



Research article

Improving thermal simulation accuracy of dry-type transformers using experimentally identified temperature-dependent epoxy properties

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Abstract: This study presents an enhanced three-dimensional conjugate heat transfer (CHT) model designed to improve the thermal simulation accuracy of a 320 kVA, 22/0.4 kV cast-resin dry-type transformer (CRDT). While conventional thermal modeling commonly assumes constant thermophysical properties for epoxy encapsulation, this research dynamically incorporates temperature-dependent functions for thermal conductivity (λ) and specific heat capacity (C_p). These parameters, previously identified through experimental measurements (ASTM D5334) on the specific 8055A/8055B epoxy system, are continuously updated at every solver iteration according to the local temperature field. By utilizing a one-way multiphysics coupling strategy, the non-uniform electromagnetic losses computed via ANSYS Maxwell are explicitly mapped as volumetric heat sources into the ANSYS Fluent CHT model. The proposed framework was rigorously validated against empirical data collected from 27 thermocouples installed on an actual industrial prototype operating under rated load conditions. The integration of temperature-dependent material properties significantly reduced the maximum relative prediction error from 10.9% (using conventional constant properties) to 4.59% along the axial profile of the high-voltage winding. Furthermore, the error margin remained below 5.1% across all 27 validated locations, successfully pinpointing the hotspot temperature of 75.0 °C at the upper section of the phase-B low-voltage winding. Given that the thermal aging of epoxy insulation accelerates exponentially with rising temperatures, this substantial improvement in prediction accuracy provides a highly credible foundation for estimating insulation service life and establishing safer, better-founded design margins for CRDTs.

Keywords: dry-type transformer; cast resin; epoxy resin; temperature-dependent properties; conjugate heat transfer; hotspot temperature; finite element method

1. Introduction

Cast-resin dry-type transformers (CRDTs) have attracted increasing attention in modern power distribution systems, particularly in urban networks, high-rise buildings, hospitals, subways, and offshore installations, owing to their high fire safety, low maintenance requirements, and environmental compatibility [1,2]. Because CRDTs are cooled by natural air convection rather than oil circulation, their heat-dissipation capability is inherently limited, and the epoxy-encapsulated windings are prone to localized overheating, with the resulting non-uniformity of losses and airflow giving rise to pronounced, difficult-to-predict hotspots [3–5]. The hotspot temperature is the single most critical thermal quantity in this respect: excessive hotspot temperatures accelerate the thermal aging of the epoxy insulation exponentially [6], which may, in extreme cases, trigger the thermal runaway of the resin [7] and degrade its dielectric strength [8]. Accurate prediction of the three-dimensional temperature distribution and of the hotspot value and location, in particular, is therefore a prerequisite for reliable CRDT design, loading-capability assessment, and insulation-life estimation [4–9]; cooling design remains a central topic of transformer research [10].

A variety of numerical approaches have been developed for the thermal analysis of dry-type transformers, each with distinct merits and limitations:

- Thermal-network (lumped-parameter) models: These are computationally inexpensive and convenient for online monitoring, but they rely on empirical convection correlations and offer only coarse spatial resolution, which makes precise hotspot localization difficult [11].
- Finite element method (FEM) analyses: These resolve heat conduction within the solid structure in detail [5,12,13]; however, in purely FEM-based thermal models, the convective boundary conditions must be imposed through empirical heat transfer coefficients, whose uncertainty propagates directly into the predicted temperatures.
- Computational fluid dynamics (CFD) and conjugate heat transfer (CHT) approaches: These remove this limitation by explicitly resolving the buoyancy-driven air flow [3,14–16], at the price of substantially higher computational cost.
- Coupled electromagnetic–thermal(–fluid) formulations: These further improve fidelity by mapping the non-uniform loss distribution onto the thermal model [14,17,18].

Recent studies have applied such frameworks to geometric and cooling optimization, including core window height [19], winding air-gap arrangement [20,21], and loading-dependent hotspot dynamics [18]; data-driven methods have also been proposed for hotspot estimation [9]. Notwithstanding this progress, three problems persist in the thermal simulation of dry-type transformers:

- (i) The thermophysical properties of the epoxy encapsulation, its thermal conductivity λ , and specific heat capacity c_p are almost universally treated as constants taken from generic material libraries, although they vary markedly with temperature [22,23].
- (ii) Experimental validation is frequently restricted to a small number of measuring points rather than to densely instrumented industrial-scale prototypes.
- (iii) The quantitative accuracy gain obtained by replacing constant properties with measured temperature-dependent functions inside a coupled electromagnetic–thermal–fluid model has not been rigorously established.

A critical review of the studies closest to the present work makes this research gap explicit. Xu et al. [12] performed an FEM-based thermal analysis of a cast-resin dry-type transformer but assumed constant material properties and validated the model at a limited number of locations. Seddik et al. [13] compared 2D and 3D FEM thermal models of a power transformer, again with temperature-independent insulation properties. Américo et al. [17] enhanced the thermal modeling of three-phase dry-type transformers, and He et al. [18] investigated hotspot temperature-rise dynamics under varied loading rates; in both cases, the insulation properties originate from generic software libraries or datasheets rather than from measurements on the actual resin system. Dawood et al. [19,20] and Zhu et al. [21] optimized geometric parameters (core window height, air-gap arrangement) for thermal or electromagnetic performance without addressing material-property modeling.

In summary, no previous study has systematically integrated directly measured, temperature-dependent thermal-property functions of the actual epoxy system into a coupled electromagnetic–thermal–fluid model of an industrial-scale CRDT and quantified the resulting accuracy gain against dense experimental instrumentation. The present study addresses precisely this gap for a 320 kVA, 22/0.4 kV CRDT: the temperature-dependent thermal conductivity $\lambda(T)$ and specific heat capacity $c_p(T)$ of the 8055A/8055B epoxy system, identified experimentally by the transient line-source method (ASTM D5334) in our previous work [23], are dynamically integrated into a one-way coupled ANSYS Maxwell–Fluent conjugate heat transfer model, and the predictions are validated against 27 thermocouple measurements on the actual prototype. This distinction can be substantiated along four concrete axes:

- Property characterization: Previous studies [5,11–14,17,18] assigned the insulation system using generic library or datasheet values, whereas the present study uses $\lambda(T)$ and $c_p(T)$ functions measured directly on the specific 8055A/8055B resin batch physically present in the validated prototype.
- Coupling depth: The thermal-network model in [11] and FEM-only formulations in [5,12,13] require an externally imposed convection coefficient, whereas the present model resolves the buoyancy-driven flow field explicitly through one-way electromagnetic–thermal–fluid coupling.
- Validation density and scale: Models in [12,13,17,18] were validated against a small number of points or laboratory-scale specimens, whereas the present study validates against 27 thermocouples on an industrial 320 kVA prototype.
- Controlled attribution: Because the temperature-dependent and constant-property simulations reported in Tables 7 and 8 share an identical mesh, an identical one-way coupling scheme, and identical boundary conditions differing only in the treatment of λ and c_p , the resulting 58% reduction in maximum relative error (from 10.9% to 4.59%) can be attributed unambiguously to the material characterization itself, isolated from any confounding modeling choice. To the best of our knowledge, this controlled attribution has not previously been reported for cast-resin dry-type transformers.

In this paper, the main contributions are summarized as follows:

- Model development: Experimentally identified temperature-dependent thermophysical property functions of the epoxy insulation [23] are dynamically integrated and updated at every solver iteration according to the local temperature field into a one-way coupled

electromagnetic–thermal–fluid (CHT) model of an industrial 320 kVA cast-resin dry-type transformer.

- Comprehensive experimental validation: The model is validated against 27 thermocouple measurements on the actual prototype under rated load, reducing the maximum relative error from 10.9% (constant-property model) to 4.59% along the examined HV-winding axial profile, and keeping the error below 5.1% at all validated locations.
- Design guidance: The quantified comparison demonstrates that temperature-dependent characterization of the epoxy resin is a necessary condition for trustworthy hotspot and insulation aging assessment, providing a validated basis for safety-margin evaluation in CRDT design.

2. Materials and research methodology

2.1. Structure of dry-type transformer

The subject of this study is a three-phase dry-type distribution transformer manufactured by SANAKY, featuring a rated capacity of 320 kVA and a voltage ratio of 22/0.4 kV, as illustrated in Figure 1 [24].

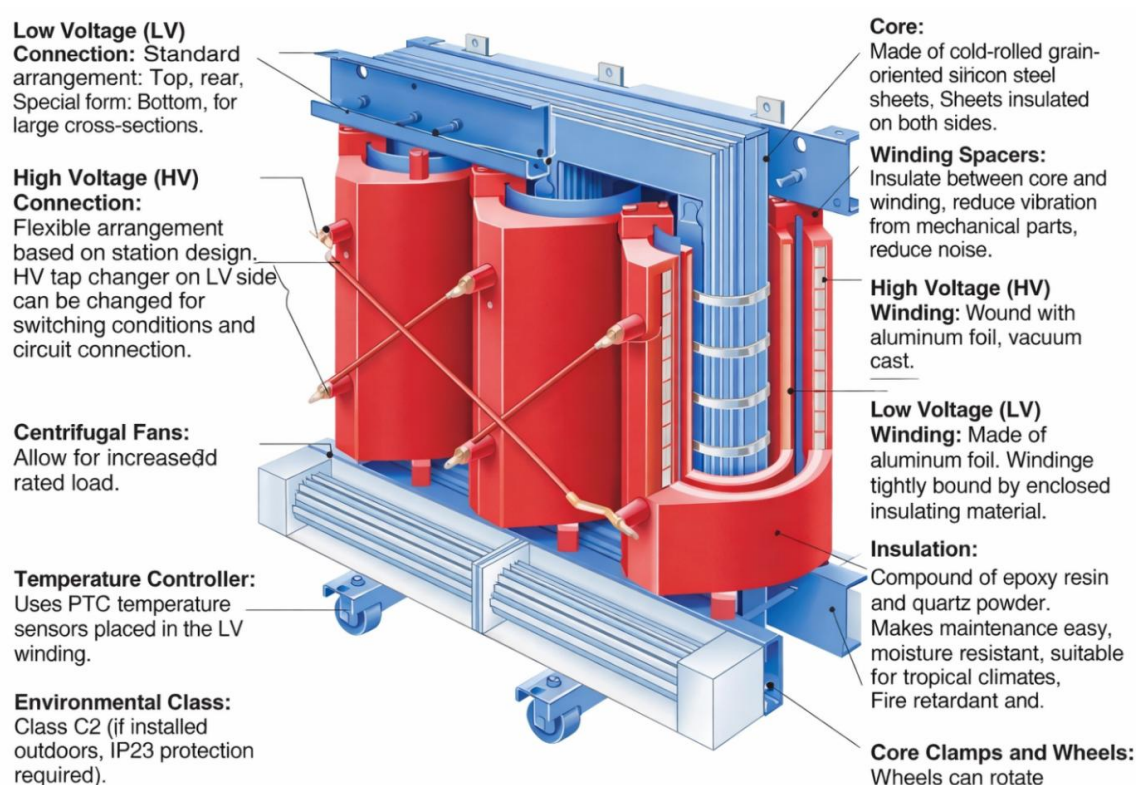


Figure 1. Dry-type transformer model [24].

The detailed technical specifications and rated parameters of the transformer are presented in Table 1 and Figure 2.

Table 1. Rated parameters of the 320 kVA dry-type transformer.

Parameter	Value	Unit
Number of phases	3	-
Frequency	50	Hz
Rated power	320	kVA
Vector group	Δ/Y_{n11}	-
HV/LV line voltage	22/0.4	kV
HV/LV phase turns W_1/W_2	2090/22	Turns
HV/LV line current	8.42/462	A
Short-circuit impedance u_k	6	%

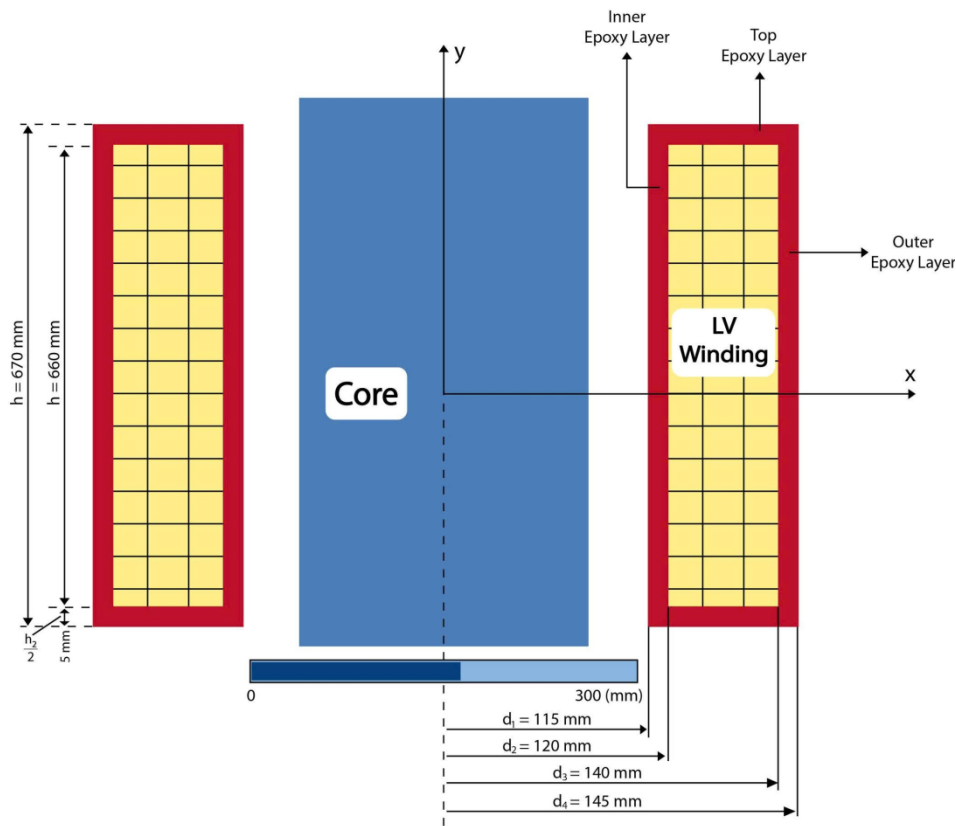


Figure 2. 2D geometric dimensions and cross-sectional view of the transformer core and windings.

2.2. Epoxy resin parameters

As proposed in [23], the epoxy insulating material was fabricated using a base resin coded 8055A and a curing agent coded 8055B, both supplied by Jiang Te, China. The mixture was prepared at a mixing ratio of 1:1 by weight. The fundamental physical constants of this specific insulating material, presented in Table 2, were used as the reference values for comparison and error evaluation [25].

Table 2. Epoxy resin parameters of the dry-type transformer [25].

Parameter	Symbol	Value	Unit
Density	D	1640	kg/m ³
Elastic modulus	E ₁	3.6	GN/m ²
Specific heat capacity	c _p	1500	J/kg.K
Thermal conductivity	λ	0.32	W/m.K
Poisson's ratio	ν	0.33	-
Coefficient of thermal expansion	α	0.7 × 10 ⁻⁴	K ⁻¹

In Table 2, the reported parameters correspond to the static physical properties of the epoxy, while the specific heat capacity and thermal conductivity are given as average values over the temperature range of 100–200 °C.

Through computational simulation and comparative analysis, the thermal properties of the epoxy material (1:1 ratio, 8055A/8055B system) were experimentally determined using the transient line-source method in accordance with ASTM D5334 [23]. Specifically, the thermal conductivity (λ), specific heat capacity (c_p), and thermal diffusivity (α) were demonstrated to be temperature-dependent rather than constant. These experimental data were approximated using a second-order Taylor series expansion as follows:

- The temperature dependence of the thermal conductivity is approximated as a function $\lambda = f(t)$ represented below:

$$\lambda(t) = 0.5612 + 0.002044t - 1.68 \cdot 10^{-5}t^2 \quad (1)$$

- The temperature-dependent relationship of the specific heat capacity, denoted as $c_p = f(t)$, is characterized by the following polynomial expression:

$$c_p(t) = 82.61 + 4.77t + 0.031t^2 \quad (2)$$

- The correlation $\alpha = f(t)$ is approximated via the polynomial representation below:

$$\alpha(t) = 2.33 - 0.031t + 21.05 \cdot 10^{-5}t^2 \quad (3)$$

where $\lambda(t)$ is the temperature dependence of the thermal conductivity, $c_p(t)$ is the temperature dependence of the specific heat capacity, $\alpha(t)$ is the temperature dependence of the correlation, and t is the temperature of the epoxy.

To ensure technical transparency and reproducibility of the coupled multiphysics simulation, the complete physical, thermal, and electromagnetic properties of the Silicon Steel 27ZH110 core and Copper windings are comprehensively detailed in Tables 3 and 4.

Table 3. Core material parameters of the dry-type transformer.

Parameters	Value	Unit	Notes
Density	7650	kg/m ³	
Thermal conductivity	28	W/m.K	
Core loss coefficients	Steinmetz parameters		Extracted from the manufacturer's datasheet

Table 4. Winding material parameters of the dry-type transformer.

Parameters	Value	Unit	Notes
Density	8900	kg/m ³	
Thermal conductivity	385	W/m.K	
Specific heat capacity	385	J/kg.K	
Electrical conductivity	47×10^6	$\Omega^{-1} \cdot \text{m}^{-1}$	At a reference temperature of 75 °C

2.3. Electromagnetic loss computation via the Maxwell equations

In this study, the internal heat generation within the transformer is determined from the spatial distribution of electromagnetic losses rather than being assumed to be uniformly distributed. The loss distribution is obtained through three-dimensional finite element electromagnetic simulations performed in ANSYS Maxwell, allowing the non-uniform characteristics of the heat sources in both the core and windings to be accurately captured.

The equations presented in the following section are not intended for direct analytical evaluation of the losses. Instead, they describe the governing electromagnetic field equations solved numerically by the finite element solver to determine the magnetic field distribution and the corresponding loss density throughout the computational domain.

The transformer operates based on electromagnetic induction produced by the energized windings. To obtain the distributions of the magnetic flux density (**B**) and the electric field intensity (**E**), ANSYS Maxwell solves Maxwell's equations over the three-dimensional computational domain, which are expressed as follows:

$$\nabla \cdot \mathbf{E} = \frac{\rho_e}{\varepsilon_0}, \quad (4a)$$

$$\nabla \cdot \mathbf{B} = 0, \quad (4b)$$

$$\nabla \times \mathbf{E} = -\frac{\partial \mathbf{B}}{\partial t}, \quad (4c)$$

$$\nabla \times \mathbf{B} = \mu \mathbf{J} + \mu \frac{\partial \mathbf{D}}{\partial t} \quad (4d)$$

where ρ_e is the electric charge density, ε_0 is the vacuum permittivity, μ is the magnetic permeability, and $\mathbf{J}$ is the current density. Throughout this paper, bold symbols denote vector quantities, whereas italic (non-bold) symbols denote scalar quantities. It should be noted that at the frequency of 50 Hz, the term of displacement current in the equation (4d) is negligible ($\frac{\partial \mathbf{D}}{\partial t} \approx 0$).

Based on the computed electromagnetic fields, the solver evaluates the volumetric heat generation rate within the high-voltage (HV) and low-voltage (LV) copper windings. The heat source term is derived from Joule heating:

$$\Delta P_{cu} = m \cdot I^2 \cdot R_{dc} \cdot k \quad (5)$$

where m is the number of phases, R_{dc} is the DC resistance of the windings, I is the RMS current values under rated load conditions ($I_{LV} = 461.9$ A, $I_{HV} = 8.4$ A), and k is the stray loss factor accounting for eddy current effects.

Due to the rectangular geometry of the silicon steel, the magnetic flux distribution is highly non-uniform. The ANSYS solver computes core losses by numerically integrating over each finite element based on the classical Steinmetz equation.

The total core loss consists of two components: hysteresis loss (P_h) and eddy current loss (p_w).

- Hysteresis loss is calculated as follows:

$$P_h = \eta \cdot f \cdot B_{max}^x, \quad (6)$$

where f is the operating frequency (50 Hz), B_{max} is the local peak magnetic flux density, η is a material coefficient, and x is the Steinmetz exponent.

- Eddy current loss is calculated as follows:

$$p_w = \frac{4}{3} \cdot \frac{(d \sigma_k \cdot f \cdot B_{max})^2}{\gamma \cdot \rho} \quad (7)$$

where d is the lamination thickness, γ is the mass density, σ_k is the form factor ($\sigma_k = 1.11$ for sinusoidal excitation), and ρ is the electrical resistivity.

- Total core loss is calculated as follows:

$$P_{core} = p_w + P_h \quad (8)$$

Finally, the total winding losses (ΔP_{cu}) and core losses (P_{core}) are extracted from ANSYS Maxwell as spatial volumetric heat generation rates, which are then used as input thermal loads for the subsequent thermo-fluid analysis.

2.4. 3D thermal-fluid CFD simulation

The thermal analysis is performed by solving a conjugate heat transfer (CHT) problem in the ANSYS Fluent environment. This approach simultaneously accounts for heat conduction in the solid domains (magnetic core, copper windings, and epoxy insulation) and natural convection in the surrounding air.

Within the solid domains, the volumetric heat generation rate (q_v), obtained from the electromagnetic analysis, is applied as an internal heat source. The governing heat conduction equation in a three-dimensional Cartesian coordinate system is expressed as:

$$\rho C_p(T) \frac{\partial T}{\partial t} = \lambda(T) \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right) + q_v, \quad (9)$$

where $\lambda(T)$ is the thermal conductivity, and $C_p(T)$ is the specific heat capacity of the epoxy resin implemented directly into the CFD model. The transformer operates under natural air (AN) cooling; therefore, the convective heat transfer coefficient is not prescribed but is resolved through the flow field.

The governing equations are:

- Continuity equation:

$$\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} + \frac{\partial w}{\partial z} = 0 \quad (10)$$

-
- Energy equation:

$$\frac{\partial T}{\partial t} + u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} + w \frac{\partial T}{\partial z} = \alpha \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right) \quad (11)$$

- Navier–Stokes equation:

$$\rho \left(\frac{\partial \mathbf{u}}{\partial t} + \mathbf{u} \cdot \nabla \mathbf{u} \right) = -\nabla p + \mu \nabla^2 \mathbf{u} + \rho \mathbf{g} \quad (12)$$

where u, v , and w denote the velocity components, α is the thermal diffusivity, p is pressure, and μ is dynamic viscosity.

Buoyancy effects are modeled using the Boussinesq approximation. The realizable k – ε turbulence model is employed to capture the airflow behavior within the narrow vertical cooling channels between the high-voltage (HV) and low-voltage (LV) windings.

Thermal radiation from the external surfaces of the epoxy-cast windings is also considered. The surface-to-surface (S2S) radiation model is applied based on the Stefan–Boltzmann law:

$$q_{rad} = \varepsilon \sigma (T_s^4 - T_{amb}^4) \quad (13)$$

where ε is the surface emissivity, σ is the Stefan–Boltzmann constant, T_s is the surface temperature, and T_{amb} is the ambient temperature (20 °C).

2.5. Boundary conditions, meshing strategy, and grid independence

To achieve reliable three-dimensional conjugate heat transfer (CHT) simulations, the computational domain, boundary conditions, and mesh discretization are carefully defined to represent the actual operating conditions of the dry-type transformer. The investigated 320 kVA dry-type transformer employs natural air (AN) cooling without any forced ventilation. Consequently, the airflow within the computational domain is induced solely by buoyancy resulting from temperature gradients. To reproduce this operating condition, a sufficiently large air enclosure is introduced around the transformer to emulate an unconfined environment. The external boundaries of the fluid domain, including the top, bottom, and side surfaces, are prescribed as pressure boundaries with zero-gauge pressure. This configuration permits ambient air to enter and leave the computational domain freely, thereby enabling the development of buoyancy-driven natural convection. The inlet air is assigned a uniform ambient temperature of 293.15 K (20 °C). In addition to convective heat transfer, thermal radiation from the external surfaces of the epoxy insulation is taken into account using the surface-to-surface (S2S) radiation model based on the Stefan–Boltzmann law.

As illustrated in Figure 3, the transformer is enclosed within a sufficiently large virtual air domain to minimize the influence of the computational boundaries and to accurately reproduce natural convection under open-air operating conditions.

To ensure transparency and reproducibility of the numerical model, all critical simulation parameters, operating conditions, and boundary conditions (BCs) applied in the coupled CFD-FEM analysis are explicitly summarized in Table 5.

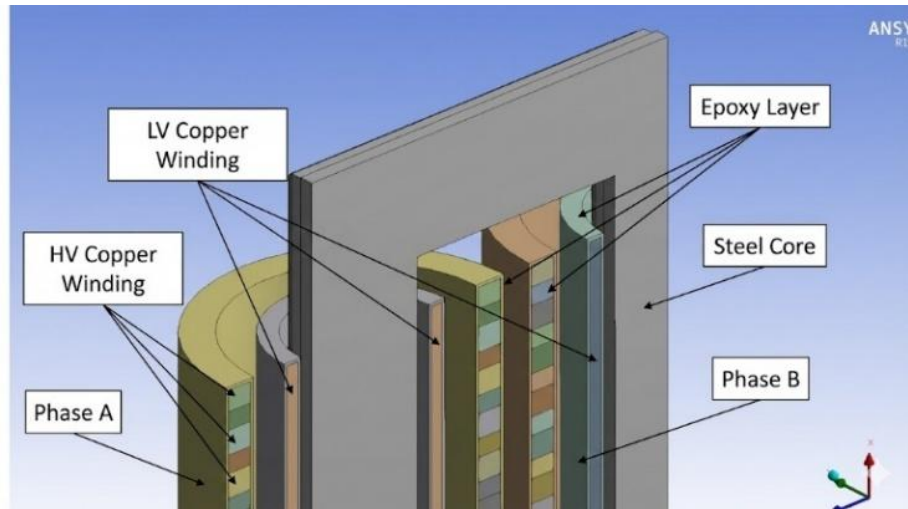


Figure 3. Model of the dry-type transformer's core with 320 kVA (22/0.4kV).

Table 5. Simulation parameters, BCs, and solver settings.

Parameter	Value/configuration	Remarks
<i>1. Operating and heat source parameters</i>		
LV winding current (I_{LV})	461.9 A	Rated operating condition of the 320 kVA
HV winding current (I_{HV})	8.4 A	Rated operating condition of the 320 kVA
Supply frequency	50 Hz	
Total core loss	835 W	Extracted from ANSYS Maxwell 3D
Total winding loss	3545 W	Extracted from ANSYS Maxwell 3D
<i>2. Thermal fluid boundary conditions</i>		
Ambient temperature	293.15 K (20 °C)	Uniform, constant boundary condition
Air domain boundaries	Zero-gauge pressure	Applied at pressure inlet/outlet
Convection coefficients	Dynamically resolved	Computed iteratively via fluid flow solution
<i>3. Solver configuration and physics models</i>		
Buoyancy model	Boussinesq approximation	Captures buoyancy-driven natural convection
Turbulence model	Realizable k- ϵ	Captures airflow in narrow cooling channels
Thermal radiation model	Surface-to-surface (S2S)	Stefan-Boltzmann law applied at solid-fluid interfaces
Pressure-velocity coupling	SIMPLE algorithm	Steady-state solution mode

With respect to heat transfer, the present analysis is formulated as a three-dimensional CHT problem under natural air-cooling conditions. Accordingly, the convective heat transfer coefficients are not prescribed as boundary conditions or estimated using empirical correlations. Instead, they are determined implicitly through the coupled solution of the airflow and temperature fields.

The airflow is driven exclusively by buoyancy forces arising from temperature differences and is modeled using the Boussinesq approximation in conjunction with the coupled Navier–Stokes and energy equations implemented in ANSYS Fluent. As a result, the local convective heat transfer coefficients evolve naturally from the numerical solution, allowing the buoyancy-induced flow and

the associated thermal boundary layer to be resolved without introducing empirical heat transfer coefficients.

3. Application test

3.1. Simulation test

The numerical model is established based on the practical 320 kVA dry-type transformer, whose specifications are summarized in Table 5. A coupled electromagnetic–thermal framework is employed to simultaneously account for the interactions between electromagnetic losses, heat transfer, and the surrounding air domain.

The simulation is based on the following assumptions. Electromagnetic losses are determined by the transformer geometry and operating temperature. Copper losses are evaluated from the operating current, with the electrical resistance updated as a temperature-dependent property, whereas core losses are calculated using the Steinmetz equation. The transient temperature distribution is governed by heat conduction, natural convection, and thermal radiation under the prescribed boundary conditions. The thermal conductivity of both the laminated steel core and the windings is modeled as anisotropic to account for the directional dependence of heat conduction, particularly within the laminated core, where the conductivity parallel and perpendicular to the lamination planes differ significantly.

For the CHT analysis, both the solid and fluid domains are discretized using a structured hexahedral mesh. Local mesh refinement, including five inflation layers, is applied at the solid–fluid interfaces to accurately resolve the thermal and velocity boundary layers. A grid independence study is performed by increasing the mesh density from 12 to more than 20 million elements. Since the predicted maximum hotspot temperature changes by less than 0.1% for meshes exceeding approximately 17 million elements, a mesh containing about 17 million elements (Figure 4) is adopted for all subsequent simulations to ensure grid-independent results while maintaining computational efficiency.

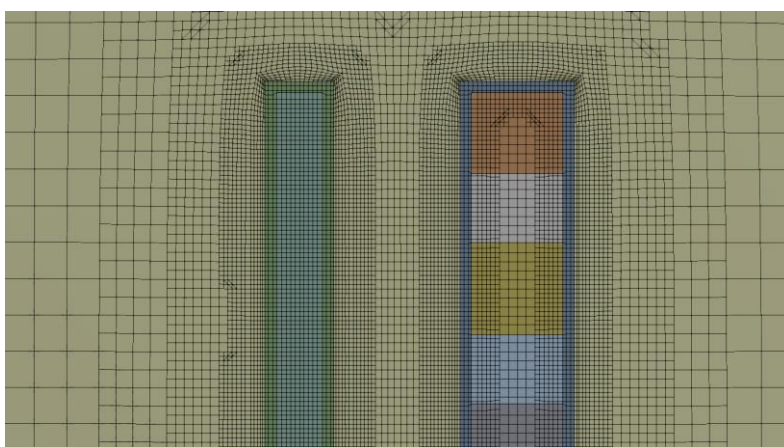


Figure 4. Mesh model used for transformer simulation.

Steady-state thermal simulations were performed at the rated load currents of $I_{LV} = 461.9$ A and $I_{HV} = 8.4$ A, using the epoxy thermal conductivity and specific heat capacity listed in Table 2. The resulting temperature distribution is presented in Figure 5.

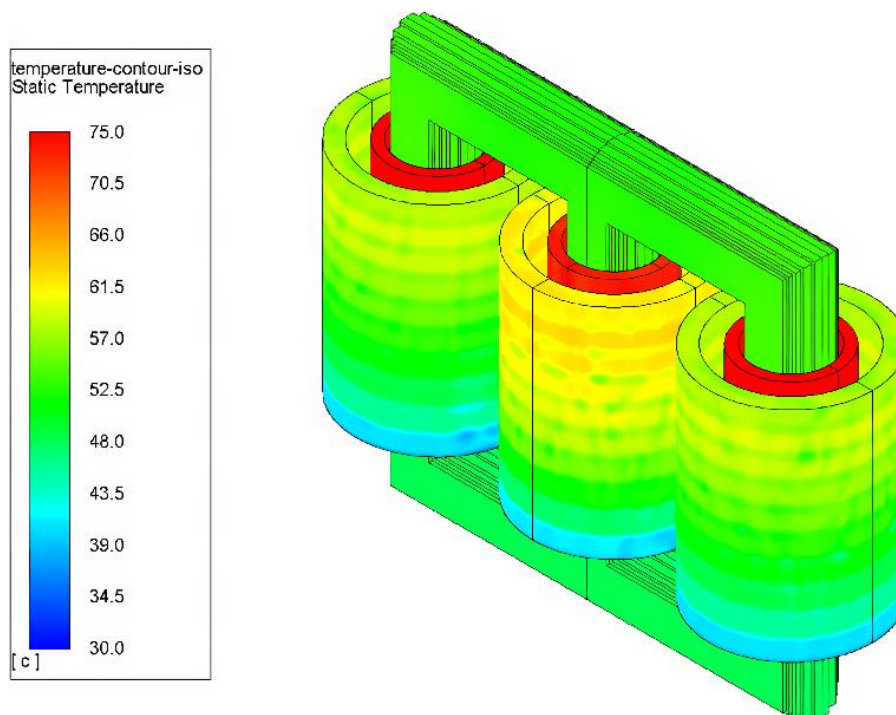


Figure 5. Temperature distribution of the three-phase transformer using temperature-dependent thermophysical properties.

The simulation results reveal a markedly non-uniform temperature field throughout the transformer. As shown in Figure 6, the temperature distribution exhibits a clear axial gradient, with the highest temperatures occurring near the upper sections of the windings. This behavior is consistent with the buoyancy-driven airflow pattern associated with natural air cooling.

A noticeable temperature difference is also observed among the three phases. The central phase (Phase B) experiences the highest thermal accumulation, whereas the outer phases (Phases A and C) operate at comparatively lower temperatures. This phenomenon can be attributed to the reduced airflow within the interphase regions and the additional heat exchange between adjacent windings through convection and radiation. The maximum temperature predicted by the simulation reaches 75.0 °C and is located at the upper portion of the Phase-B low-voltage winding, as illustrated in Figure 6.

Utilizing the aforementioned transformer parameters, a thermal simulation of the dry-type transformer was executed with the epoxy thermal conductivity and specific heat capacity held constant at the values shown in Table 2. The resultant temperature distribution is depicted in Figure 7.

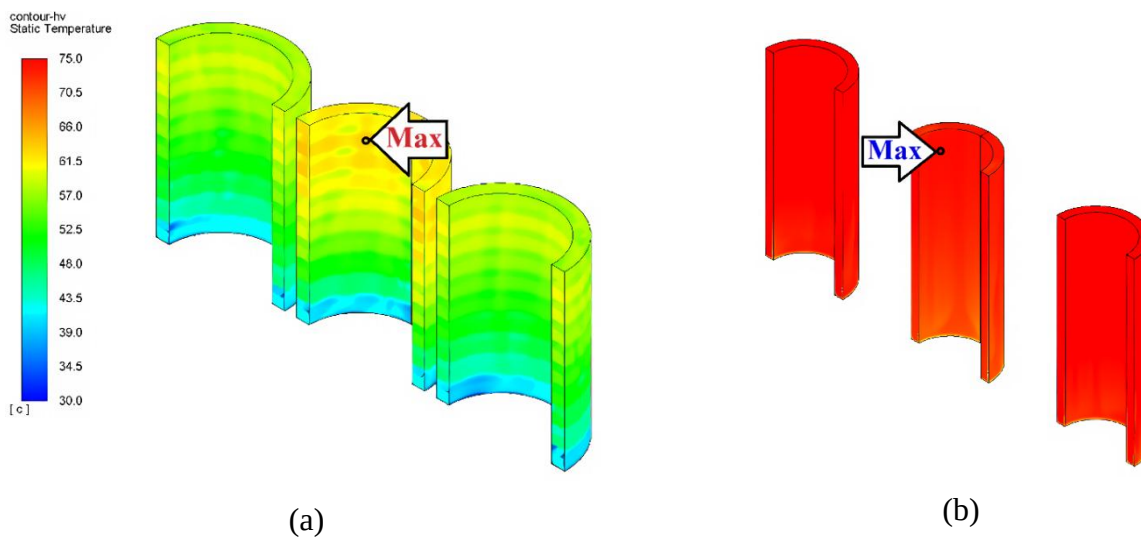


Figure 6. Temperature distribution of the HV (a) and LV (b) windings with the hotspot in phase B.

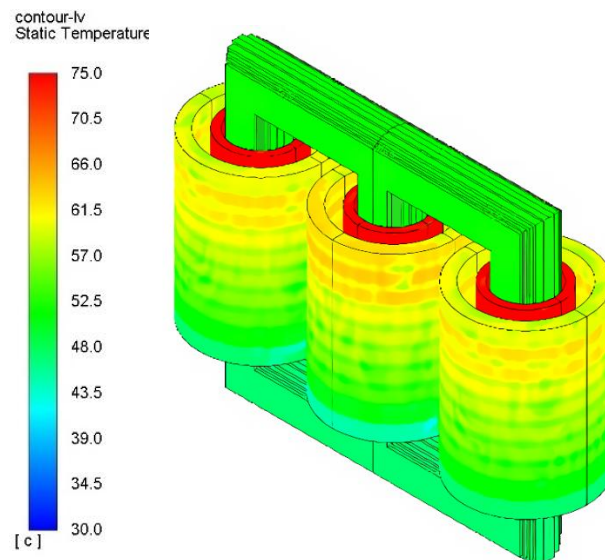


Figure 7. Temperature distribution of the three-phase transformer using constant thermal coefficients.

3.2. Experimental test

The experimental subject is a three-phase dry-type transformer rated at 320 kVA and 22/0.4 kV, with overall dimensions of 1340 mm × 930 mm × 1550 mm. Type-T thermocouples (Copper–Constantan, manufactured by OMRON) were employed for temperature measurement. A total of 27 thermocouples were directly attached to predefined measurement locations and connected to a data acquisition system (datalogger) (Figure 8).

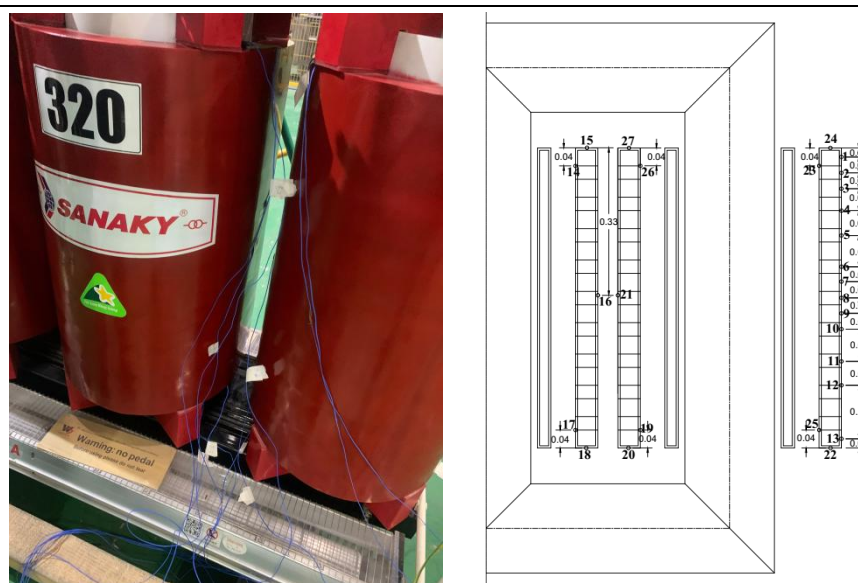


Figure 8. SANAKY transformer and the 27 spot-temperature measurement.

The measurement accuracy of the Type-T thermocouples is specified as $\pm 1\text{ }^{\circ}\text{C}$ or $\pm 0.75\%$ of the measured value. The operating temperature range of the thermocouples spans from -270 to $370\text{ }^{\circ}\text{C}$, ensuring adequate coverage for the present application. Data acquisition was initiated only after the system reached thermal steady-state conditions. In this study, steady state is defined as a temperature fluctuation of less than $1\text{ }^{\circ}\text{C}$ over a continuous period of 2 h.

The empirical thermal data acquired from the 27 measurement points are presented in Table 6, which reports the temperature recorded at each thermocouple location and thereby illustrates the thermal distribution profile of the transformer under steady-state conditions.

The numerical predictions are validated against experimental measurements obtained from 27 thermocouple locations distributed throughout the HV and LV windings of the three transformer phases under rated operating conditions. While Table 3 summarizes the complete measurement dataset, Tables 7 and 8 present a representative subset of 13 thermocouple locations positioned along the axial direction of the HV winding in Phase A, covering heights from 0.02 to 0.64 m. This measurement profile is selected to clearly illustrate the buoyancy-induced axial temperature gradient and the development of the hotspot near the upper winding region. The complete comparison for all 27 measurement locations is provided in Appendix 1 and Appendix 2. Using the experimentally identified temperature-dependent thermophysical properties, the proposed 3D CHT model shows excellent agreement with the experimental results. The maximum relative error is limited to 4.59% along the axial profile presented in Table 7 and remains below 5.1% across all 27 validation points (Appendix 1 and 2), demonstrating that the model accurately predicts both the overall temperature distribution and hotspot location under natural air-cooling conditions. For comparison, an additional simulation is performed using constant epoxy thermophysical properties obtained from standard reference data. In this case, the prediction accuracy deteriorates noticeably, with the maximum relative error increasing to 10.9% at the measurement height of 0.64 m, while most other locations exhibit errors close to 10%. These results demonstrate the importance of incorporating temperature-dependent material properties to improve the predictive capability of thermal simulations for dry-type transformers.

Table 6. Measured temperature on the surfaces of the LV and HV windings of the dry-type transformer.

Thermocouple number	Measured temperature (°C)	Thermocouple number	Measured temperature (°C)	Thermocouple number	Measured temperature (°C)
1	53.3	10	51.6	19	33.4
2	55.0	11	48.5	20	32.7
3	58.4	12	51.1	21	62.6
4	58.8	13	40.9	22	29.5
5	52.6	14	58.6	23	67.5
6	51.1	15	48	24	42.6
7	54.6	16	66.5	25	51.1
8	51.7	17	26.6	26	57.5
9	53.4	18	33.2	27	45.8

Table 7. Comparison of experimental and simulated temperatures (temperature-dependent thermal conductivity).

HV height	Experimental temperature	Simulated temperature	Relative error (%)
0.02	40.9	41.6	1.79
0.14	51.1	50.3	1.57
0.19	48.5	49.1	1.15
0.26	51.6	50.3	2.5
0.30	53.4	55.6	4.12
0.33	51.7	51.5	0.43
0.37	54.6	54.2	0.73
0.40	51.1	51.3	0.38
0.47	52.6	50.9	3.24
0.53	58.8	61.5	4.59
0.58	58.4	60.2	3.08
0.62	55.0	56.0	1.82
0.64	53.3	52.1	2.25

Table 8. Comparison of experimental and simulated temperatures (constant thermal conductivity).

HV height	Experimental temperature	Simulated temperature	Relative error (%)
0.02	40.9	42.2	3.1
0.14	51.1	56.3	10.2
0.19	48.5	49.6	2.2
0.26	51.6	48.0	7.0
0.30	53.4	58.9	10.3
0.33	51.7	51.8	0.2
0.37	54.6	58.0	6.2
0.40	51.1	54.0	5.7
0.47	52.6	50.0	4.9
0.53	58.8	59.4	1.1
0.58	58.4	64.2	9.9
0.62	55.0	50.0	9.1
0.64	53.3	47.5	10.9

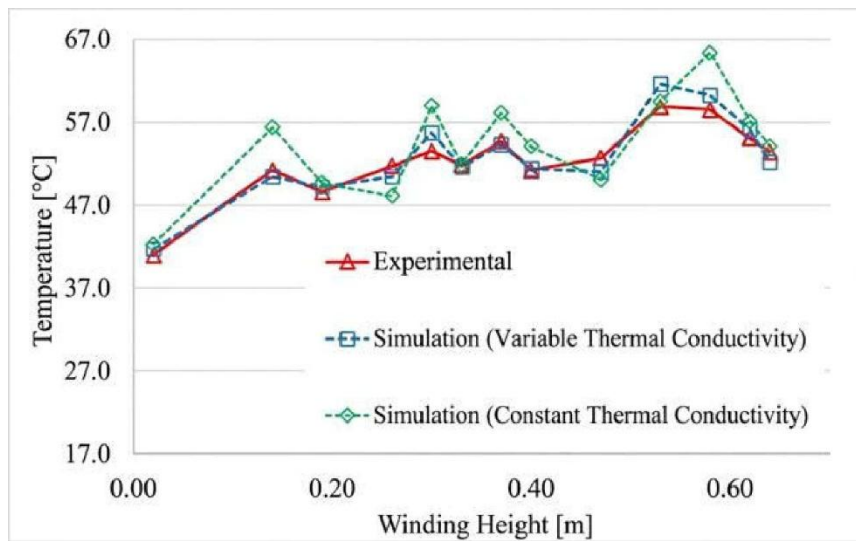


Figure 9. Comparison of simulation results versus experimental data.

The comparison indicates that the model incorporating temperature-dependent thermophysical properties significantly outperforms the constant-property model. The maximum prediction error obtained with constant material properties is more than twice that of the proposed variable-property model. This improvement is primarily attributed to the nonlinear variation of the epoxy resin thermal conductivity ($\lambda(T)$) and specific heat capacity ($C_p(T)$) with temperature. The influence of these temperature-dependent properties becomes increasingly significant under natural air-cooling conditions, where buoyancy-driven convection produces a pronounced axial temperature gradient along the winding height. As illustrated in Figure 9, the temperature profile predicted by the proposed model closely follows the experimental measurements over the entire winding height, whereas the constant-property model exhibits progressively larger deviations, particularly in the upper winding region where the hotspot develops. These results demonstrate that incorporating the temperature-dependent thermophysical properties of the epoxy insulation is essential for accurate thermal prediction in dry-type transformers. The validated model provides a reliable foundation for subsequent investigations, including hotspot prediction, insulation thermal aging analysis, and thermal safety assessment under severe operating conditions, such as prolonged overload.

4. Conclusions

This paper presented a three-dimensional CHT model for a 320 kVA cast-resin dry-type transformer by coupling finite element electromagnetic analysis with CFD. The model incorporates experimentally identified temperature-dependent thermophysical properties of the epoxy insulation to improve the prediction of heat transfer under natural air-cooling conditions. Validation against experimental measurements obtained at 27 thermocouple locations under rated load demonstrates the effectiveness of the proposed approach. Incorporating the temperature-dependent thermal conductivity ($\lambda(T)$) and specific heat capacity ($C_p(T)$) significantly improves thermal prediction accuracy. The proposed model achieves a maximum relative error of 4.59% along the investigated HV winding

profile and remains below 5.1% at all 27 validation points, whereas the conventional constant-property model exhibits a maximum error of 10.9%. The simulation identifies the highest temperature (75.0 °C) at the upper region of the Phase-B low-voltage winding, which agrees well with the measured temperature distribution. The predicted hotspot location is governed by buoyancy-driven natural convection and heat accumulation within the restricted airflow passages between adjacent windings. The results demonstrate that the temperature dependence of epoxy thermophysical properties has a significant influence on the predicted temperature field and hotspot location. Neglecting this effect introduces considerable prediction errors, which may affect transformer thermal design and insulation life assessment.

The proposed model provides a reliable numerical tool for thermal analysis of cast-resin dry-type transformers and can be extended to hotspot evaluation, insulation aging prediction, and thermal performance assessment under overload conditions. Future work will focus on fully coupled thermo–electromagnetic–mechanical analysis to investigate the interaction between temperature rise, thermal stress, and insulation reliability.

Author contributions

System Description; Simulation and Result Analysis, H.P.H. and T.L.D.; Draft Preparation, M.T.A.; Writing Review and Editing, M.T.A., H.P.H. and T.L.D.; Supervision, H.P.H. and T.L.D.; Project Administration, H.P.H. and T.L.D.; All authors reviewed the manuscript.

Use of Generative-AI tools declaration

The authors declare they have not used Artificial Intelligence (AI) tools in the creation of this article.

Conflict of interest

The authors declare there is no conflict of interest.

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Appendix

Appendix 1: Comparison of experimental and simulated temperatures along the HV winding (all instrumented HV locations).

HV height (thermocouple number)	Experimental temperature	Simulated temperature with constant thermal conductivity (relative error)	Simulated temperature with temperature-dependent thermal conductivity (relative error)
0.00 (22)	29.5	30.9 (4.7%)	30.2 (2.3%)
0.02 (13)	40.9	42.2 (3.1%)	41.6 (1.79%)
0.04 (25)	51.1	53.6 (4.89%)	52.4 (2.54%)
0.14 (12)	51.1	56.3 (10.2%)	50.3 (1.57%)
0.19 (11)	48.5	49.6 (2.2%)	49.1 (1.15%)
0.26 (10)	51.6	48.0 (7.0%)	50.3 (2.52%)
0.30 (9)	53.4	58.9 (10.3%)	55.3 (3.55%)
0.33 (8)	51.7	51.8 (0.2%)	51.5 (0.43%)
0.37 (7)	54.6	58.0 (6.2%)	54.2 (0.73%)
0.40 (6)	51.1	54.0 (5.7%)	51.3 (0.38%)
0.47 (5)	52.6	50.0 (4.9%)	50.9 (3.24%)

0.53 (4)	58.8	59.4 (1.1%)	61.5 (4.59%)
0.58 (3)	58.4	64.2 (9.9%)	60.2 (3.08%)
0.62 (2)	55.0	50.0 (9.1%)	56.0 (1.82%)
0.64 (23)	53.3	49.7 (6.75%)	54.2 (1.69%)
0.66 (1)	53.3	49.9 (6.37%)	52.1 (2.25%)
0.68 (24)	42.6	45.6 (7.04%)	43.2 (1.41%)

Appendix 2: Comparison of experimental and simulated temperatures along the LV winding (all instrumented LV locations).

LV height (thermocouple number)	Experimental temperature	Simulated temperature with constant thermal conductivity (relative error)	Simulated temperature with temperature-dependent thermal conductivity (relative error)
0.00 (18)	33.2	34.9 (5.12%)	33.9 (2.11%)
0.00 (20)	32.7	34.0 (3.98%)	33.5 (2.45%)
0.04 (17)	26.6	28.3 (6.39%)	26.9 (1.13%)
0.04 (19)	33.4	30.9 (7.49%)	35.1 (5.09%)
0.35 (16)	66.5	68.1 (2.41%)	63.7 (4.21%)
0.35 (21)	62.6	65.5 (4.63%)	60.1 (3.99%)
0.64 (26)	57.5	60.2 (4.70%)	58.3 (1.39%)
0.64 (14)	58.6	60.5 (3.24%)	56.8 (3.07%)
0.68 (15)	48	47.8 (0.42%)	49.7 (3.54%)
0.68 (27)	45.8	46.9 (2.40%)	47.0 (2.62%)



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