Simulations

In order to verify the thermal models presented in section 2.4, combined 3D heat transfer and fluid dynamic FEM simulations were performed. The simulated velocity field inside the presented cooling structure is shown in Fig. 12, while the temperature distribution is given in Fig. 13. The input flow rate at the inlet is assumed to be 8 L/min and the inlet water temperature is equal to 20 °C, which corresponds to the flow rate and water temperature used during experimental measurement. In Table 4, a comparison between the analytical calculation and the FEM simulation for the flow velocity distribution inside individual channels of the parallel multi-channel structure is given. The values of the material parameters considered in the analytical thermal calculations listed in Table 2 are also used for the 3D FEM simulations. The comparison between the calculated temperatures of the transformer obtained with the thermal model given in Fig. 10 and the results from FEM simulations are given in Table 5.

#### 3.3 Eddy Current Simulations

In order to get the eddy current induced losses in the transformer cooling structure 3D FEM simulations were performed. There, two cases have been considered: 1) single Al bar located between the middle transformer leg and the primary winding on the bottom, 2) two AlN bars located on top and bottom of the middle transformer leg. The surface current densities induced
Fig. 12. Water velocity field inside the presented converter cooling structure

Fig. 13. Temperature distribution of the integrated converter structure

Table 4. Calculated velocity distribution in internal channels of the integrated cooling structure from Fig. 11

<table>
<thead>
<tr>
<th>Velocity</th>
<th>Analytical</th>
<th>FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>$V_1$</td>
<td>0.45 m/s</td>
<td>0.47 m/s</td>
</tr>
<tr>
<td>$V_2$</td>
<td>0.34 m/s</td>
<td>0.35 m/s</td>
</tr>
<tr>
<td>$V_3$</td>
<td>0.26 m/s</td>
<td>0.34 m/s</td>
</tr>
<tr>
<td>$V_4$</td>
<td>0.21 m/s</td>
<td>0.29 m/s</td>
</tr>
<tr>
<td>$V_5$</td>
<td>0.092 m/s</td>
<td>0.086 m/s</td>
</tr>
</tbody>
</table>

Fig. 14. Eddy currents induced in the bottom cooling structure with Al bar

Table 5. Calculated temperatures of the transformer with presented thermal model (Fig. 10)

<table>
<thead>
<tr>
<th>Temperature</th>
<th>Analytical</th>
<th>FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core mid leg ($T_{C1}$)</td>
<td>72 °C</td>
<td>70 °C</td>
</tr>
<tr>
<td>Core outer mid leg ($T_{C2}$)</td>
<td>42 °C</td>
<td>40 °C</td>
</tr>
<tr>
<td>Core side leg ($T_{C3}$)</td>
<td>28 °C</td>
<td>29 °C</td>
</tr>
<tr>
<td>AlN inner ($T_{AlN}$)</td>
<td>68 °C</td>
<td>71 °C</td>
</tr>
<tr>
<td>Primary winding ($T_{Wp}$)</td>
<td>83 °C</td>
<td>78 °C</td>
</tr>
<tr>
<td>Potting material ($T_{Pot}$)</td>
<td>80 °C</td>
<td>77 °C</td>
</tr>
<tr>
<td>Secondary winding ($T_{Ws}$)</td>
<td>88 °C</td>
<td>81 °C</td>
</tr>
<tr>
<td>Al top/bottom cover ($T_{Top}$)</td>
<td>66 °C</td>
<td>43 °C</td>
</tr>
</tbody>
</table>

Table 6. Induced eddy current losses in the transformer cooling structure

<table>
<thead>
<tr>
<th>Calculation</th>
<th>Al bar</th>
<th>AlN bar</th>
</tr>
</thead>
<tbody>
<tr>
<td>Induced losses</td>
<td>75 W</td>
<td>36 W</td>
</tr>
</tbody>
</table>

Table 7. Comparison of computation times for analytical calculations and FEM simulations

<table>
<thead>
<tr>
<th>Calculation</th>
<th>Analytical</th>
<th>FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Leakage inductance</td>
<td>0.01 s</td>
<td>450 s</td>
</tr>
<tr>
<td>Heat transfer &amp; CFD</td>
<td>0.12 s</td>
<td>1380 s</td>
</tr>
<tr>
<td>Eddy currents</td>
<td>-</td>
<td>320 s</td>
</tr>
</tbody>
</table>

4. Experimental Results

In order to experimentally validate the presented models, a 50 kW module employing SiC MOSFETs as switching devices and medium-frequency transformer was built. In Fig. 16 the photo of a single module for a modular DC-DC converter system (Fig. 1) is given.

4.1 Leakage Inductance Measurement The leakage inductance is measured with a power choke tester (ED-K DPG10-1500B), that is a pulsed inductance measurement device. The measurement was performed on three built transformers and the results are shown in Fig. 17. Comparing the measurement results with the results of the analytical calculation and FEM simulation, given in Table 3, it can be seen that these match very well.

4.2 Thermal Measurement In order to measure the temperature of the transformer winding, an NTC temperature probe is attached to the secondary winding. The converter is operated in open loop with nominal voltages of 700 V and current of 60 A. The water cooling system was operated with constant flow rate of 8 L/min and a water temperature of 20 °C. The converter was operated until reaching steady...
Fig. 16. Photo of the 50 kW DAB converter for the considered modular DC-DC system shown in Fig. 1.

Fig. 17. Measured short circuit inductance of the built transformers.

Fig. 18. Steady state converter temperatures measured with a thermal camera.

state temperatures, i.e. approximately 90 min. The picture from the thermal camera at the end of the test is shown in Fig. 18. Table 8 gives the comparison between the measured and calculated temperatures.

4.3 Partial Discharge Measurement The final measurement which has been performed is the partial discharge measurement. This measurement is performed by a 2.8 kV/50 Hz peak voltage to the secondary side winding while having the primary side winding and core grounded. The results of the partial discharge measurement procedure are depicted in Fig. 19. The figure shows the cumulative partial discharges during a testing period of 20 min, and their occurrence as a function of the supplied voltage. As can be seen, the highest discharge values are lower than 18 pC, which can, for the given requirements, be regarded as partial discharge free.

4.4 Transformer Circuit Operation The designed transformer has been tested in single active bridge (SAB) mode during startup phase (Fig. 20) at nominal voltage, and in DAB mode during normal operation (Fig. 21) at 700 V and 60 A, that corresponds to overload of approximately 10%. The controller design is presented in (22).

5. Conclusion

In this paper, the design of a medium frequency transformer with integrated cooling structure is presented. The system integration with advanced cooling design results in a higher power density value (Fig. 2). Detailed loss, leakage inductance and thermal models of the medium frequency transformer with integrated cooling structure are used to find the optimal design parameters using the methodology presented in section 2. The resulting design was validated with extensive FEM simulations for various design aspects. The final validation is performed with experimental measurements on...
the prototype system.

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