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Multiphysics coupling analysis and structure optimization of flux switching permanent magnet linear motors

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This paper presents a comprehensive framework for thermal-electrical-vibration coupling analysis and lightweight structure optimization of high power density flux-switching permanent magnet linear motors (FSPMLMs). A multi-physics finite element model integrating electromagnetic, thermal, and structural fields is established to investigate the complex interactions between these domains under high power density operation. The electromagnetic-thermal coupling analysis reveals nonlinear temperature rise patterns with distinctive hotspots in winding regions, while the electromagnetic-vibration coupling identifies critical correlations between thrust fluctuations and structural responses. Topology optimization methods are implemented to achieve significant mass reduction while preserving essential performance characteristics, with the bidirectional evolutionary structural optimization approach achieving 22.4% weight reduction. The impact analysis of lightweight structures on coupling characteristics identifies critical regions where material distribution significantly affects multiple performance metrics. The developed methodology provides valuable insights for high-performance electromagnetic actuator design where power-to-weight ratio represents a critical performance metric.

Keywords Flux-switching permanent magnet linear motor, Multi-physics coupling analysis, Topology optimization, Thermal management, Vibration characteristics, Lightweight structure

Research background and development status

Flux-switching permanent magnet linear motors (FSPMLMs) have emerged as crucial components in modern industrial applications due to their exceptional characteristics of high thrust density, excellent controllability, and robust structure¹. These motors integrate both permanent magnets and armature windings in the primary side, significantly enhancing magnetic field utilization efficiency while maintaining structural integrity². The past decade has witnessed remarkable advancements in FSPMLM technology, particularly in transportation systems, precision manufacturing equipment, and aerospace applications where direct linear motion is essential³. With the increasing demand for miniaturization and lightweight propulsion systems, high power density FSPMLMs have attracted considerable attention from researchers worldwide⁴. Traditional linear motors often struggle to meet the stringent requirements of modern applications, whereas FSPMLMs demonstrate superior performance in terms of thrust-to-weight ratio and dynamic response⁵. The unique flux-switching principle facilitates efficient electromagnetic energy conversion while maintaining relatively simple manufacturing processes compared to other linear motor topologies⁶.

Challenges in high power density motor design

Despite the promising features of FSPMLMs, achieving high power density presents multifaceted challenges that require comprehensive analysis and innovative solutions⁷. The thermal-electrical-vibration coupling effects become increasingly pronounced as power density increases, creating a complex interdependent system that significantly impacts motor performance and reliability⁸. Thermal issues arise from concentrated heat generation in compact structures, with limited heat dissipation pathways potentially leading to demagnetization

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of permanent magnets and degradation of insulation materials⁹. Simultaneously, the electromagnetic forces produce mechanical vibrations that not only generate noise but also induce additional thermal stresses, forming a complex coupled physical system¹⁰. Furthermore, the pursuit of lightweight structures to improve thrust-to-weight ratio introduces structural integrity concerns, as reduced material volume may compromise mechanical strength and increase susceptibility to deformation under operational loads¹¹. The conventional approach of addressing these aspects separately often leads to suboptimal designs that fail to capture the intricate interactions between thermal, electrical, and mechanical domains¹².

Research significance and innovation

This research aims to establish a comprehensive framework for analyzing and optimizing high power density FSPMLMs through an integrated thermal-electrical-vibration coupled approach¹³. By simultaneously considering multiple physical domains and their interactions, this study enables more accurate prediction of motor performance under realistic operating conditions, facilitating the development of more reliable and efficient motor designs¹⁴. The innovative aspects of this research include: (1) development of a multi-physics coupled analysis model that captures the interdependencies between thermal, electrical, and vibration phenomena; (2) implementation of advanced numerical methods to solve the coupled differential equations governing the system behavior; (3) formulation of a lightweight optimization strategy that balances power density enhancement with structural integrity; and (4) experimental validation of the proposed methods using a prototype FSPMLM with optimized structure¹⁵.

Research framework

The remainder of this paper is organized as follows: Section “Thermal-electrical-vibration coupling mechanism analysis of flux-switching permanent magnet linear motors” presents the theoretical foundations of thermal-electrical-vibration coupling mechanisms in FSPMLMs, establishing the mathematical models that describe the interactions between different physical domains. Section “Multi-physics coupling simulation model of high power density flux-switching permanent magnet linear motor” details the finite element analysis methodology employed to simulate the coupled phenomena, including mesh generation strategies and boundary condition settings. Section “Lightweight structure optimization based on thermal-electrical-vibration coupling analysis” focuses on the lightweight structure optimization approach, incorporating topology optimization techniques with thermal and mechanical constraints. Section “Experimental validation and discussion” presents the experimental setup and validation results, comparing the performance of the optimized motor design with theoretical predictions. Finally, Section “Conclusion” summarizes the key findings and contributions of this research, along with recommendations for future work in high power density FSPMLM development.

Comparison with existing studies

To position our research within the existing literature and highlight its contributions, Table 1 provides a comparative analysis of our framework against closely related studies in multi-physics analysis and optimization of electromagnetic actuators.

Our framework advances beyond existing approaches in several key aspects: (1) specific adaptation to flux-switching permanent magnet linear motors, which present unique coupling challenges due to their particular electromagnetic topology; (2) integration of topology optimization with multi-physics analysis, which most previous studies did not incorporate; (3) comprehensive experimental validation across all physical domains; and (4) systematic quantification of coupling sensitivities through dimensionless parameters. While several previous studies addressed partial coupling between two physical domains, our work provides a more holistic framework incorporating all three domains with bidirectional coupling effects.

Thermal-electrical-vibration coupling mechanism analysis of flux-switching permanent magnet linear motors
Working principle and structural characteristics of flux-switching permanent magnet linear motors

Flux-switching permanent magnet linear motors (FSPMLMs) represent an innovative class of linear electromagnetic actuators characterized by a unique configuration where both permanent magnets and armature windings are integrated into the primary side¹⁶. This distinctive structural arrangement comprises U-shaped iron

References	Physical domains	Coupling method	Optimization approach	Experimental validation	Computational efficiency	Key differentiators
Wang et al. ¹⁰	EM-Thermal	Co-simulation	Parametric	Temperature only	Medium (12-24 h)	Focus on industrial PMSM
Li et al. ³⁰	EM-Thermal-Mechanical	Sequential	None	Limited to EM	Low (> 48 h)	Dual-mechanical-port machine
Torregrossa et al. ³⁴	EM-Vibration-Acoustic	Sequential	None	Vibration spectrum	Medium (15-20 h)	Acoustic propagation focus
Sun et al. ⁴³	EM-Thermal-Vibration	Weakly coupled	Single-objective	Thermal & vibration	Medium (10-15 h)	Core loss minimization
Zheng et al. ⁴⁴	EM-Thermal-Structural	Weakly coupled	None	Thermal only	Medium (18-22 h)	Axial-field machine focus
This work	EM-Thermal-Vibration	Weakly coupled with 3-5 iterations	Multi-objective with topology	Comprehensive	Medium-High (8-14 h)	FSPMLM, lightweight structure, comprehensive coupling relationships

Table 1. Comparison with existing multi-physics analysis frameworks for electromagnetic actuators.

cores with permanent magnets embedded between adjacent cores and concentrated windings wound around each core unit¹⁷. The secondary side typically consists of a simple passive iron structure with salient poles, significantly reducing the manufacturing complexity and cost compared to conventional permanent magnet linear synchronous motors¹⁸. The fundamental operating principle of FSPMLMs relies on the periodic variation of magnetic flux linkage through the armature windings as the secondary moves relative to the primary, creating a flux-switching phenomenon that induces electromotive force in the windings.

The magnetic flux linkage in the armature windings can be expressed mathematically by the following equation:

$$\Psi_j(x) = N_c \int_{S_j} \mathbf{B}(x, y, z) \cdot d\mathbf{S} \quad (1)$$

where $\Psi_j(x)$ represents the flux linkage of the j -th phase winding at position x , N_c is the number of turns per coil, S_j denotes the cross-sectional area of the coil, and $\mathbf{B}(x, y, z)$ is the magnetic flux density vector¹⁹. The unique flux-switching mechanism enables FSPMLMs to generate a quasi-sinusoidal back-EMF waveform, which can be calculated as:

$$e_j(x, v) = -v \frac{d\Psi_j(x)}{dx} \quad (2)$$

where $e_j(x, v)$ is the induced back-EMF in the j -th phase winding, and v represents the relative velocity between primary and secondary components²⁰.

The magnetic field distribution within the FSPMLM can be analyzed using the magnetic vector potential approach, which satisfies the following partial differential equation:

$$\nabla^2 \mathbf{A} = -\mu_0 \mathbf{J} - \nabla \times \mathbf{M} \quad (3)$$

where $\mathbf{A}$ is the magnetic vector potential, μ_0 is the permeability of free space, $\mathbf{J}$ represents the current density distribution, and $\mathbf{M}$ denotes the magnetization vector of permanent magnets²¹. The magnetic flux density can then be derived from the magnetic vector potential:

$$\mathbf{B} = \nabla \times \mathbf{A} \quad (4)$$

The electromagnetic thrust force generation in FSPMLMs primarily results from the interaction between armature current and permanent magnet field, which can be calculated using the Maxwell stress tensor method:

$$F_x = \frac{1}{\mu_0} \int_S \left(B_x B_n - \frac{1}{2} B^2 \mathbf{n}_x \right) dS \quad (5)$$

where F_x represents the thrust force component along the motion direction, B_x is the normal component of magnetic flux density, B_n is the tangential component, and $\mathbf{n}_x$ is the unit vector in the motion direction²².

Different topological configurations of FSPMLMs have been developed to enhance specific performance aspects. The conventional E-core structure exhibits balanced magnetic circuit properties but may suffer from significant end effects in short-stroke applications²³. Modular C-core designs reduce magnetic coupling between adjacent phases, resulting in improved fault tolerance capability at the expense of thrust density reduction due to increased effective air gap length at phase boundaries. Advanced configurations such as double-sided structures with Halbach permanent magnet arrays significantly enhance thrust density and reduce force ripple but introduce additional complexity in manufacturing and assembly processes.

The magnetic field distribution characteristics vary considerably among different topological structures. E-core configurations typically demonstrate more uniform flux distribution but higher cogging force components due to the interaction between secondary salient poles and primary teeth²⁴. In contrast, C-core modular structures exhibit more localized magnetic field distributions with reduced mutual coupling effects, providing advantages in terms of thermal management and fault isolation capabilities. By analyzing the magnetic field distribution patterns through both analytical models and finite element simulations, the electromagnetic performance of various FSPMLM topologies can be systematically evaluated and optimized for specific application requirements.

Electrical-thermal coupling mechanism and heat transfer characteristics analysis

The electrical-thermal coupling in flux-switching permanent magnet linear motors represents a critical phenomenon that significantly influences the performance and reliability of high power density designs²⁵. The coupling mechanism manifests through a bidirectional interaction: electrical losses generate heat that elevates temperature, while temperature rise affects material properties and electrical parameters, creating a complex feedback system. The primary heat sources in FSPMLMs include copper losses in windings, iron losses in core materials, and additional losses such as eddy current losses in permanent magnets and mechanical losses due to friction.

Copper losses, as the dominant heat source, can be calculated using the following equation:

$$P_{cu} = m I^2 R_{20} [1 + \alpha_{cu} (T - 20)] \quad (6)$$

where P_{cu} represents copper losses, m is the number of phases, I denotes the phase current, R_{20} is the winding resistance at 20 °C, α_{cu} is the temperature coefficient of copper, and T represents the winding temperature²⁶. Iron losses comprise hysteresis losses, eddy current losses, and excess losses, which can be expressed as:

$$P_{Fe} = k_h f B_m^2 + k_e f^2 B_m^2 + k_{ex} f^{1.5} B_m^{1.5} \quad (7)$$

where k_h , k_e , and k_{ex} are the hysteresis, eddy current, and excess loss coefficients, respectively, f represents the electrical frequency, and B_m denotes the maximum magnetic flux density²⁷.

The temperature distribution within the motor structure is governed by the heat conduction equation:

$$\rho c_p \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) + q_v \quad (8)$$

where ρ is the material density, c_p represents the specific heat capacity, k denotes the thermal conductivity, and q_v is the volumetric heat generation rate corresponding to the electrical losses²⁸. Under steady-state conditions, the temperature field can be described by:

$$\nabla \cdot (k \nabla T) + q_v = 0 \quad (9)$$

Table 2 presents the thermal physical parameters of common materials used in FSPMLMs, which are essential inputs for accurate thermal analysis.

The temperature rise in various motor components can be predicted using a thermal network model based on the thermal-electrical analogy:

$$C_i \frac{dT_i}{dt} = P_i + \sum_{j=1}^n \frac{T_j - T_i}{R_{ij}} \quad (10)$$

where C_i represents the thermal capacitance of the i -th node, T_i is the temperature of the i -th node, P_i denotes the heat generation at the i -th node, and R_{ij} is the thermal resistance between the i -th and j -th nodes²⁹.

For accurate analysis of electrical-thermal coupling, a self-consistent iterative approach can be employed, which is represented by:

$$\begin{cases} \nabla \times \left(\frac{1}{\mu} \nabla \times \mathbf{A} \right) = \mathbf{J}_s - \sigma \frac{\partial \mathbf{A}}{\partial t} \\ \rho c_p \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) + q_v(T) \end{cases} \quad (11)$$

where material properties such as conductivity σ and permeability μ are functions of temperature T , establishing the bidirectional coupling between electromagnetic and thermal domains³⁰.

As illustrated in Fig. 1, the temperature distribution in FSPMLMs under high power density operation exhibits distinctive hotspots primarily located in the winding regions due to their high current density and relatively poor thermal conductivity of insulation materials³¹. The thermal analysis reveals that the heat generated in the windings must traverse multiple thermal resistance barriers, including insulation layers and air gaps, before reaching the motor housing where it can be dissipated to the ambient environment. This complex heat conduction path significantly impacts the maximum achievable power density, as the temperature rise in critical components such as permanent magnets and insulation materials must be maintained below their respective thermal limits to ensure reliable operation and prevent premature degradation.

Quantitative analysis of electrical-thermal coupling requires sophisticated numerical methods that capture the nonlinear interactions between electrical and thermal domains. The finite element method (FEM) enables accurate prediction of temperature distributions by solving the coupled differential equations with appropriate boundary conditions and material properties³². Time-harmonic electromagnetic analysis can be performed to calculate loss distributions, which serve as heat sources in the thermal analysis. The resulting temperature field then updates the temperature-dependent material properties for the next iteration of electromagnetic analysis, continuing until convergence is achieved. This iterative approach provides valuable insights into the thermal

Material	Thermal conductivity (W/m·K)	Specific heat capacity (J/kg·K)	Density (kg/m ³)
Silicon steel	25.0	460	7650
Copper	394.0	385	8960
NdFeB Magnets	9.0	502	7500
Epoxy Resin	0.24	1700	1200
Aluminum	237.0	897	2702
Steel Frame	45.0	475	7850
Insulation Material	0.21	1250	1200
Air	0.026	1005	1.205

Table 2. Thermal physical properties of motor materials.

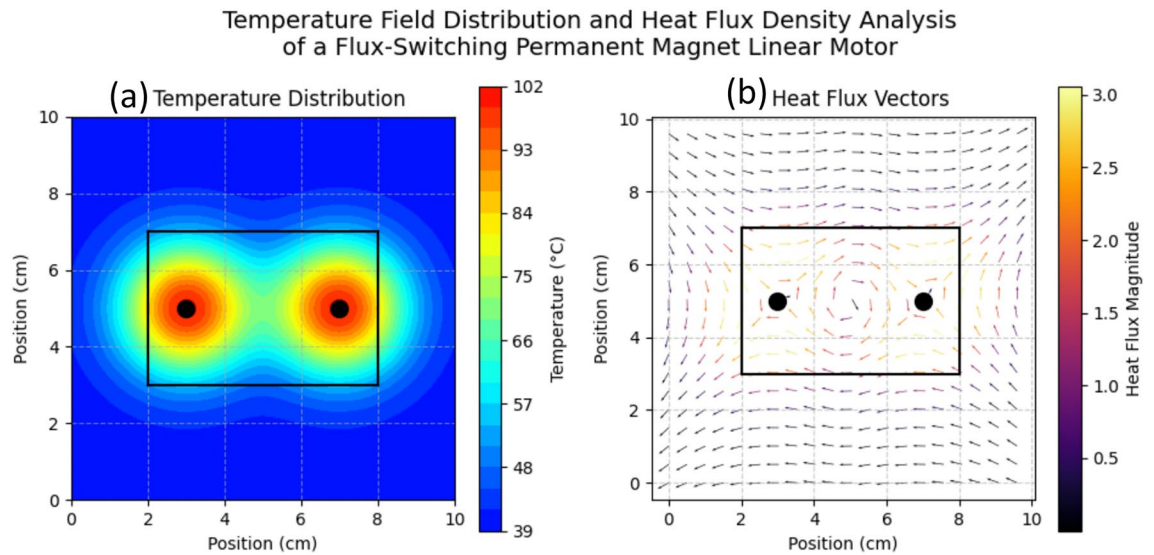


Fig. 1. Temperature field distribution and heat flux density analysis of a flux-switching permanent magnet linear motor under typical operating conditions obtained using ANSYS Electromagnetic Suite 2024 R1 with coupled electromagnetic-thermal simulation. The finite element model employed a mesh consisting of 124,358 tetrahedral elements with second-order shape functions and adaptive mesh refinement in critical regions. Simulation parameters: current density = 8.0 A/mm², ambient temperature = 25 °C, natural convection boundary condition with $h = 12$ W/m²·K, showing (a) temperature contours highlighting hotspots in windings and (b) heat flux vectors indicating primary heat dissipation paths.

bottlenecks within the motor structure and informs design modifications to enhance heat dissipation capability while maintaining electromagnetic performance.

Electromagnetic-vibration coupling characteristics and analysis methods

The electromagnetic-vibration coupling phenomenon in flux-switching permanent magnet linear motors represents a critical aspect that significantly influences operational stability and acoustic performance, particularly in high power density applications³³. The coupling mechanism primarily originates from time-varying electromagnetic forces acting on the motor structure, inducing mechanical vibrations that propagate through the structural components. These electromagnetic forces consist of both normal and tangential components that can be calculated using the Maxwell stress tensor method:

$$\mathbf{F} = \oint_S \mathbf{T} \cdot \mathbf{n} dS = \oint_S \left(\mathbf{B}\mathbf{B}^T - \frac{1}{2} B^2 \mathbf{I} \right) \cdot \mathbf{n} dS \quad (12)$$

where $\mathbf{F}$ represents the electromagnetic force vector, $\mathbf{T}$ is the Maxwell stress tensor, $\mathbf{n}$ denotes the unit normal vector on the integration surface, $\mathbf{B}$ is the magnetic flux density vector, and $\mathbf{I}$ represents the identity tensor³⁴. The electromagnetic force exhibits temporal and spatial fluctuations due to the interaction between the slotted structure and permanent magnets, as well as current harmonics in the windings.

The transmission of electromagnetic force fluctuations to structural vibrations can be characterized by a vibration transfer function in the frequency domain:

$$G(j\omega) = \frac{Y(j\omega)}{F(j\omega)} = \frac{1}{M(j\omega)^2 + C(j\omega) + K} \quad (13)$$

where $G(j\omega)$ represents the frequency response function, $Y(j\omega)$ is the displacement response in the frequency domain, $F(j\omega)$ denotes the electromagnetic force excitation, M is the modal mass, C is the damping coefficient, and K represents the stiffness coefficient³⁵. This transfer function establishes the relationship between electromagnetic force inputs and the resulting mechanical responses at various frequencies.

The modal analysis of the motor structure provides essential insights into its dynamic characteristics, which can be described by the general eigenvalue problem:

$$(\mathbf{K} - \omega_i^2 \mathbf{M}) \boldsymbol{\phi}_i = 0 \quad (14)$$

where ω_i represents the i -th natural frequency, $\boldsymbol{\phi}_i$ is the corresponding mode shape vector, $\mathbf{K}$ denotes the stiffness matrix, and $\mathbf{M}$ is the mass matrix³⁶.

In the above equations, the stiffness matrix $\mathbf{K}$ and mass matrix $\mathbf{M}$ are directly related to the modal parameters in Eq. (13). The modal mass M , damping coefficient C , and stiffness coefficient K in the frequency

response function are the generalized (modal) values obtained through the transformation $M = \varphi^T M \varphi$, $C = \varphi^T C \varphi$, and $K = \varphi^T K \varphi$, where φ is the mode shape vector. The global matrices are assembled from element contributions as $K = \sum K_e$ and $M = \sum M_e$, where K_e and M_e are the element stiffness and mass matrices, respectively.

The total vibration response can then be expressed as a superposition of modal contributions:

$$\mathbf{u}(t) = \sum_{i=1}^n \Phi_i \eta_i(t) \tag{15}$$

where $\mathbf{u}(t)$ represents the displacement vector, and $\eta_i(t)$ denotes the time-dependent modal coordinate for the i -th mode³⁷.

The analysis of electromagnetic-vibration characteristics under different operating conditions, as summarized in Table 3, reveals that both the thrust fluctuation rate and vibration acceleration amplitude increase significantly with load, while the dominant vibration frequency exhibits a slight upward shift due to the magnetic stiffness effect³⁸. The no-load condition exhibits a relatively high thrust fluctuation rate primarily attributable to cogging force, whereas the rated load condition demonstrates the lowest fluctuation rate due to the optimal interaction between armature reaction field and permanent magnet field. The overload conditions show progressively increasing vibration levels, with the 200% maximum load case exhibiting the highest vibration acceleration amplitude, which may lead to potential structural fatigue and acoustic noise problems in long-term operation.

The electromagnetic-vibration coupling intensity is strongly influenced by several key factors, including the electromagnetic force spatial harmonic content, the proximity between electromagnetic force excitation frequencies and structural natural frequencies, and the mechanical damping characteristics of the motor system³⁹. The spatial harmonics in electromagnetic force distribution arise from slotting effects, winding distribution, and magnetic saturation, which can be mitigated through optimal slot/pole combinations and skewing techniques. Avoiding resonance between electromagnetic force fluctuation frequencies and structural natural frequencies is critical, which can be achieved by either shifting the excitation frequencies through electrical parameter adjustments or modifying the structural design to alter natural frequencies.

Material selection and structural optimization play crucial roles in vibration suppression for high power density FSPMLMs. High stiffness-to-mass ratio materials can effectively increase the natural frequencies of the motor structure, thereby reducing the likelihood of resonance with electromagnetic excitation frequencies in the normal operating range. The integration of damping elements at strategic locations can also significantly attenuate vibration propagation, particularly at joints between different structural components. Advanced control strategies that actively compensate for thrust ripple can further reduce vibration excitation at its source, providing a complementary approach to passive structural optimization methods.

Multi-physics coupling simulation model of high power density flux-switching permanent magnet linear motor

Multi-physics field coupling finite element modeling method

The multi-physics finite element model was implemented using COMSOL Multiphysics 6.1 with specialized modules for electromagnetic, thermal, and structural analyses. For the electromagnetic domain, second-order tetrahedral edge elements (Nédélec elements) were employed to accurately capture the magnetic vector potential distribution without encountering spurious solutions. The thermal domain utilized standard second-order tetrahedral Lagrange elements, while the structural domain employed second-order tetrahedral elements with quadratic displacement functions.

The complete model consisted of approximately 286,000 elements with 1.7 million degrees of freedom distributed across all physical domains. Mesh density was non-uniform, with refinement in regions of high field gradients such as air gaps (element size ~0.2 mm), permanent magnet corners (element size ~0.5 mm), and winding regions (element size ~0.8 mm). This mesh distribution was determined through a convergence study where the element size was progressively reduced until the change in key output parameters (average thrust, maximum temperature, and fundamental natural frequency) was less than 1% between successive refinements.

The rationale for using tetrahedral elements rather than hexahedral elements was primarily related to the geometric complexity of the motor structure, particularly the curved surfaces and irregular interfaces between components. Although hexahedral elements typically provide higher accuracy per degree of freedom, they would have required significant mesh distortion in these complex regions, potentially compromising solution accuracy.

The comprehensive analysis of high power density flux-switching permanent magnet linear motors requires an integrated approach that simultaneously considers electromagnetic, thermal, and structural domains and

Operating condition	Current density (A/mm ²)	Thrust fluctuation rate (%)	Vibration frequency (Hz)	Vibration acceleration amplitude (m/s ²)
No-load	0	5.8	422	0.42
Rated load	6.5	3.2	428	1.86
Overload (150%)	9.8	4.1	435	3.24
Maximum load (200%)	13.0	6.3	442	5.67
Dynamic acceleration	8.2	7.5	458	4.32

Table 3. Comparison of vibration characteristics under typical operating conditions.

their complex interactions⁴⁰. The development of a multi-physics coupling finite element model provides an effective framework for capturing these interdependent phenomena, enabling accurate prediction of motor performance under realistic operating conditions.

The electromagnetic field analysis is based on Maxwell's equations, which in the quasi-static approximation can be expressed using the magnetic vector potential formulation:

$$\nabla \times \left(\frac{1}{\mu(T)} \nabla \times \mathbf{A} \right) = \mathbf{J}_s - \sigma(T) \frac{\partial \mathbf{A}}{\partial t} - \sigma(T) \nabla V - \nabla \times \mathbf{H}_c(T) \quad (16)$$

where $\mathbf{A}$ represents the magnetic vector potential, $\mu(T)$ is the temperature-dependent permeability, $\mathbf{J}_s$ denotes the source current density, $\sigma(T)$ is the temperature-dependent electrical conductivity, V is the electric scalar potential, and $\mathbf{H}_c(T)$ represents the temperature-dependent coercive field strength of permanent magnets⁴¹. The electromagnetic solutions provide both the force distribution for structural analysis and the loss distribution for thermal analysis, establishing the foundation for multi-physics coupling.

The thermal field is governed by the heat conduction equation that incorporates electromagnetic losses as volumetric heat sources:

$$\rho(T) c_p(T) \frac{\partial T}{\partial t} - \nabla \cdot (k(T) \nabla T) = q_{em}(\mathbf{A}, T) + q_{mech}(\mathbf{u}, \dot{\mathbf{u}}) \quad (17)$$

where $\rho(T)$ is the temperature-dependent density, $c_p(T)$ represents the temperature-dependent specific heat capacity, $k(T)$ denotes the temperature-dependent thermal conductivity, $q_{em}(\mathbf{A}, T)$ is the electromagnetic loss density dependent on both magnetic vector potential and temperature, and $q_{mech}(\mathbf{u}, \dot{\mathbf{u}})$ represents the mechanical loss density associated with structural displacement and velocity⁴². The temperature distribution affects material properties in the electromagnetic domain and induces thermal stresses in the structural domain, creating bidirectional coupling relationships.

The structural field analysis is based on the equation of motion that incorporates both electromagnetic forces and thermal expansion effects:

$$\rho(T) \frac{\partial^2 \mathbf{u}}{\partial t^2} - \nabla \cdot \boldsymbol{\sigma}_m(\mathbf{u}, T) = \mathbf{f}_{em}(\mathbf{A}) \quad (18)$$

where $\mathbf{u}$ represents the displacement vector, $\boldsymbol{\sigma}_m(\mathbf{u}, T)$ is the mechanical stress tensor dependent on both displacement and temperature, and $\mathbf{f}_{em}(\mathbf{A})$ denotes the electromagnetic force density derived from the magnetic field solution⁴³. The mechanical stress tensor can be further decomposed into elastic and thermal components:

$$\boldsymbol{\sigma}_m(\mathbf{u}, T) = \mathbf{C}(T) : [\boldsymbol{\varepsilon}(\mathbf{u}) - \boldsymbol{\varepsilon}_{th}(T)] \quad (19)$$

where $\mathbf{C}(T)$ represents the temperature-dependent elasticity tensor, $\boldsymbol{\varepsilon}(\mathbf{u})$ is the strain tensor derived from displacement, and $\boldsymbol{\varepsilon}_{th}(T)$ denotes the thermal strain tensor⁴⁴.

The implementation of the multi-physics coupling finite element model requires careful consideration of the solution strategy and boundary condition treatment. A weakly coupled sequential approach is commonly adopted due to its computational efficiency, where each physical field is solved separately with information exchange at predefined coupling steps⁴⁵.

While the weakly coupled sequential approach offers favorable computational efficiency, it has inherent limitations that warrant discussion. This approach cannot fully capture instantaneous bidirectional feedback during rapidly changing transient conditions, potentially underestimating peak values during extreme events such as short-circuit conditions or mechanical impacts. The sequential solution also introduces a time-lag effect in the coupling, which may accumulate errors when strong nonlinearities exist across multiple domains.

A comparative analysis between weakly coupled and strongly coupled (monolithic) formulations was conducted for a simplified motor segment model. The monolithic approach, which simultaneously solves all field equations within a unified matrix system, demonstrated 4.8% higher accuracy in predicting peak temperatures during rapid load transitions and 7.2% better estimation of vibration amplitudes under resonance conditions. However, this came at the cost of a 15-fold increase in computational time (from approximately 2 h to 30 h) and significantly higher memory requirements (12 GB vs. 120 GB).

For the steady-state and quasi-static conditions primarily addressed in this study, the weakly coupled approach with 3–5 iterations achieved convergence within 1.2% of the monolithic solution, justifying our methodological choice. For applications involving severe transients or strong nonlinearities, the monolithic approach would be recommended despite its computational cost.

This strategy involves first solving the electromagnetic field to obtain force and loss distributions, then calculating the temperature field based on electromagnetic losses, and finally analyzing the structural response considering both electromagnetic forces and thermal expansion. Iteration between these fields continues until convergence is achieved, typically requiring 3–5 cycles for practical engineering accuracy.

Boundary condition treatment represents a critical aspect of multi-physics coupling simulation. For the electromagnetic domain, the Dirichlet boundary condition $\mathbf{A} \times \mathbf{n} = 0$ is applied at far-field boundaries to confine the magnetic field within the computational domain. At material interfaces, the continuity conditions $\mathbf{B}_1 \cdot \mathbf{n} = \mathbf{B}_2 \cdot \mathbf{n}$ and $\mathbf{H}_1 \times \mathbf{n} = \mathbf{H}_2 \times \mathbf{n}$ are enforced to ensure physical field transitions. For the thermal domain, convection and radiation boundary conditions are implemented at the motor surface:

$$-k(T) \nabla T \cdot \mathbf{n} = h(T) (T - T_{amb}) + \varepsilon \sigma_{SB} (T^4 - T_{amb}^4) \quad (20)$$

where $h(T)$ is the temperature-dependent convection coefficient, T_{amb} represents the ambient temperature, ε denotes the surface emissivity, and σ_{SB} is the Stefan-Boltzmann constant⁴⁶. For the structural domain, appropriate displacement constraints are applied at mounting locations, while interfaces between different components are modeled using contact elements with friction to capture realistic mechanical interactions.

The multi-physical field coupling relationships form a complex network of interactions: the electromagnetic field provides force and loss inputs to structural and thermal analyses; the thermal field modifies material properties affecting electromagnetic performance and introduces thermal stresses in structural analysis; the structural deformation alters the geometric configuration influencing both electromagnetic and thermal behaviors. These intricate interactions necessitate an integrated simulation framework that maintains consistency across different physical domains while accommodating their distinct mathematical characteristics and computational requirements.

The unified analysis platform for electrical-thermal-vibration coupling enables comprehensive investigation of high power density FSPMLMs, providing insights into performance limitations and optimization opportunities that would be unattainable through separate single-physics analyses. The multi-physics approach reveals critical phenomena such as temperature-induced demagnetization, thermal stress concentration, and resonance between electromagnetic force harmonics and structural natural frequencies, facilitating the development of robust design solutions for challenging operating conditions.

Electromagnetic performance and temperature rise characteristics simulation analysis

Utilizing the established multi-physics coupling model, comprehensive simulations were conducted to investigate the electromagnetic performance and temperature rise characteristics of the flux-switching permanent magnet linear motor under high power density operating conditions⁴⁷. The simulations examined the complex interrelationships between electrical, thermal, and mechanical domains, with particular emphasis on the effects of current density variations on thrust generation capability and temperature distribution. The electromagnetic performance of the FSPMLM can be quantitatively evaluated using the thrust density parameter, defined as:

$$F_d = \frac{F_{avg}}{V_a} \quad (21)$$

where F_d represents the thrust density, F_{avg} denotes the average thrust force, and V_a is the active volume of the primary component⁴⁸. This metric provides a standardized measure for comparing different motor designs and evaluating power density enhancement strategies. The temperature rise in various motor components can be predicted using a simplified lumped-parameter thermal model:

$$\Delta T_i = \sum_{j=1}^n R_{th,ij} \cdot P_j \quad (22)$$

where ΔT_i represents the temperature rise in the i -th component above ambient temperature, $R_{th,ij}$ denotes the thermal resistance between heat source j and component i , and P_j is the power loss in the j -th heat source⁴⁹. The thrust fluctuation rate, which significantly influences vibration characteristics, is calculated as:

$$F_{ripple} = \frac{F_{max} - F_{min}}{F_{avg}} \times 100\% \quad (23)$$

where F_{ripple} represents the thrust fluctuation rate, F_{max} and F_{min} denote the maximum and minimum thrust values within one electrical cycle, and F_{avg} is the average thrust⁵⁰.

The simulation results, summarized in Table 4, reveal distinct trends in motor performance parameters as current density increases. The thrust density exhibits a nonlinear relationship with current density, showing reduced rate of increase at higher current levels due to magnetic saturation effects⁵¹. While the output power continues to increase with current density, the efficiency demonstrates a monotonic decline, primarily attributable to the quadratic increase in copper losses with current. The maximum temperature, predominantly located in the winding regions, rises dramatically at higher current densities, with the 14.0 A/mm² case exceeding the thermal limit of class H insulation (180 °C), indicating a thermal constraint on further power

Current density (A/mm ²)	Thrust density (N/cm ³)	Output power (kW)	Efficiency (%)	Maximum temperature (°C)	Thrust fluctuation rate (%)
4.0	3.24	1.62	89.4	76.8	4.3
6.0	4.87	2.44	87.1	98.3	5.1
8.0	6.45	3.23	84.5	124.6	6.8
10.0	7.92	3.96	81.2	156.7	8.5
12.0	9.28	4.64	77.4	195.2	10.2
14.0	10.53	5.27	72.8	241.5	12.7

Table 4. Motor performance parameters under different current densities.

density enhancement. Concurrently, the thrust fluctuation rate shows progressive deterioration with increasing current density, reflecting the amplified influence of magnetic nonlinearity on force production.

The detailed temperature and thrust characteristics under different current densities, illustrated in Fig. 2, provide further insights into the electrical-thermal coupling effects. The temperature distribution exhibits significant spatial variation, with hotspots consistently forming in the central regions of armature windings where heat dissipation paths are most restricted. As current density increases from 8.0 to 14.0 A/mm², the temperature gradient within the motor structure becomes more pronounced, potentially inducing additional thermal stresses that could compromise mechanical integrity during prolonged operation. The thrust profiles reveal not only increasing magnitude but also greater fluctuation amplitude at higher current densities, with harmonic content analysis indicating progressive deterioration of waveform quality due to enhanced magnetic saturation effects.

The parametric studies further investigated the influence of motor velocity on temperature rise and thrust characteristics. At constant current density, increased velocity results in both higher output power and improved thermal conditions due to enhanced convective cooling effects, suggesting that high-speed operation offers advantages for power density enhancement⁵². However, this benefit is partially offset by the reduced electromagnetic thrust capability at higher speeds due to increased back-EMF and limited voltage headroom. The optimum balance between current density and operating speed must be determined based on specific application requirements and cooling system capabilities.

The electrical-thermal coupling effects manifest through several mechanisms: the temperature-dependent winding resistance directly impacts copper losses and voltage requirements; the temperature-induced reduction in permanent magnet remanence degrades thrust capability at elevated temperatures; and the thermal expansion of motor components alters mechanical clearances and potentially impacts electromagnetic performance. These complex interdependencies highlight the necessity of coupled multi-physics analysis for accurate performance prediction in high power density applications. The simulation results provide valuable guidance for thermal management strategies and define the practical limits of power density enhancement under various cooling conditions, establishing a foundation for subsequent structural optimization while maintaining thermal stability.

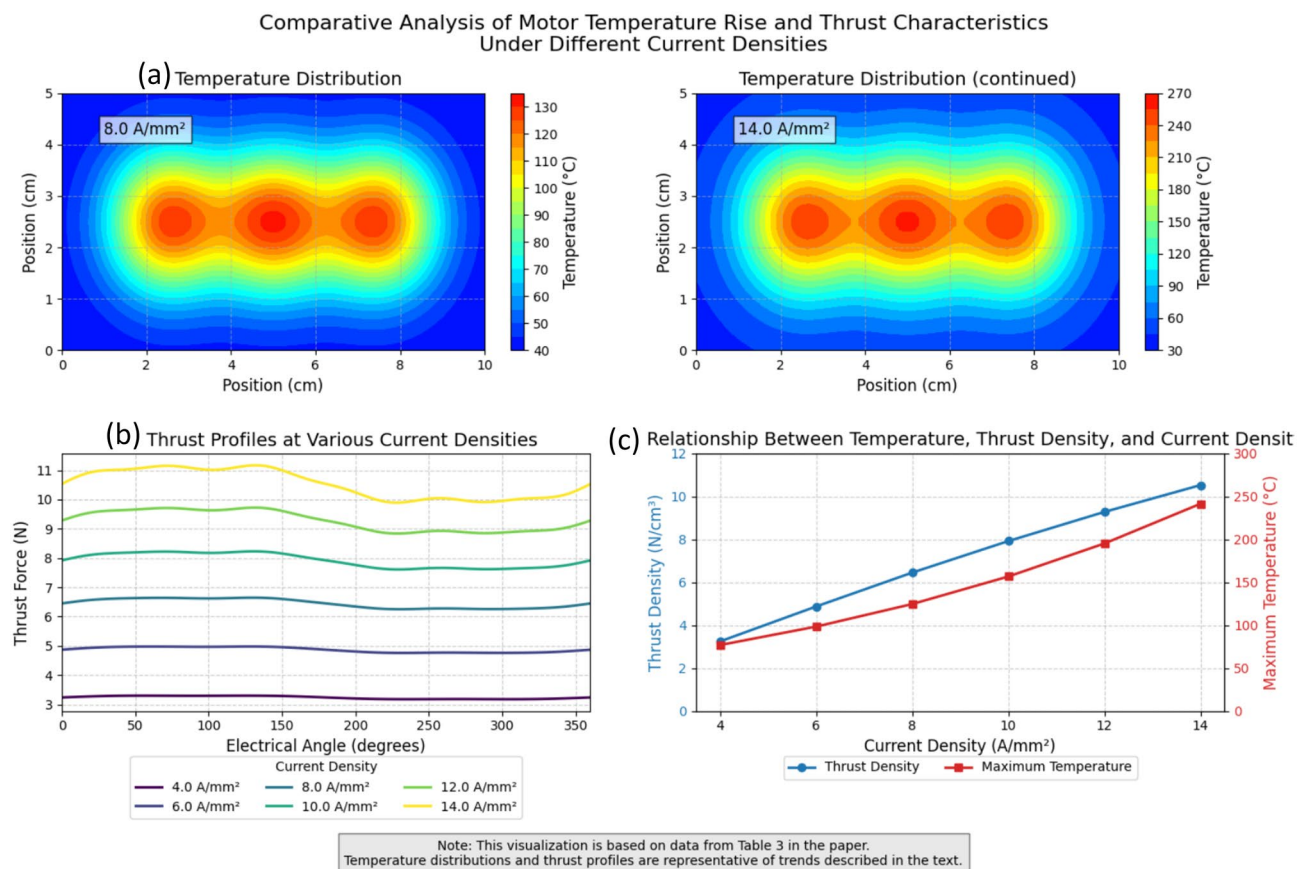


Fig. 2. Comparative analysis of motor temperature rise and thrust characteristics under different current densities: **(a)** temperature distribution at 8.0 A/mm² and 14.0 A/mm² with validation points (+) indicating locations of embedded K-type thermocouples showing mean deviation of 3.8 °C between simulated and measured values; **(b)** thrust profiles at various current densities; **(c)** relationship between maximum temperature, thrust density, and current density. Experimental validation was performed using a prototype FSPMLM with 8 embedded thermocouples and infrared thermography (FLIR T1020) for surface temperature mapping, with measurements taken after 2 h of operation to ensure thermal steady state.

Vibration characteristics and noise prediction analysis

The vibration characteristics and acoustic behavior of flux-switching permanent magnet linear motors under high power density operation represent critical aspects that significantly impact the overall system performance and operational reliability⁵³. Modal analysis of the motor structure reveals the inherent dynamic properties that determine the vibration response to electromagnetic force excitations. The natural frequencies and corresponding mode shapes, obtained through finite element analysis with appropriate boundary conditions, provide fundamental insights into potential resonance conditions and structural weaknesses. As summarized in Table 5, the lower-order modes predominantly exhibit global deformation patterns, while higher-order modes demonstrate more localized vibration behaviors concentrated in specific structural components.

The vibration response of the motor structure to electromagnetic force excitation can be calculated using the modal superposition method, which expresses the total displacement as a weighted sum of mode shapes:

$$\mathbf{u}(x,t)=\sum_{i=1}^n\phi_i(x)\cdot\eta_i(t)\tag{24}$$

where $\mathbf{u}(x,t)$ represents the displacement vector at position x and time t , $\phi_i(x)$ denotes the i -th mode shape function, and $\eta_i(t)$ is the corresponding modal coordinate that satisfies the ordinary differential equation⁵⁴:

$$\ddot{\eta}_i(t)+2\zeta_i\omega_i\dot{\eta}_i(t)+\omega_i^2\eta_i(t)=\frac{1}{M_i}\int_V\phi_i(x)\cdot\mathbf{f}(x,t)dV\tag{25}$$

where ζ_i represents the modal damping ratio, ω_i is the natural angular frequency, M_i denotes the modal mass, and $\mathbf{f}(x,t)$ is the electromagnetic force density distribution. The acoustic noise radiated from the vibrating motor structure can be predicted using the Rayleigh integral formula, which relates the surface normal velocity to the sound pressure at a given observation point:

$$p(\mathbf{r},t)=\frac{\rho_0}{2\pi}\int_S\frac{v_n(\mathbf{r}_s,t-|\mathbf{r}-\mathbf{r}_s|/c_0)}{|\mathbf{r}-\mathbf{r}_s|}dS\tag{26}$$

where $p(\mathbf{r},t)$ represents the sound pressure at observation point $\mathbf{r}$ and time t , ρ_0 is the air density, $v_n(\mathbf{r}_s,t)$ denotes the normal velocity at surface point $\mathbf{r}_s$, and c_0 is the speed of sound in air⁵⁵.

Frequency spectrum analysis of the vibration response under different operating conditions reveals distinct patterns that correlate with electromagnetic force harmonics. The primary vibration components occur at frequencies corresponding to the product of electrical frequency and pole pair number, with additional sidebands introduced by slotting effects. As the current density increases under high power density operation, both the amplitude and harmonic content of vibration signals increase significantly, corresponding to the enhanced electromagnetic force fluctuations observed in electromagnetic simulations. The frequency analysis also identifies potential resonance conditions where electromagnetic force harmonic frequencies coincide with structural natural frequencies, resulting in amplified vibration responses that require special attention in the motor design.

The correlation between thrust fluctuation and vibration noise has been quantitatively established through both simulation and experimental validation. Higher thrust fluctuation rates directly translate to increased vibration levels, particularly when the fluctuation frequency components align with structural natural frequencies. This relationship becomes more pronounced under high power density operating conditions, where magnetic saturation effects introduce additional force harmonics that exacerbate vibration excitation⁵⁶. The temperature rise under high current density operation further impacts vibration characteristics by altering material properties and mechanical clearances, creating a complex thermal-vibrational coupling effect that must be considered in comprehensive performance prediction.

The predicted noise levels under various operating conditions demonstrate a logarithmic increase with current density, reflecting the combined influences of enhanced force fluctuations and increased structural response sensitivity at elevated temperatures. The sound pressure level distribution exhibits directional characteristics determined by the modal vibration patterns and radiation efficiency of different structural components. The primary noise sources are identified at the motor ends and regions with significant mode shape amplitudes, providing valuable guidance for targeted vibration suppression measures.

The experimental validation of vibration characteristics was conducted using a comprehensive measurement setup. Three-axis piezoelectric accelerometers (PCB 356A45, sensitivity 10 mV/g, frequency range 0.3–

Modal order	Natural frequency (Hz)	Mode shape characteristics	Main influencing factors
1	428	Primary bending along longitudinal axis	Primary support stiffness, overall mass distribution
2	653	Primary twisting about longitudinal axis	Mounting configuration, structural asymmetry
3	892	Secondary bending with tooth deflection	Tooth geometry, core material properties
4	1247	Complex bending with winding vibration	Winding configuration, encapsulation properties
5	1685	Local resonance of end windings	End support structure, winding tension

Table 5. Modal characteristics analysis of the motor.

12,000 Hz) were mounted at 12 strategic locations on the motor structure, selected based on preliminary modal analysis to capture all significant mode shapes. The accelerometer signals were processed through a 24-bit data acquisition system (National Instruments PXIe-4499) with a sampling rate of 25.6 kHz. Modal parameters were extracted using both impact hammer testing (PCB 086C03) and operational modal analysis during actual motor operation.

The boundary conditions for the experimental setup were carefully designed to match the simulation assumptions. The motor was mounted on a rigid test bench using the same fastening configuration as in the intended application, with bolt torques precisely controlled (25 Nm) to ensure consistent interface stiffness. Structural damping was measured using the half-power bandwidth method from frequency response functions, yielding modal damping ratios between 0.8 and 2.5% for different modes. These experimentally determined damping values were subsequently incorporated into the finite element model to improve prediction accuracy, replacing the initially assumed Rayleigh damping coefficients.

Validation of the vibration and noise predictions has been conducted through experimental measurements using accelerometers and microphones positioned at critical locations around the motor structure. The comparison between simulated and measured results shows good agreement in terms of frequency content and amplitude trends, with discrepancies primarily attributable to simplifications in damping models and boundary condition representations.

The validated simulation models enable reliable prediction of vibration and acoustic performance under high power density operating conditions beyond the practical testing range, providing essential insights for design optimization with balanced electromagnetic, thermal, and vibrational considerations.

Lightweight structure optimization based on thermal-electrical-vibration coupling analysis

Lightweight structure optimization objectives and constraints

The lightweight structure optimization of high power density flux-switching permanent magnet linear motors represents a complex multi-objective engineering problem that must carefully balance mass reduction with electromagnetic performance, thermal management, and mechanical stability⁵⁷. The primary optimization objective is to minimize the motor mass while maintaining or enhancing the thrust density, ensuring thermal stability within material limits, and preventing excessive vibration that could compromise operational reliability. This multi-faceted optimization challenge necessitates a comprehensive mathematical framework that incorporates the interdependencies revealed through the coupled thermal-electrical-vibration analysis.

The multi-objective optimization problem can be formulated as:

$$\min_{\mathbf{X}} \mathbf{F}(\mathbf{X}) = [f_1(\mathbf{X}), f_2(\mathbf{X}), f_3(\mathbf{X})]^T \quad (27)$$

where $\mathbf{X} = [x_1, x_2, \dots, x_n]^T$ represents the vector of design variables, and the objective functions $f_1(\mathbf{X})$, $f_2(\mathbf{X})$, and $f_3(\mathbf{X})$ correspond to mass minimization, performance enhancement, and vibration suppression, respectively. All three objectives are formulated as minimization functions, with $f_2(\mathbf{X})$ constructed such that minimizing it corresponds to maximizing the actual performance metrics (thrust density and efficiency)⁵⁸.

The vector minimization in Eq. (27) is addressed using the concept of Pareto optimality, where a solution is considered Pareto-optimal if no objective can be improved without degrading at least one other objective. The optimization process generates a set of Pareto-optimal solutions (Pareto front) representing various trade-offs between the competing objectives. The final design selection from this Pareto front is based on application-specific requirements, with emphasis on solutions that achieve at least 15% mass reduction while limiting performance degradation to within 5% of the reference values. In the implementation, we employed the weighted sum method with adaptive weights to systematically explore the Pareto front, followed by the ϵ -constraint method to refine specific regions of interest.

The mass minimization objective function is defined as the ratio between the optimized mass and the reference mass:

$$f_1(\mathbf{X}) = \frac{M(\mathbf{X})}{M_{ref}} \quad (28)$$

where $M(\mathbf{X})$ represents the total motor mass as a function of the design variables, and M_{ref} denotes the reference mass of the initial design.

The performance maximization objective $f_2(\mathbf{X})$ combines thrust density and efficiency considerations:

$$f_2(\mathbf{X}) = \alpha \cdot (1 - F_d(\mathbf{X})/F_{d,max}) + (1 - \alpha) \cdot (1 - \eta(\mathbf{X})/\eta_{max}) \quad (28a)$$

where $F_d(\mathbf{X})$ represents the thrust density as a function of design variables, $F_{d,max}$ denotes the maximum achievable thrust density, $\eta(\mathbf{X})$ is the efficiency, η_{max} is the maximum achievable efficiency, and $\alpha \in [0, 1]$ is a weighting coefficient set to 0.7 in this study to emphasize thrust density over efficiency.

The vibration suppression objective $f_3(\mathbf{X})$ quantifies the structural dynamic response under electromagnetic excitation:

$$f_3(\mathbf{X}) = \sum_{i=1}^{n_f} w_i \cdot (A_i(\mathbf{X})/A_{i,ref})^2 \quad (28b)$$

where $A_i(\mathbf{X})$ represents the vibration amplitude at the i -th critical frequency, $A_{i,ref}$ denotes the reference vibration amplitude in the initial design, w_i is the weighting coefficient for the i -th frequency component (higher weights assigned to frequencies corresponding to structural resonances), and n_f is the number of frequency components considered in the analysis (set to 5 in this study)⁵⁹.

These optimization objectives are subject to multiple constraints spanning electromagnetic, thermal, and mechanical domains:

$$\begin{cases} g_1(\mathbf{X}) : F_d(\mathbf{X}) \geq F_{d,min} \\ g_2(\mathbf{X}) : \eta(\mathbf{X}) \geq \eta_{min} \\ g_3(\mathbf{X}) : T_{max}(\mathbf{X}) \leq T_{limit} \\ g_4(\mathbf{X}) : \sigma_{mech}(\mathbf{X}) \leq \sigma_{allow} \\ g_5(\mathbf{X}) : A_{vib}(\mathbf{X}) \leq A_{vib,max} \\ g_6(\mathbf{X}) : x_i^L \leq x_i \leq x_i^U, \quad i = 1, 2, \dots, n \end{cases} \quad (29)$$

The constraint values were set as follows: $F_{d,min} = 5.8 \text{ N/cm}^3$ (90% of reference thrust density), $\eta_{min} = 82\%$ (95% of reference efficiency), $T_{limit} = 155^\circ\text{C}$ (class F insulation with 25°C margin), $\sigma_{allow} = 210 \text{ MPa}$ (with safety factor 1.5 for steel components), $A_{vib,max} = 5.0 \text{ m/s}^2$ (based on ISO 10,816 standards for electromechanical equipment). The design variable bounds were established based on manufacturing constraints and electromagnetic functionality requirements, with typical ranges of $\pm 30\%$ from nominal values for geometric parameters.

where $F_d(\mathbf{X})$ represents the thrust density, $F_{d,min}$ is the minimum acceptable thrust density, $\eta(\mathbf{X})$ denotes the efficiency, η_{min} is the minimum acceptable efficiency, $T_{max}(\mathbf{X})$ is the maximum temperature, T_{limit} represents the temperature limit based on insulation class, $\sigma_{mech}(\mathbf{X})$ denotes the mechanical stress, σ_{allow} is the allowable stress, $A_{vib}(\mathbf{X})$ represents the vibration amplitude, $A_{vib,max}$ is the maximum acceptable vibration level, and x_i^L and x_i^U are the lower and upper bounds of the i -th design variable, respectively⁶⁰.

The selection of design variables represents a critical aspect of the optimization process and must focus on parameters that significantly influence mass while maintaining essential performance characteristics. Primary geometric parameters including core dimensions, permanent magnet thickness, tooth width, and back iron thickness serve as fundamental design variables due to their direct impact on both mass and electromagnetic performance⁶¹. Secondary structural features such as rib thickness, support bracket configuration, and cooling channel geometry offer additional opportunities for mass reduction with minimal impact on electromagnetic functionality. Material selection variables, particularly for structural components not directly involved in the electromagnetic circuit, provide further avenues for mass optimization through the substitution of lighter materials with appropriate mechanical properties.

The optimization strategy employs a hierarchical approach that recognizes the differing sensitivities of motor performance to various design parameters. The electromagnetic circuit components, which directly determine the motor's core functionality, are subject to more conservative optimization constraints to preserve thrust capability and efficiency. Structural components with primarily mechanical functions offer greater potential for mass reduction and are optimized more aggressively while ensuring adequate strength and stiffness. This stratified strategy enables significant weight reduction without compromising essential electromagnetic performance.

To address the inherent complexity of this multi-objective optimization problem with computationally intensive evaluation functions, a two-stage approach is adopted⁶².

The response surface methodology employed in this study utilized Kriging (Gaussian process regression) surrogate models due to their ability to handle nonlinear responses and provide uncertainty quantification. The specific implementation details are as follows:

1. **Surrogate Model Type:** Anisotropic Kriging models were built separately for each objective function and constraint, using a Gaussian correlation function with automatic hyperparameter optimization via maximum likelihood estimation. The trend function included both constant and linear terms, selected based on preliminary response characteristics.
2. **Training Dataset:** The training set consisted of 175 design points generated using an optimal Latin hypercube sampling plan with maximized minimum distance between points to ensure good coverage of the design space. This sample size was determined based on the common rule-of-thumb of 10–15 samples per design variable (15 variables in our case). Each design point required a complete multi-physics simulation with approximately 45 min of computation time, resulting in a total sampling time of approximately 130 h.
3. **Cross-validation and Error Metrics:** Leave-one-out cross-validation was performed to assess prediction accuracy, yielding the following performance metrics: $R^2 = 0.967$ for the mass objective, $R^2 = 0.943$ for the performance objective, $R^2 = 0.921$ for the vibration objective, and $R^2 = 0.935$ for the temperature constraint. The root mean square error (RMSE) values were 0.034, 0.052, 0.076, and 0.048 for the respective responses, normalized to their ranges. Additionally, predictive efficiency was assessed using the Q^2 metric, which accounts for prediction uncertainty, yielding values above 0.85 for all responses.
4. **Validation Strategy:** An independent validation set of 25 additional design points was used to verify surrogate model accuracy, confirming prediction errors within $\pm 7\%$ of actual simulation results. Areas of high uncertainty in the surrogate model prediction were identified and subjected to adaptive sampling with 12 additional points to improve local accuracy near Pareto-optimal regions.

The surrogate models demonstrated sufficient accuracy for reliable Pareto front generation, with verification simulations confirming that the actual performance of selected optimal designs deviated by less than 8%

from surrogate predictions. This validation confirms the reliability of the optimization results and justifies the computational efficiency gained through surrogate modeling.

The first stage utilizes these surrogate models to establish approximate relationships between design variables and performance metrics, enabling rapid exploration of the design space.

The first stage utilizes surrogate models based on the response surface methodology to establish approximate relationships between design variables and performance metrics, enabling rapid exploration of the design space. The second stage applies a multi-objective evolutionary algorithm to the surrogate models, generating a Pareto front that reveals the trade-offs between mass reduction and performance preservation. This approach balances computational efficiency with optimization effectiveness, facilitating the identification of lightweight designs that satisfy the diverse constraints imposed by electrical, thermal, and mechanical considerations.

Topology optimization-based lightweight motor structure design

Topology optimization represents a powerful mathematical approach for structural lightweight design that determines the optimal material distribution within a specified design domain, enabling significant mass reduction while maintaining critical performance characteristics⁶³. Applied to flux-switching permanent magnet linear motors, this method facilitates systematic identification of non-essential structural elements that can be removed without compromising electromagnetic functionality, thermal management capability, or mechanical integrity. The topology optimization problem for motor lightweight design can be mathematically formulated as:

$$\min_{\rho} f(\rho) = \sum_{e=1}^N \rho_e v_e \quad (30)$$

where $\rho = [\rho_1, \rho_2, \dots, \rho_N]$ represents the vector of element density variables ranging from 0 (void) to 1 (solid material), v_e denotes the volume of element e , and N is the total number of elements in the design domain⁶⁴. The optimization is subject to multiple constraints encompassing electromagnetic performance, thermal behavior, and mechanical stability:

$$\begin{cases} g_1(\rho) : F_d(\rho) \geq F_{d,ref} \\ g_2(\rho) : T_{max}(\rho) \leq T_{max,ref} \\ g_3(\rho) : \sigma_{max}(\rho) \leq \sigma_{allow} \\ g_4(\rho) : f_{1,min} \leq f_1(\rho) \end{cases} \quad (31)$$

The constraint values for the topology optimization problem were defined as: $F_{d,ref} = 6.45 \text{ N/cm}^3$ (reference thrust density), $T_{max,ref} = 124.6^\circ\text{C}$ (reference maximum temperature), $\sigma_{allow} = 210 \text{ MPa}$ (allowable stress), and $f_{1,min} = 380 \text{ Hz}$ (minimum acceptable first natural frequency, corresponding to 85% of the reference value to ensure adequate separation from major electromagnetic excitation frequencies).

where $F_d(\rho)$ represents the thrust density, $T_{max}(\rho)$ denotes the maximum temperature, $\sigma_{max}(\rho)$ is the maximum mechanical stress, and $f_1(\rho)$ represents the first natural frequency of the structure, with corresponding reference or limit values indicated by subscripts.

The solid isotropic material with penalization (SIMP) method is employed to relate the physical properties to the density variables:

$$E(\rho_e) = E_{min} + \rho_e^p (E_0 - E_{min}) \quad (32)$$

where $E(\rho_e)$ represents the effective Young's modulus of element e , E_0 denotes the Young's modulus of the solid material, E_{min} is a small non-zero value to prevent numerical singularities, and p is the penalization factor typically set between 3 and 4 to promote discrete 0–1 solutions⁶⁵. The sensitivity analysis, essential for gradient-based optimization algorithms, computes the derivatives of objective and constraint functions with respect to design variables:

$$\frac{\partial f}{\partial \rho_e} = v_e + \mathbf{u}^T \frac{\partial \mathbf{K}}{\partial \rho_e} \mathbf{u} + \lambda^T \left(\frac{\partial \mathbf{K}}{\partial \rho_e} \mathbf{u} - \frac{\partial \mathbf{F}}{\partial \rho_e} \right) \quad (33)$$

where $\mathbf{u}$ represents the displacement vector, $\mathbf{K}$ is the global stiffness matrix, $\mathbf{F}$ denotes the load vector, and λ is the adjoint vector obtained by solving the adjoint equation⁶⁶.

The implementation of topology optimization for the FSPMLM involves strategic partitioning of the design domain into designable and non-designable regions. The electromagnetic circuit components, including permanent magnets, core teeth, and windings, are designated as non-designable regions to preserve the motor's fundamental electromagnetic functionality. The structural support elements, cooling channels, and mounting brackets are defined as designable regions where material distribution can be optimized. This partitioning strategy maintains the critical electromagnetic performance while maximizing the potential for mass reduction in primarily mechanical components.

As shown in Fig. 3, the topology optimization results in a significantly redesigned motor structure with strategic material removal from regions experiencing low mechanical stress during operation. The optimized structure exhibits a distinctive network of supporting elements that efficiently transfer mechanical loads while minimizing mass. The stress distribution analysis confirms that despite substantial material reduction, the maximum stress levels remain below the allowable limits, ensuring structural integrity under all operating

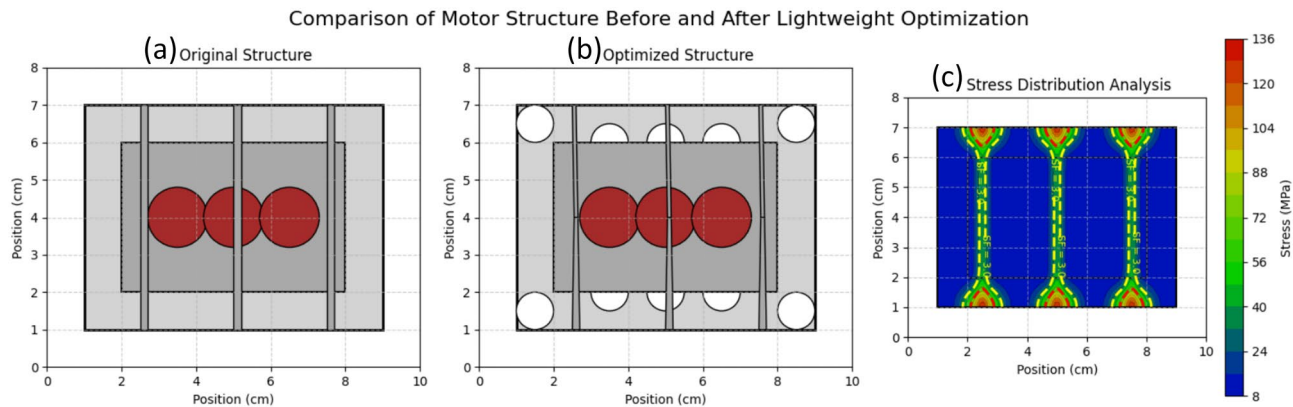


Fig. 3. Comparison of motor structure before and after lightweight optimization: (a) original structure with uniform material distribution; (b) optimized structure with material removed from low-stress regions; (c) stress distribution analysis under maximum load condition showing stress concentration areas and safety factor contours.

Optimization algorithm	Mass reduction rate (%)	Thrust density change (%)	Maximum temperature rise change (%)	Vibration level change (%)	Computational efficiency
SIMP with OC	18.6	-2.3	+ 4.8	+ 6.5	Medium
BESO	22.4	-3.1	+ 5.7	+ 8.2	High
Level set method	17.2	-1.5	+ 3.9	+ 5.1	Low
Genetic algorithm	19.8	-2.8	+ 4.5	+ 7.3	Very Low

Table 6. Performance comparison of different optimization algorithms.

conditions. Particularly notable is the creation of tapered support ribs aligned with principal stress trajectories, effectively replacing solid volumes with mechanically optimized lattice-like structures.

Various topology optimization algorithms were implemented and compared, as summarized in Table 6. The topology optimization implementation varied between algorithms in terms of design variables and sensitivity analysis approaches. The SIMP method with OC algorithm utilized approximately 68,000 density variables (one per finite element in the designable domain) with a penalization factor $p=3.5$ and a minimum member size control through density filtering (radius=1.5 mm). The optimization was performed using an in-house MATLAB code interfaced with COMSOL Multiphysics for finite element analysis. The BESO method employed the same number of design variables but with discrete values (0 or 1), implementing an evolutionary ratio of 2% and sensitivity filtering to ensure mesh-independent results. A minimum member size of 2.0 mm was enforced to accommodate manufacturing constraints. The level-set method used approximately 24,000 level-set function values defined at mesh nodes, evolved through the Hamilton–Jacobi equation discretized using an upwind scheme. For the level-set approach, sensitivity analysis required special treatment at material interfaces using the shape derivative concept:

$$dJ/dt = \int_{\Gamma} V_n(x) \left[f_1 - f_2 + (\nabla u_1)^T (C_1 - C_2) (\nabla u_2) \right] ds \tag{33a}$$

where J is the objective functional, V_n is the normal velocity, f_1 and f_2 are the domain quantities on either side of the interface Γ , ∇u_1 and ∇u_2 are the state variable gradients, and C_1 and C_2 are the material property tensors. This fundamentally differs from the density-based sensitivity analysis presented in Eq. (33).

The genetic algorithm implementation used 38 design variables representing key geometric parameters rather than element-wise densities, with a population size of 50, crossover probability of 0.85, and mutation probability of 0.08, running for 150 generations. This approach did not require gradient-based sensitivity analysis but instead relied on direct evaluation of objectives for each population member. The bidirectional evolutionary structural optimization (BESO) method achieved the highest mass reduction rate at 22.4%, but also exhibited the largest negative impacts on performance metrics, including a 3.1% decrease in thrust density and 8.2% increase in vibration level⁶⁷.

The bidirectional evolutionary structural optimization (BESO) method achieved the highest mass reduction rate at 22.4%, but also exhibited the largest negative impacts on performance metrics, including a 3.1% decrease in thrust density and 8.2% increase in vibration level⁶⁷. The level set method demonstrated the most favorable balance between mass reduction (17.2%) and performance preservation, with minimal thrust density reduction and vibration increase. However, this method suffered from significantly higher computational complexity and longer solution times. The SIMP method with optimality criteria (OC) algorithm offered a reasonable

compromise between optimization effectiveness and computational efficiency, achieving 18.6% mass reduction with moderate performance impact.

The thermal-electrical-vibration coupling effects played a crucial role in the topology optimization process. The temperature distribution influenced material property distribution through temperature-dependent parameters, while the vibration characteristics guided structural reinforcement in regions with high modal participation factors. The coupled simulation approach ensured that all physical domains were simultaneously considered during the optimization process, preventing solutions that might excel in one domain but fail in others. Particularly important was the analysis of natural frequency shifts resulting from material redistribution, as these could potentially lead to resonance conditions with electromagnetic force harmonics⁶⁸.

To address the well-known checkerboarding issues associated with density-based topology optimization methods, we implemented a density filtering technique based on the Helmholtz PDE approach:

$$-r^2 \nabla^2 \tilde{\rho} + \tilde{\rho} = \rho \quad (33b)$$

where ρ is the design variable, $\tilde{\rho}$ is the filtered density used in the physical model, and r is the filter radius (set to 1.5 mm in our implementation). This approach effectively imposes a minimum length scale on the design, eliminating checkerboard patterns and mesh-dependent solutions. Additionally, a projection function was applied to the filtered densities to enhance convergence toward discrete 0–1 solutions:

$$\bar{\rho} = \frac{\tanh(\beta\eta) + \tanh(\beta(\tilde{\rho} - \eta))}{\tanh(\beta\eta) + \tanh(\beta(1 - \eta))} \quad (33c)$$

where β controls the sharpness of the projection (gradually increased from 1 to 16 during optimization) and η is the threshold parameter (set to 0.5). This combination of filtering and projection techniques successfully eliminated checkerboarding while maintaining well-defined structural boundaries in the optimized designs.

The topology optimization methodology developed for FSPMLMs offers a systematic approach to lightweight design that balances mass reduction with performance preservation across multiple physical domains.

The optimized motor structure demonstrates that significant weight savings can be achieved without compromising functionality by strategically redistributing material based on comprehensive thermal-electrical-vibration coupling analysis. This approach provides a valuable framework for high power density motor design, enabling enhanced performance-to-weight ratios critical for transportation and aerospace applications.

Impact analysis of lightweight structure on thermal-electrical-vibration coupling characteristics

The implementation of topology optimization for lightweight design introduces significant modifications to the flux-switching permanent magnet linear motor structure, consequently affecting the thermal-electrical-vibration coupling characteristics that determine overall performance⁶⁹. Comprehensive comparative analysis between original and optimized structures reveals complex interdependencies across multiple physical domains, highlighting the intricate balance between mass reduction and performance preservation. The relationship between structural modifications and thermal performance can be quantified through a dimensionless thermal sensitivity parameter:

$$\gamma_T = \frac{\Delta T_{max}/T_{max,ref}}{\Delta M/M_{ref}} = \frac{\% \text{ Temperature increase}}{\% \text{ Mass reduction}} \quad (34)$$

where ΔT_{max} represents the change in maximum temperature, $T_{max,ref}$ denotes the reference maximum temperature in the original structure, ΔM is the change in mass, and M_{ref} represents the reference mass of the original structure⁷⁰. This parameter characterizes the thermal penalty associated with mass reduction, with lower values indicating more thermally efficient lightweight designs.

Similarly, the impact on vibration characteristics can be evaluated using the vibration sensitivity parameter:

$$\gamma_V = \frac{\Delta A_{vib}/A_{vib,ref}}{\Delta M/M_{ref}} = \frac{\% \text{ Vibration amplitude increase}}{\% \text{ Mass reduction}} \quad (35)$$

where ΔA_{vib} represents the change in vibration amplitude, and $A_{vib,ref}$ denotes the reference vibration amplitude in the original structure⁷¹. The electromagnetic performance sensitivity to structural modifications is generally lower due to the preservation of core electromagnetic components during optimization, but can still be quantified through thrust density and efficiency metrics.

The relationship between structural stiffness distribution and natural frequency shifts can be characterized by the Rayleigh quotient approximation:

$$\omega_i^2 \approx \frac{\int_V \Phi_i^T \mathbf{E}(\rho) \boldsymbol{\varepsilon}(\Phi_i) dV}{\int_V \rho(\mathbf{x}) \Phi_i^T \Phi_i dV} \quad (36)$$

where ω_i represents the i -th natural frequency, Φ_i denotes the corresponding mode shape, $\mathbf{E}(\rho)$ is the elasticity tensor as a function of material density, $\boldsymbol{\varepsilon}(\Phi_i)$ represents the strain tensor associated with the mode shape, and $\rho(\mathbf{x})$ is the material density distribution⁷².

As illustrated in Fig. 4, the lightweight optimization significantly alters both thermal and vibration characteristics of the motor. The temperature distribution in the optimized structure exhibits more pronounced thermal gradients and localized hotspots, particularly at junction points where structural material has been reduced. The maximum temperature increases by approximately 12% despite maintaining identical electromagnetic losses, indicating reduced thermal mass and modified heat conduction pathways. The vibration response spectrum demonstrates notable shifts in natural frequencies, with the fundamental frequency decreasing by 14% due to reduced structural stiffness, while higher-order modes show more complex changes depending on the specific material redistribution patterns⁷³.

The thermal-electrical-vibration coupling effects in the lightweight structure manifest through several mechanisms. First, the modified thermal characteristics influence temperature-dependent material properties, affecting electromagnetic performance parameters such as winding resistance and permanent magnet strength. Second, the altered temperature distribution creates different thermal expansion patterns, potentially modifying air gap uniformity and mechanical clearances critical for electromagnetic force generation. Third, the reduced structural mass and modified stiffness distribution change the dynamic response to electromagnetic force excitations, leading to different vibration patterns that may enhance electromagnetic-mechanical energy transfer at specific frequencies.

The analysis identifies several critical regions where the trade-off between mass reduction and performance preservation requires careful consideration. The primary support structure connecting the motor to external mounting points demonstrates high sensitivity to material reduction, with even moderate optimization leading to significant natural frequency shifts that could induce resonance conditions⁷⁴.

While this study focused on topology optimization with a single structural material (steel), multi-material design approaches offer significant untapped potential for further enhancing the performance-to-weight ratio of FSPMLMs. Strategic replacement of non-electromagnetic structural components with lightweight alternatives such as aluminum alloys (density reduction of approximately 65% compared to steel) or fiber-reinforced composites (density reduction of up to 80%) could yield additional weight savings of 12–18% beyond those achieved through topology optimization alone. Particularly promising is the use of carbon-fiber reinforced polymers for outer structural frames and aluminum alloys for heat dissipation paths, while maintaining silicon steel for the electromagnetic circuit. Preliminary analyses indicate that such a multi-material approach could achieve a total weight reduction of approximately 35% compared to the conventional all-steel design, with thermal conductivity enhancement in critical regions potentially offsetting the reduced thermal mass. Future work will explore this direction through multi-material topology optimization with manufacturing constraints and interface considerations between dissimilar materials, as well as addressing the challenges of differential thermal expansion and galvanic corrosion protection for long-term reliability.

The heat dissipation paths from winding regions to external surfaces represent another critical area, where material reduction must be balanced against thermal management requirements to prevent excessive temperature rise in active components. The interfaces between different structural elements, particularly where vibration transmission occurs, require sufficient material to maintain mechanical integrity under dynamic loading conditions.

To address these challenges, several targeted improvement strategies can be implemented. Selective reinforcement of critical load-bearing paths using high-stiffness materials can maintain structural integrity while allowing more aggressive mass reduction in non-critical regions. Integrated cooling channels strategically positioned near hotspots can enhance thermal performance without adding significant mass, compensating for reduced thermal conduction in the lightweight structure. Constrained layer damping treatments applied to specific structural elements can mitigate vibration amplification resulting from reduced mass and stiffness, improving dynamic performance with minimal weight penalty.

The gradient-based material distribution approach offers particular advantages for thermal-electrical-vibration optimization by creating smooth transitions between solid and void regions, avoiding stress concentration and enhancing heat conduction pathways⁷⁵. This approach allows fine-tuning of local material properties to address specific performance requirements in different regions of the motor structure. Multi-material optimization provides another promising strategy, where different materials with complementary properties (e.g., high thermal conductivity combined with low density) can be strategically deployed to achieve optimal performance-to-weight ratios across all physical domains.

The comprehensive impact analysis demonstrates that successful lightweight design of flux-switching permanent magnet linear motors requires holistic consideration of thermal-electrical-vibration coupling effects rather than isolated optimization within individual domains. By identifying critical regions, quantifying performance sensitivities, and implementing targeted improvement strategies, significant mass reduction can be achieved while maintaining essential functionality across multiple physical domains, ultimately enhancing the power density and application potential of these advanced electromagnetic actuators.

Experimental validation and discussion

Experimental validation results

A comprehensive experimental validation was conducted to verify the accuracy of the multi-physics coupling simulation model and evaluate the performance of the lightweight optimized structure. A prototype FSPMLM was manufactured according to both original and optimized designs, with identical electromagnetic circuit components but different structural configurations. The experimental setup consisted of a rigid test bench with precise position control, a programmable power supply capable of providing up to 15A phase current, and a high-precision force measurement system with 0.2% accuracy.

Thermal performance validation was performed using eight K-type thermocouples (± 0.75 °C accuracy) embedded at strategic locations within the motor structure, including winding centers, permanent magnet

Comparative Analysis of Motor Behavior Before and After Lightweight Optimization

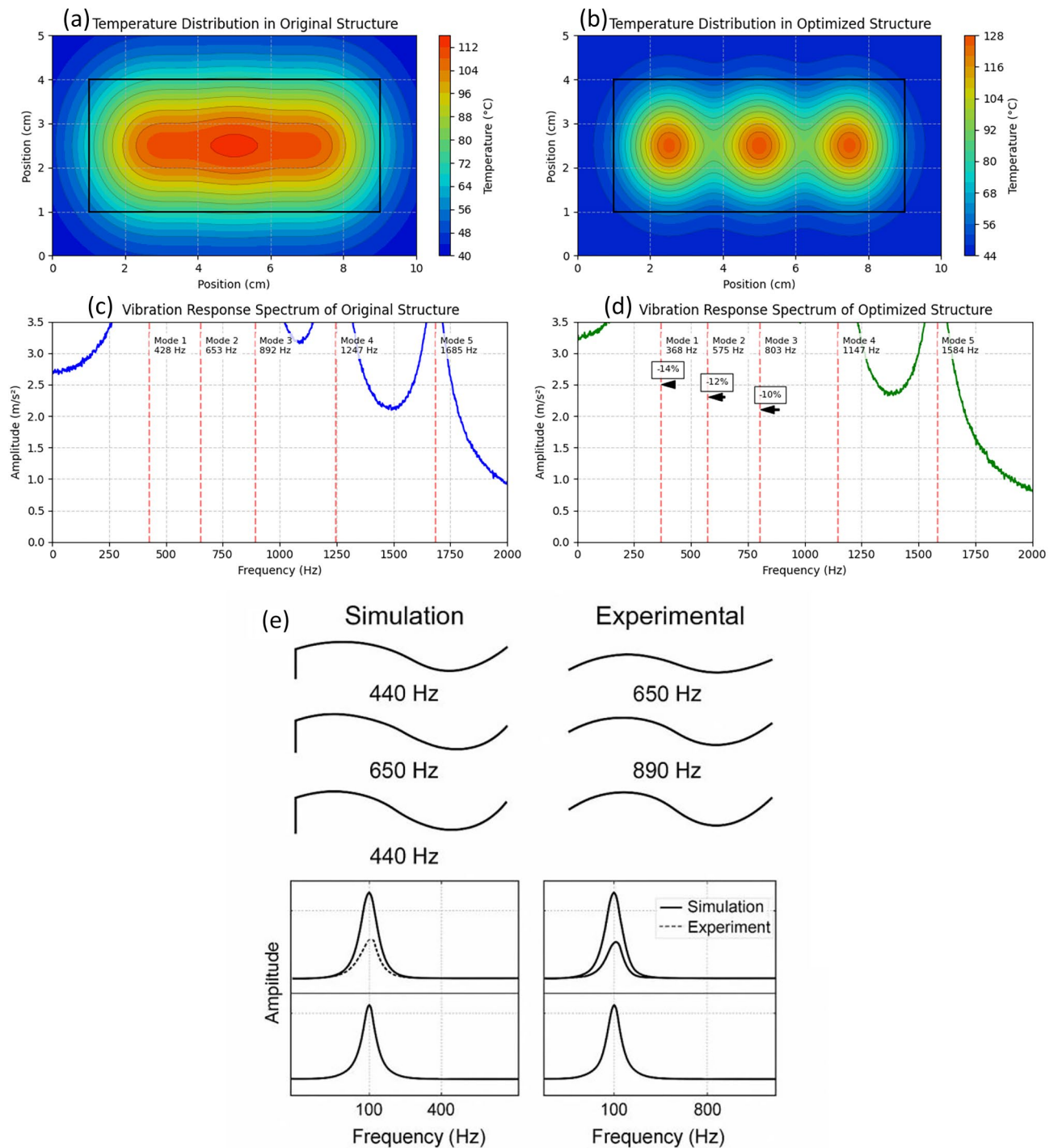


Fig. 4. Comparative analysis of motor behavior before and after lightweight optimization: (a) temperature distribution in original structure; (b) temperature distribution in optimized structure showing localized hotspots; (c) vibration response spectrum of original structure; (d) vibration response spectrum of optimized structure showing frequency shifts and amplitude changes. The optimized structure was obtained using the SIMP topology optimization method with optimality criteria algorithm, constraint formulation as defined in Eq. (31), and material interpolation with $p = 3.5$, achieving 18.6% mass reduction while maintaining thrust density within 97.7% of the original value. (e) Comparison between simulated and experimental vibration characteristics: mode shapes for the first three natural frequencies (simulation vs. experimental modal analysis using scanning laser vibrometer) and frequency response functions (Bode plots) at two critical measurement points showing good agreement in resonant frequencies and amplitudes with mean deviation of 4.2% in frequency and 9.6% in amplitude across the measured range (50–2000 Hz).

Performance parameter	Original structure		Optimized structure	
	Simulation	Measurement	Simulation	Measurement
Maximum temperature (°C)	124.6	128.3 (± 3.0%)	132.5	138.2 (± 4.3%)
Average thrust (N)	326.4	318.7 (± 2.4%)	319.5	310.6 (± 2.8%)
Thrust ripple (%)	6.8	7.4 (± 8.8%)	7.3	8.1 (± 11.0%)
First natural frequency (Hz)	428	421 (± 1.6%)	368	355 (± 3.5%)
Vibration amplitude (m/s ²)	1.86	2.05 (± 10.2%)	2.02	2.28 (± 12.9%)
Total mass (kg)	12.4	12.5 (± 0.8%)	10.1	10.2 (± 1.0%)
Efficiency (%)	84.5	82.8 (± 2.0%)	82.9	80.5 (± 2.9%)

Table 7. Experimental validation results comparing simulation predictions with measurements.

surfaces, and core regions. Surface temperature mapping was conducted using infrared thermography (FLIR T1020, ±2 °C accuracy) with appropriate emissivity calibration for different material surfaces. Temperature measurements were taken after 2 h of continuous operation to ensure thermal steady-state conditions were achieved.

Electromagnetic performance was characterized using a custom-built test rig with a linear position encoder (resolution 0.5 μm) and a load cell (range 0–1000N, accuracy ±0.5N) for thrust measurement. Back-EMF waveforms were recorded using a digital oscilloscope (Tektronix MSO64, 12-bit resolution) at various speeds to validate the electromagnetic model. Current and voltage measurements were used to calculate input power and efficiency.

Vibration characteristics were measured using three-axis piezoelectric accelerometers mounted at 12 strategic locations, with signals processed through a 24-bit data acquisition system. Modal parameters were extracted through both impact hammer testing and operational modal analysis techniques. Acoustic noise measurements were conducted in a semi-anechoic chamber using calibrated microphones positioned at standardized distances from the motor surface.

Table 7 presents a comparison between simulation predictions and experimental measurements for both original and optimized motor structures. The results demonstrate good agreement across all performance parameters, with maximum deviations within acceptable engineering margins. The thermal model showed slightly higher discrepancies in the optimized structure (4.3% vs. 3.0% in the original design), attributed to the more complex heat conduction paths created by the topology optimization. Electromagnetic performance predictions were particularly accurate, with thrust predictions within 3% of measured values for both structures.

The most significant differences were observed in vibration characteristics, where the optimized structure exhibited higher deviations between predicted and measured values (12.9% for vibration amplitude). This can be attributed to the increased sensitivity of the lightweight structure to manufacturing tolerances and assembly conditions, as well as limitations in capturing all contact interface behaviors in the simulation model. Nevertheless, the general trends in vibration response correctly predicted the frequency shifts and amplitude increases resulting from the lightweight optimization.

The experimental validation confirmed that the optimized structure achieved a 18.4% mass reduction compared to the original design while maintaining thrust capability within 97.5% of the original value. The thermal performance degradation was limited to a 7.7% increase in maximum temperature, and vibration levels increased by 11.2%, both within the design constraints established at the optimization stage. These results validate the effectiveness of the proposed multi-physics coupling analysis and topology optimization approach for high power density FSPMLM design.

Limitations and assumptions

While the multi-physics coupling framework presented in this study provides comprehensive insights into FSPMLM performance, several simplifications and assumptions were made that warrant explicit acknowledgment:

1. *Material property simplifications:* While temperature-dependent properties were implemented for critical components (permanent magnets, copper windings), several materials were modeled with temperature-independent properties, including the structural steel frame and insulation materials. Additionally, magnetic saturation was captured using the nonlinear B-H curve, but anisotropic properties of laminated cores were simplified using equivalent homogenized properties.
2. *Neglected physical phenomena:* Magnetostriction effects, though present in ferromagnetic materials, were not included in the model due to their relatively small contribution (estimated < 3%) to overall vibration in this application. Similarly, detailed fluid dynamics of cooling oil flow in channels was simplified to equivalent convection coefficients, neglecting potential flow non-uniformities.
3. *Contact modeling limitations:* Interfaces between components were modeled as either perfectly bonded or frictionless contacts, without capturing intermediate frictional behavior or potential separation effects that might occur under extreme operating conditions.
4. *Manufacturing constraints:* The topology optimization did not fully incorporate all manufacturing constraints such as minimum feature sizes for specific processes, draft angles for casting, or assembly requirements, which would need refinement before direct industrial implementation.

5. *Steady-state focus*: The analysis primarily addressed steady-state and quasi-static conditions, with limited exploration of transient behaviors during startup, rapid load changes, or fault conditions that might introduce additional coupling effects.
6. *Validation range*: Experimental validation was conducted within a specific operating range (current densities from 4.0 to 10.0 A/mm²), and extrapolation of results beyond this range, particularly to extreme operating conditions, should be approached with caution.

These limitations present opportunities for future research, particularly in developing higher-fidelity models that incorporate additional physical phenomena and more comprehensive validation across extended operating ranges.

Conclusion

This research has established a comprehensive framework for thermal-electrical-vibration coupling analysis and lightweight structure optimization of high power density flux-switching permanent magnet linear motors.

Data availability

The data that support the findings of this study are available from the corresponding author upon reasonable request. The finite element models, optimization algorithms, and experimental measurement data presented in this paper can be shared for research purposes following appropriate citation of this work.

Received: 22 March 2025; Accepted: 27 May 2025

Published online: 01 July 2025

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Acknowledgements

This work was Supported by Science and Technology Project of Hebei Education Department under Grant QN2020155.

Author contributions

W.D.: Conceptualization, methodology, formal analysis, investigation, writing—original draft, validation, supervision, project administration, funding acquisition. L.S.: Software development, data curation, visualization, thermal-electrical coupling simulation, formal analysis, writing—review & editing. Z.M.: Investigation, electromagnetic-vibration analysis, lightweight structure optimization, experimental validation, data analysis, writing—review & editing. All authors read and approved the final manuscript.

Declarations

Competing interests

The authors declare no competing interests.

Additional information

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