

Article

Data-Driven Optimization of Electromagnetic–Mechanical Coupling of Permanent Magnet Topology for Vibration Suppression in PMDC Motors

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Abstract

This study proposes a vibration reduction strategy for a 12-slot, two-pole permanent magnet brushed DC (PMDC) motor used in automotive blower systems. A multi-parameter optimization framework combining finite element analysis and experimental validation is developed to address cogging torque, a critical source of electromagnetic vibration and acoustic noise. The influence of pole arc coefficient and permanent magnet eccentricity on cogging torque is systematically investigated using response surface methodology, identifying an optimal design with significantly reduced torque ripple and vibration. Furthermore, a machine learning model based on the random forest algorithm is introduced to predict cogging torque, air gap magnetic flux density, and output torque, achieving high accuracy and strong generalizability. The results confirm that the optimized motor structure suppresses resonance-induced noise near 7500 Hz, improving overall motor stability and acoustic performance. The proposed data-driven design approach offers a reliable and efficient pathway for vibration optimization in low-cost automotive PMDC motors.

Keywords: permanent magnet DC motor; cogging torque optimization; vibration reduction; response surface methodology; random forest prediction

1. Introduction

With the rapid growth of the electric vehicle industry, consumer expectations in terms of ride comfort have risen significantly. Unlike traditional fuel-powered vehicles, electric vehicles eliminate engine noise but, in turn, make secondary sound sources more prominent, such as high-frequency vibrations from the drive motor, road and tire noise, air conditioning system noise, and aerodynamic noise [1]. Especially at low vehicle speeds or when the vehicle is stationary, the blower motor, serving as the core actuator of the air conditioning system, becomes a primary source of non-traditional cabin noise during in-vehicle air circulation and temperature regulation. Based on the noise generation mechanisms of electromechanical systems, the audible noise produced by motors can be primarily classified into three categories: mechanical noise caused by structural vibrations (e.g., bearing defects, rotor imbalance), electromagnetic noise arising from harmonic electromagnetic forces and current distortion, and aerodynamic noise induced by airflow



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turbulence (e.g., fan rotation, air duct interactions) [2,3]. Currently, permanent magnet brushed DC motors remain the primary choice for automotive blowers due to their low cost and reliable performance. However, the long-term periodic contact between the brushes and the commutator inevitably introduces vibration excitation into the system. Therefore, the vibration characteristics of permanent magnet brushed DC motors warrant further investigation [4].

For permanent magnet motors, the inherent cogging effect, as a typical nonlinear disturbance source of electromagnetic–mechanical coupling, significantly restricts their application in various scenarios [5,6]. Cogging torque arises from the periodic interaction between the permanent magnet magnetic field and the stator slot-modulated magnetic resistance. Essentially, it is an alternating torque generated by the coupling of air gap permeance harmonics and permanent magnet mmf (magnetomotive force) harmonics [7]. This torque ripple causes mechanical vibration and harmonic noise of the motor, thereby affecting the control accuracy of the motor. Meanwhile, this vibration energy is transmitted to the air medium through sound pressure fluctuations, generating audible noise. Therefore, the spectral characteristics of noise directly depend on the spatial distribution of radial force waves and the structural characteristics of vibration transmission paths [3,8]. Thus, optimizing the motor structure to reduce the amplitude of cogging torque can effectively reduce noise radiation intensity or improve its acoustic quality [9].

The increase in the greatest common divisor of slot and pole numbers can suppress electromagnetic vibration by adjusting slot–pole combinations, while such adjustments may be accompanied by negative effects such as increased cogging torque and reduced power density [10]. In ref. [11], the radial electromagnetic force waves of six types of permanent magnet motor are systematically investigated to study the influence of different slot–pole parameters on the NVH (noise, vibration, and harshness) characteristics of a motor. For stator skewed slot technology, Han et al. [12] revealed the evolution law of magnetic flux-electromagnetic force distribution characteristics through comparisons of multiple skewed slot stator structures, converted deformation displacement into sound pressure level parameters to quantify noise spectral characteristics, and compared noise performance differences under different stator skewed slot coefficients [13]. The auxiliary slots are opened on stator teeth and rotors, which can significantly improve the vibration performance of motors [14]. Xing et al. [15] designed auxiliary slots on the stators of permanent magnet motors with four different slot–pole combinations and explored the influence of the number of auxiliary slots on the weakening of electromagnetic force waves. Permanent magnet motors with auxiliary slots can reduce low-order radial electromagnetic force waves, suggesting the quantitative relationship between the number of slots (k) and the order of low-order harmonics ($j(k + 1)$), and demonstrating that this structure can selectively weaken radial electromagnetic force waves of specific orders. Vicente Simón-Sempere et al. presented a novel method for cogging torque reduction in axial flux permanent-magnet motors, which is based on optimizing the edge shape of permanent magnets [16]. Deng et al. took the generation mechanism of electromagnetic force harmonics as the starting point. Based on the electromagnetic force modulation effect, they systematically analyzed the influences of slot–pole combination, winding configuration, electromagnetic topology, and drive control strategy on electromagnetic force, as well as electromagnetic vibration and noise, and summarized the corresponding targeted suppression strategies [17,18]. To sum up, research on suppressing motor vibration mostly focuses on the optimization of radial electromagnetic force waves, weakening electromagnetic force harmonics by adjusting pole–slot matching or skewing magnetic poles, thereby reducing vibration and noise. However, systematic research on the cogging torque of motors with permanent magnets is still insufficient. As an inherent characteristic of permanent magnet motors, cogging torque

originates from the periodic interaction between stator slotting and permanent magnet magnetic fields. The torque ripple caused by it not only accelerates the wear of commutator brushes but also amplifies through the mechanical transmission chain, causing broadband electromagnetic noise [19].

In this work, a 12-slot two-pole permanent magnet brushed DC (PMDC) motor, for use in automotive blower drives, is proposed, using a multi-parameter collaborative optimization strategy, and systematically studies the modulation law of pole arc coefficient and eccentricity on cogging torque (T_c). Through the combination of finite element simulation and prototype experiments, the modulation mechanism of magnetic pole structural parameters on cogging torque is revealed. To improve the vibration performance of the designed PMDC motor, the cogging torque is optimized by the response surface method. We further present a random forest method, which can accelerate learning to optimize cogging torque, and the results are highly consistent with the response surface. The overall research process of this study is illustrated in Figure 1.

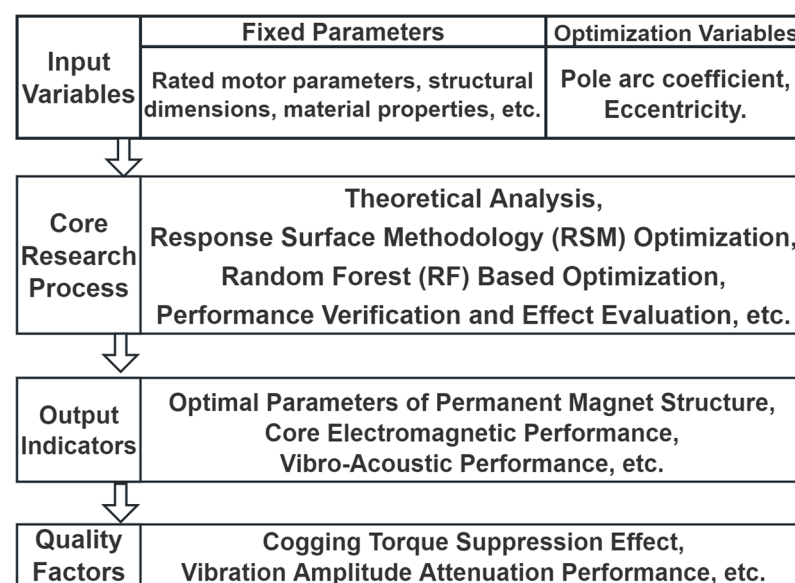


Figure 1. The flow chart of the overall research process.

2. Theoretical Analysis of Calculations

2.1. Theoretical Analysis of Air Gap Flux Density and Radial Electromagnetic Force

For any relative position β , the distribution of the radial component of the air gap flux of the motor along the armature surface can be expressed as follows [20]:

$$B(\theta, \beta) = B_r(\theta) \frac{e_m(\theta)}{e_m(\theta) + \delta(\theta, \beta)} = B_r(\theta) \frac{e_m(\theta)}{e_m} \frac{e_m}{e_m(\theta) + \delta(\theta, \beta)} \quad (1)$$

where e_m denotes the length in the magnetization direction at the center of the permanent magnet pole, which is a fixed design parameter in this work; $e_m(\theta)$ represents the length in the magnetization direction of the permanent magnet at the position with an angle θ from the central axis of the permanent magnet. This value varies with the eccentricity of the permanent magnet, and is the core physical variable optimized in this study; $\delta(\theta, \beta)$ refers to the actual air gap length at the position of angle θ when the relative position between the stator and rotor is β , whose value changes periodically with the circumferential distribution of the stator slots and teeth. Then, the magnetic field energy inside the motor is as follows [21]:

$$W = \frac{1}{2\mu_0} \int_V \left[B_r(\theta) \frac{e_m(\theta)}{e_m} \right]^2 \left[\frac{e_m(\theta)}{e_m(\theta) + \delta(\theta, \beta)} \right]^2 dV = \frac{1}{2\mu_0} \int_V B_r^2(\theta) \left[\frac{e_m(\theta)}{e_m(\theta) + \delta(\theta, \beta)} \right]^2 dV, \quad (2)$$

where μ_0 represents the permeability of vacuum, $\mu_0 = 4\pi \times 10^{-7}$ H/m.

Radial electromagnetic force can cause electromagnetic vibration and noise in the motor. Based on Maxwell's principle, the expression of radial electromagnetic force density is derived as follows [22]:

$$p_r(\theta, t) = \frac{B_r^2(\theta, t) - B_t^2(\theta, t)}{2\mu_0} \approx \frac{B_r^2(\theta, t)}{2\mu_0}, \quad (3)$$

where B_r represents the radial air gap flux density; B_t is the tangential air gap flux density (usually $B_t \ll B_r$, so that B_t can be neglected).

2.2. Theoretical Analysis of Cogging Torque

Cogging torque, as an inherent characteristic of permanent magnet motors, specifically refers to the periodic torque generated by the interaction between the permanent magnet magnetic field and the armature slot tooth structure when the windings are de-energized. The physical essence stems from the no-load torque ripple caused by the change in magnetic resistance between the stator and rotor. Especially under low-load or deep flux-weakening operating conditions, this torque ripple will be amplified through the mechanical transmission chain, significantly increasing the vibration and noise level of the system [23–26]. By means such as optimizing the pole shape or adopting a skewed slot design to suppress the cogging torque, the stability of the torque output of the motor in precision control scenarios can be effectively improved. The cogging torque is usually defined as the negative derivative of the magnetic field energy W when the motor is not energized with respect to the relative position angle α of the stator and rotor, that is [21]:

$$T_{cog} = -\frac{\partial W}{\partial \alpha}, \quad (4)$$

where α refers to the relative position angle between the stator and the rotor, representing the angle between the center line of a certain armature tooth and the center line of a certain permanent magnet. Fourier expansion is performed on $B_r^2(\theta)$ and $[h_m/(h_m(\theta) + \delta(\theta, \beta))]^2$ as given by Equation (2), from which Equations (5) and (6) can be derived.

$$B_r^2(\theta) = B_{r0} + \sum_{n=1}^{\infty} B_{rn} \cos 2np\theta, \quad (5)$$

where $B_{r0} = \alpha_p B_r^2$, $B_{rn} = 2/n\pi(B_r^2 \sin n\alpha_p \pi)$, α_p is the pole arc coefficient. The pole arc coefficient is calculated by the formula $\alpha_p = \tau_p/\tau$, where τ_p is the arc length of the permanent magnet pole along the circumferential direction of the rotor (i.e., the pole arc width), and τ is the pole pitch of the motor, which refers to the circumferential length corresponding to each pair of poles. The calculation formula of the pole pitch is: $\tau = \pi D_{ro}/2p$, where D_{ro} is the outer diameter of the rotor; p is the pole pair number of the motor, which is 1 in this work.

$$\left[\frac{h_m(\theta)}{h_m(\theta) + \delta(\theta)} \right]^2 = G_0 + \sum_{n=1}^{\infty} G_n \cos nz\theta \quad (6)$$

After the Fourier expansion, the cogging torque expression of the selected straight slot armature in the study is obtained [20]:

$$T_{cog}(\alpha) = \frac{\pi z L_a}{4\mu_0} (R_2^2 - R_1^2) \sum_{n=1}^{\infty} n G_n B_r \frac{nz}{2p} \sin nz\alpha \quad (7)$$

where R_1 represents the outer radius of the armature; R_2 is the inner radius of the stator yoke; and n is an integer that makes $nz/2p$ an integer.

3. Motor Design and Finite Element Analysis

3.1. Main Parameter Design

The design of an automotive blower drive motor must take into account a wide range of parameters related to its intended application, including the structural dimensions of each motor component and other performance-affecting variables. At the same time, key indicators such as air gap flux density, magnetic flux density, and cogging torque must be thoroughly evaluated [27,28]. In this work, the motor is designed as a permanent magnet brushed DC motor with 2 poles and 12 slots. The 12-slot two-pole PMDC motor adopts a double-layer lap integral-slot winding configuration; its armature winding features a short pitch with a coil span of five slots, and two coil sides are placed in each rotor slot. The motor is equipped with 12 commutator segments, which conforms to the structural characteristics of integral-slot winding. The permanent magnets mounted on the stator shell are composed of two symmetrically arranged tile-shaped pieces, ensuring no magnetic pole offset. The rotor features 12 slots, and there is no eccentricity between the stator and rotor. With ongoing advancements in permanent magnet DC motor design and increasing market competition, reducing manufacturing costs has become a key consideration. To this end, we selected 50AW800 electrical steel and ferrite as the primary materials for the motor. The saturation flux density of 50AW800 is 2.0 T. At 60 °C, the ferrite exhibits a coercivity of 281.5 kA/m and a remanence of 0.3746 T. The maximum electromagnetic torque under continuous load is positively correlated with the motor size. Taking installation feasibility into account, the outer diameter of the motor housing is set to 65 mm, the outer rotor diameter is 47 mm, the rotor's inner diameter is 8 mm, and the rotor length is 31.5 mm. Accordingly, the main design parameters of the motor are determined based on both design requirements and empirical formulas, as summarized in Table 1.

Table 1. Parameters of the proposed PMDC motor in an electric vehicle cooling fan.

Parameters	Value	Parameters	Value
Rated Power	180 W	Pole–Slot Number	2/12
Rated Voltage	12 V	Rotor Outer Diameter	47 mm
Rated Speed	1300 rpm	Rotor Inner Diameter	8 mm
Steel Material	50AW800 (BAOSTEEL)	Housing Outer Diameter	65 mm
Magnet Material	Ferrite (MASTEEL)	Magnet Arc Total Length	40 mm

3.2. Finite Element Analysis of the Motor

Based on the previously designed motor structure and key parameters, a two-dimensional (2D) model was constructed using the ANSYS Maxwell 2020R2 platform, as shown in Figure 2a. A no-load simulation was first performed to evaluate the effectiveness of the design. Figure 2b,c illustrate the flux density and magnetic field lines under no-load conditions. The flux density in the rotor yoke and teeth ranges from 0.4 to 1.7 T, with no signs of magnetic saturation or risk of demagnetization. The flux density distribution exhibits periodic symmetry, having minimal impact on motor performance and satisfying the design requirements for magnetic loading. However, in terms of color mapping, there is a noticeable imbalance in the transition between regions, with localized concentrations of magnetic flux density. From a structural standpoint, the magnetic field

lines are symmetrically distributed and follow regular paths between the stator and rotor. Most lines enter the rotor unobstructed, indicating a relatively optimal magnetic flux distribution. Overall, the flux density and magnetic field lines under no-load conditions conform to the design expectations [29,30].

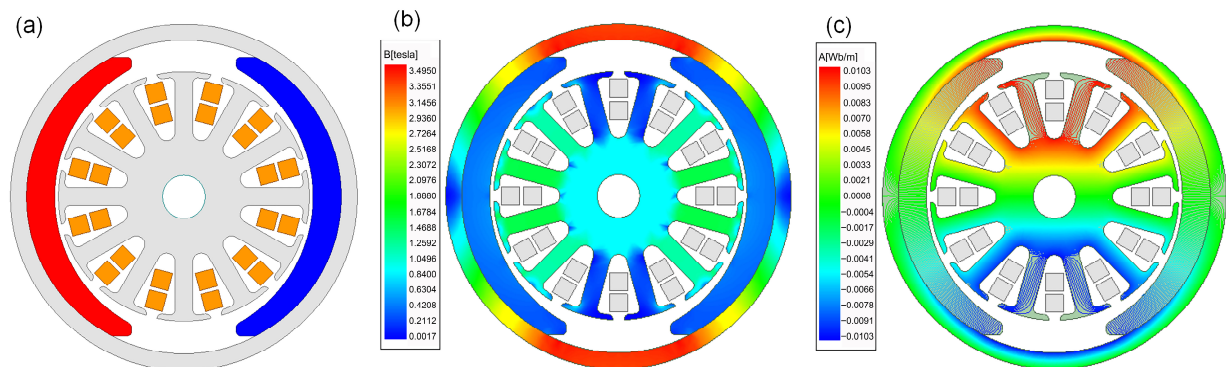


Figure 2. (a) Cross-sectional view of the studied PMDC motor structure; (b) magnetic flux density; and (c) magnetic field lines of the proposed motor model under no-load conditions.

In the 2D finite element model of the motor, the circumferential region of the air gap near the stator teeth is selected to extract three-dimensional diagrams of the air gap flux density and radial electromagnetic force wave across the full mechanical angle of the motor, plotted as functions of both time and space, as shown in Figure 3a,b. The spatiotemporal distribution of the air gap flux density exhibits a typical periodic and symmetric structure, with values ranging from 0 to 0.6 T. The waveform approximates a trapezoidal shape, indicating relatively high magnetic efficiency. The radial electromagnetic force wave demonstrates periodic oscillations in the space-time domain, reaching a peak magnitude of 0.6 N/mm². However, the pronounced peak fluctuations may reduce the motor's capacity to suppress vibration and noise. Under no-load conditions, the cogging torque was simulated with a time step of 0.02 s, and the result is illustrated in Figure 3c. The torque shows regular pulsations during operation, with an amplitude of 0.284 N·m (mean value approximately zero) and a peak of 0.568 N·m. Although these relatively smooth waveforms support stable motor operation, further optimization is still needed to improve vibration and noise suppression.

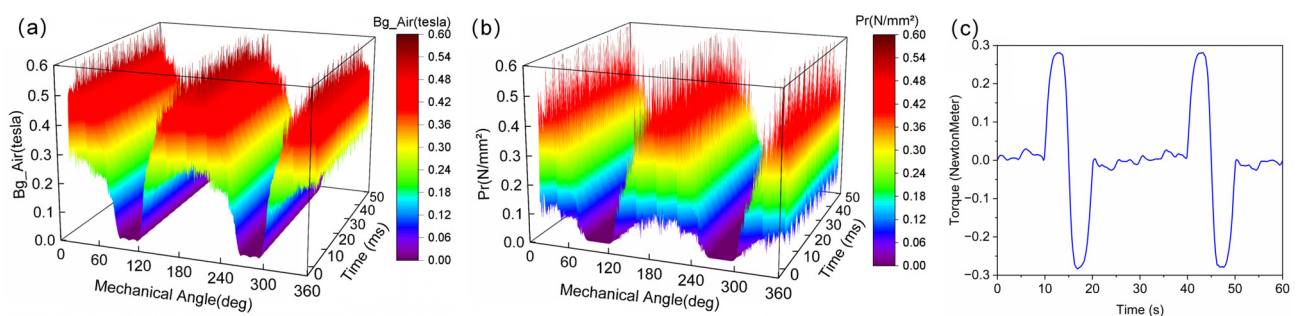


Figure 3. (a) Time–space evolution of the air gap magnetic flux density; (b) radial electromagnetic force density distribution; and (c) the computed cogging torque waveform.

3.3. Motor Vibration Simulation

Using various modules within the ANSYS Workbench 2020R2 platform, a three-dimensional (3D) model was constructed based on the design parameters of the motor and its casing, as shown in Figure 4a. The mesh for the commutator region was locally refined, and the electromagnetic force acting on the rotor teeth, obtained from the previ-

ously described 2D electromagnetic model, was imported into the 3D structural model of the motor for vibration simulation. The vibration acceleration on the surface of the motor commutator was then calculated, with the results presented in Figure 4b. Significant peaks in vibration acceleration are observed in the frequency ranges of 3200–3600 Hz and around 8000 Hz (specifically between 7400–7600 Hz). The rated speed of the PMDC motor in this work is $n = 1300$ rpm with a pole pair number $p = 1$. According to the formula $f_0 = 60 np$, the fundamental mechanical frequency of the rotor rotation is calculated as $f_0 \approx 21.67$ Hz. For the 12-slot two-pole PMDC motor studied herein, the fundamental order of the cogging torque is equal to the least common multiple (LCM) of the stator slot number and the rotor pole number, i.e., $\text{LCM}(12, 2) = 12$. Accordingly, the fundamental frequency of the cogging torque is derived as $f_c = 12 \times f_0 \approx 260$ Hz. The frequency band with prominent vibration peaks observed in the simulation (7400–7600 Hz) corresponds to the 29th harmonic of the fundamental frequency of the cogging torque, with a theoretical frequency of $29 \times 260 = 7540$ Hz. These frequency bands should therefore be given particular attention during subsequent prototype vibration and noise testing.

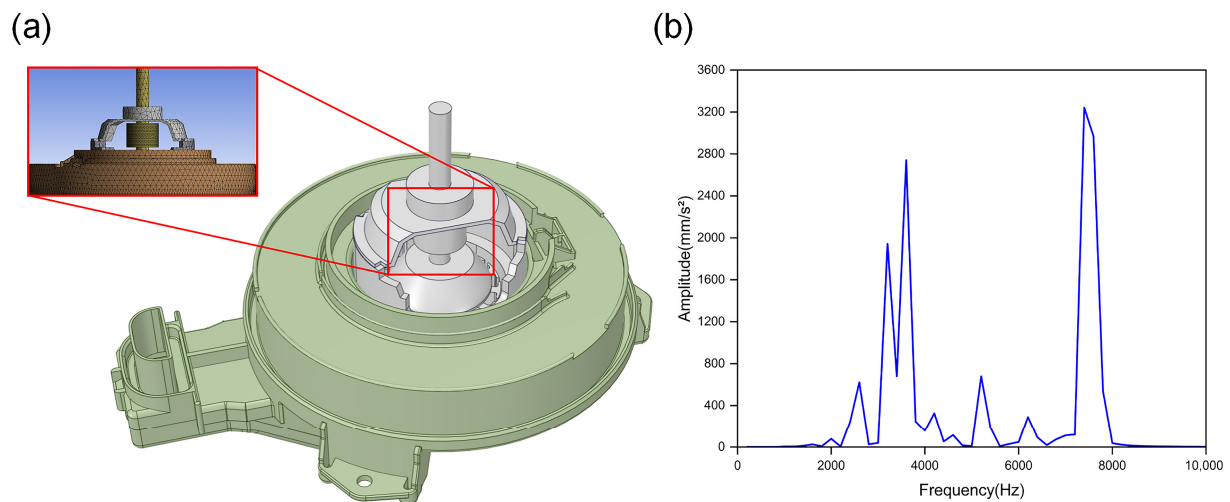


Figure 4. (a) The motor commutator and (b) the simulated frequency spectrum analysis under no-load conditions.

4. Vibration Testing of PMDC Motor Prototype

To validate the accuracy of the simulation results, a full-scale (1:1) physical prototype was fabricated based on the motor design parameters (as shown in Figure 5a,b, red frame). An acoustic testing environment compliant with ISO 3745 standards was established in a semi-anechoic chamber. A dual-point synchronous acquisition method was employed during the experiment: two PCB HT378A21 ICP microphones (PCB Piezotronics, New York, NY, USA) were horizontally mounted on the axial plane of the motor, positioned 10 cm radially from the positive brush and 5 cm from the negative brush, respectively. The axes of the two microphones were carefully aligned to be collinear and perpendicular to the rotational center of the motor, as illustrated in Figure 5c. During the test, the prototype operated stably at a speed of 1300 rpm.

Acoustic signals were captured using high-precision sound pressure sensors and analyzed in the time-frequency domain with Head Acoustics front-end equipment and ArtemiS SUITE platform. The original frequency spectrum is presented in Figure 6a. The 0–5000 Hz frequency range displays a continuous and uniform distribution, with sound pressure levels (SPL) consistently between 30 and 40 dB, which is attributed to environmental background noise and is not directly associated with structural vibrations. To accurately extract the whistling characteristics, a filter bank was employed to isolate

the energy within the target frequency range (4000–10,000 Hz), as shown in Figure 6b. Time-frequency analysis reveals prominent broadband whistling during no-load motor operation. A continuous energy cloud is observed in the 7000–8000 Hz range, with SPL amplitudes ranging from 61.69 to 65.40 dB, persisting throughout the entire operational cycle. Notably, a stable narrowband peak is identified at 7500 Hz, with a bandwidth of less than 50 Hz.

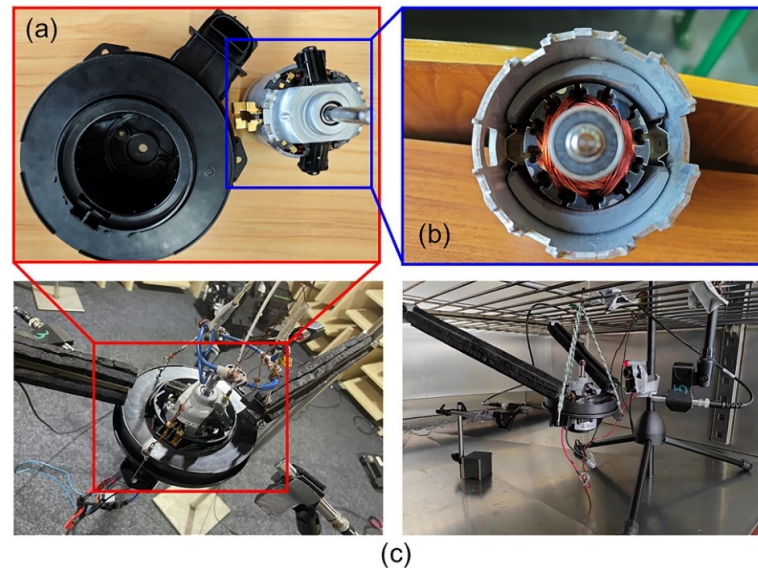


Figure 5. (a) The prototype consisting of the flange (left) and the motor (right, blue frame); (b) the internal structure of the motor; (c) the noise and vibration testing setup in a semi-anechoic chamber.

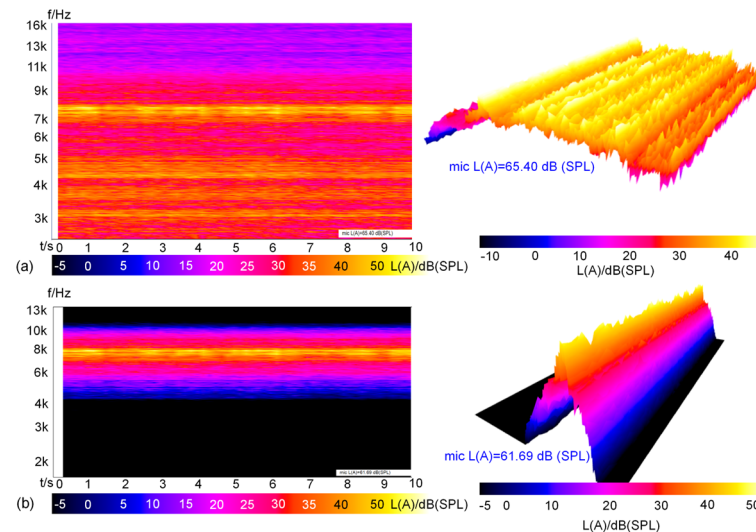


Figure 6. Motor noise test results: (a) full-spectrum spectrogram and (b) enlarged view of the spectrum near 7500 Hz.

High-frequency electromagnetic excitation forces induce structural resonance in motor components, which, through surface vibrations, radiate noise into the air in the form of an audible whistle. In a blind listening test conducted at a distance of 1 m, all six participants confirmed the perception of an abnormal “squeaking” sound near the prototype’s characteristic frequency of 7500 Hz. The subjective auditory descriptions aligned precisely with the narrowband peak observed in the noise spectrum. This phenomenon is consistent with the pronounced vibration acceleration peaks in the 7400–7600 Hz range identified in previous vibration simulations, demonstrating the accuracy of our calculation results compared to

experiment. The results confirm that the motor undergoes structural resonance induced by electromagnetic excitation at 7500 Hz, which leads to audible noise radiation. This verifies the transmission path from structural vibration to airborne sound and elucidates the full “electromagnetic–mechanical–acoustic” coupling chain. Consequently, the noise generated by the motor is primarily due to pronounced vibrations around 7500 Hz. Reducing the amplitude of cogging torque through structural optimization can effectively suppress both vibration and noise [31,32].

5. Improving Vibration Characteristics Based on Cogging Torque Optimization

5.1. Response Surface Methodology Optimization

The selection of the pole arc coefficient (α_p) affects the magnetic flux density distribution and the saturation level of the magnetic circuit, thereby influencing the core length, the dimensions of the stator and rotor laminations, and the air gap of the motor. In practical applications, the choice of α_p is constrained by various factors and should be determined through a comprehensive consideration of permanent magnet utilization, cogging torque reduction, and overall motor performance [33,34].

First, parametric simulations were performed by varying the pole arc coefficient from 0.60 to 0.90, while keeping all other structural parameters constant. The resulting cogging torque amplitudes are shown in Figure 7a. Within this range, the cogging torque amplitude first decreases and then increases, reaching a minimum value of approximately 0.283 N·m at a pole arc coefficient of around 0.775. Deviating from this optimal point, either decreasing to 0.60 or increasing to 0.90, leads to a notable rise in cogging torque amplitude, returning to about 0.35 N·m. This trend clearly underscores the importance of optimizing the pole arc coefficient to effectively suppress cogging torque. To facilitate integrated optimization alongside eccentricity parameters while preserving motor performance, the pole arc coefficient range for subsequent analyses is set between 0.70 and 0.85.

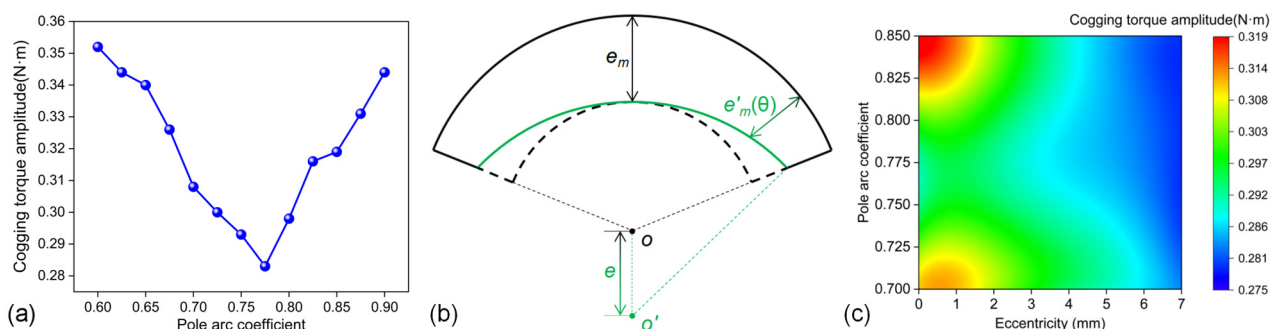


Figure 7. (a) Cogging torque corresponding to different pole arc coefficients; (b) schematic diagram illustrating magnet eccentricity and uneven thickness; (c) response surface of cogging torque as a function of eccentricity and pole arc coefficient.

The air gap magnetic field of a permanent magnet motor varies with the thickness of the magnetic pole, which in turn influences the cogging torque. As shown in Figure 7b, O denotes the center of the outer arc of the permanent magnet, O' represents the center of the inner arc of the permanent magnet, and e indicates the eccentricity, which is defined as the linear distance between the outer arc center (O) and the inner arc center (O') of the permanent magnet. The minimum air gap length, e_m , typically remains constant. When the inner and outer centers of the permanent magnet do not coincide, which means the magnet

is eccentric, the radial component of the air gap flux density in the motor can be expressed as follows [20]:

$$B'(\theta) = B_r'(\theta) \frac{e_m}{e_m + \delta(\theta)}, \quad (8)$$

where $B_r'(\theta)$ represents the varying thickness of the permanent magnet, which can be obtained as $B_r'(\theta) = (e_m'(\theta) \times B_r(\theta)) / e_m$, $e_m'(\theta)$. $\delta(\theta)$ is the varying air gap thickness. After rearrangement, the relationship between eccentricity and cogging torque can be expressed as Equation (9) [20], and the magnitude of cogging torque can be optimized by changing the thickness of the permanent magnet.

$$T_{cog}(\beta) = \frac{\pi z L_a}{4\mu_0} (R_2^2 - R_1^2) \sum_{n=1}^{\infty} n G_n B_r(\theta) \frac{e_m'(\theta)}{e_m(\theta) + \delta(\theta)} \sin n z \beta \quad (9)$$

While keeping other structural parameters constant, the pole arc coefficient (α_p) was varied from 0.700 to 0.850 in increments of 0.025, and the eccentric distance (e) was varied from 0 mm to 7 mm in increments of 0.5 mm. This parameter selection resulted in 105 distinct parameter combinations. Finite element simulations were performed for each parameter set to obtain the corresponding cogging torque amplitude, thereby forming a complete sample set comprising 105 data points. This approach ensured high-density coverage of the parameter space and guaranteed the reliability of the model data.

Response surface methodology (RSM) is a statistical and mathematical optimization method widely applied to the design and performance optimization of multi-variable nonlinear systems. It constructs a quantitative polynomial regression model between input design variables and output response values through rational experimental design, to realize the accurate prediction of system response and the solution of optimal parameter combinations. Compared with the traditional single-factor variable method, RSM can effectively characterize the coupling effect between multiple design variables, reduce the number of required simulation tests, and significantly improve the efficiency of multi-parameter collaborative optimization, which is highly suitable for the electromagnetic performance optimization scenario of the permanent magnet brushed DC (PMDC) motor in this work.

In this study, the central composite design (CCD), the most classic and widely used experimental design method in RSM, was adopted to formulate the optimization experimental scheme and complete sample data collection. CCD is suitable for the optimization of continuous variables, and can accurately fit the nonlinear relationship between design variables and response values with high experimental efficiency and low computational cost [35]. The Design-Expert 13 software was utilized for model fitting and analysis. The pole arc coefficient (α_p) and eccentric distance (e) were defined as numerical input factors, with the cogging torque amplitude (T_c) serving as the response variable. Regression analysis was performed using the CCD methodology, which is suitable for constructing second-order RSM models.

It can be observed that for a fixed eccentricity ($e = 3$ mm), the cogging torque amplitude decreases sharply from 0.297 N·m to 0.289 N·m as the pole arc coefficient is reduced from 0.85 to 0.775. However, with a further decrease to 0.70, the torque amplitude gradually increases to 0.301 N·m, exhibiting a nonlinear concave behavior. Conversely, for a fixed pole arc coefficient ($\alpha_p = 0.80$), the cogging torque amplitude decreases with increasing eccentricity, dropping from 0.300 N·m to 0.276 N·m, indicating a negative correlation. Although a slight increase in cogging torque amplitude is observed at certain pole arc coefficients during the initial rise in eccentricity, the overall trend reveals an approximately linear negative relationship between cogging torque and eccentricity.

The optimal parameter combinations for minimizing cogging torque are located in the deep blue core region at the lower right corner of Figure 7c. Three representative combinations of pole arc coefficient and eccentricity, (0.825, 6 mm), (0.8, 6.5 mm), and (0.75, 6.5 mm), yield similarly minimal cogging torque amplitudes of approximately 0.277 N·m, with corresponding peak-to-peak values of 0.548 N·m, 0.547 N·m, and 0.549 N·m, respectively. The output torque of the PMDC motor under rated load conditions for these three configurations is shown in Figure 8a. Their torque ripple values are 21.02, 21.23, and 21.61, respectively. Based on this analysis, the combination of a pole arc coefficient of 0.825 and an eccentricity of 6 mm is identified as the optimal design. Furthermore, compared to the original PMDC motor, the peak-to-peak cogging torque in the optimized motor is reduced by 0.02 N·m (from 0.568 N·m to 0.548 N·m), and the output torque ripple is significantly lowered (from 21.98 to 21.02). These results demonstrate that the joint optimization of pole arc coefficient and eccentricity effectively suppresses cogging torque while enhancing load stability. Then, the vibration acceleration on the surface of the motor commutator is also calculated by an optimized PMDC motor, as shown in Figure 8b. Compared to the original PMDC motor structure, all three optimization schemes maintain vibration frequencies in the range of 7400–7600 Hz. However, under the optimization scheme with $\alpha_p = 0.825$ and $e = 6$ mm, the vibration amplitude is significantly reduced, indicating an effective vibration suppression across the entire motor system.

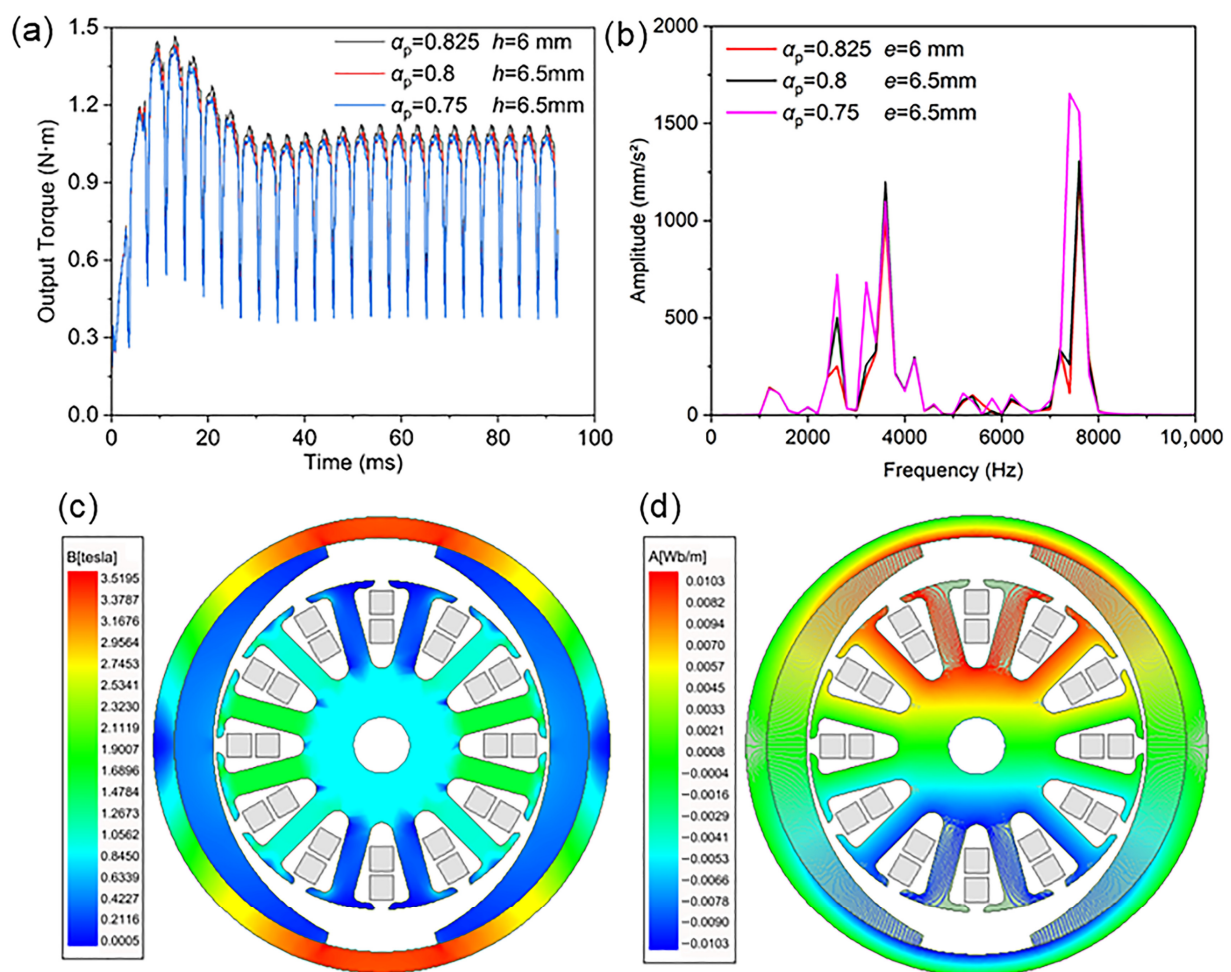


Figure 8. (a) Output torque of the three optimized configurations under rated load conditions; (b) calculated frequency spectra of the three optimized configurations under no-load conditions; (c) magnetic flux density and (d) magnetic field lines of the optimized motor with $\alpha_p = 0.825$ and $e = 6$ mm under no-load conditions.

To verify whether the optimized motor structure ($\alpha_p = 0.825$ and $e = 6$ mm) meets the design requirements, a 2D model was developed using the ANSYS Maxwell 2020R2 platform to analyze the magnetic flux density and magnetic field lines, as shown in Figure 8c,d, respectively. In the optimized model, adjustments to the pole arc coefficient and the eccentricity of the permanent magnet result in a better alignment between the magnet and the stator slots, improving both positional and geometric compatibility. This provides a more optimal baseline configuration for the overall electromagnetic structure of the PMDC motor [36]. Compared to the flux density distribution of the original structure (Figure 2b), the optimized motor exhibits a markedly more uniform and continuous color gradient, with smooth transitions between regions of high and low flux density. This suggests that magnetic flux is no longer locally concentrated as in the original design, but instead more evenly distributed throughout the magnetic circuit. Such an improvement can significantly reduce magnetic losses: a more uniform flux distribution leads to a more stable and predictable energy dissipation path, thereby enhancing the efficiency of magnetic energy utilization. During motor operation, this stable flux distribution helps maintain consistent magnetic energy conversion, minimizing additional losses or performance degradation caused by localized flux fluctuations [37]. From the optimized magnetic field diagram (Figure 8d), it is clear that the magnetic field lines are symmetrically and optimally distributed, closely resembling those in the original design (Figure 2c), and flow smoothly into the rotor. The color gradients and line patterns suggest that there are virtually no stray edge magnetic lines that could cause magnetic leakage. From the perspective of electromagnetic performance, these optimizations significantly enhance the PMDC motor's efficiency and contribute to stable operation under various working conditions, thereby improving the overall reliability and effectiveness of the motor [38,39].

5.2. Machine Learning Optimization Method

The random forest (RF) algorithm is an ensemble machine learning method based on multiple decision trees, in which predictions are obtained by averaging the outputs of trees generated through bootstrap sampling and random feature selection. In this study, the dataset generated from the RSM analysis is used to train the RF model, enabling efficient prediction and optimization of motor vibration characteristics based on electromagnetic-structural parameters by MATLAB R2024a. Building on previous research, we further propose a random forest-based prediction method to estimate the cogging torque, air gap magnetic flux density, and output torque of the investigated PMDC motor. First, a dataset is compiled, containing various structural parameters of the PMDC motor alongside their corresponding cogging torque (T_c), air gap magnetic flux density (B_r), and output torque (T) values. Critical structural features, such as eccentricity and pole arc coefficient, are extracted, as illustrated in Figure 9a. 75% percent of the dataset is used to train the prediction model, while the remaining 25% serves as a test set to evaluate its robustness, as also shown in Figure 9a. The structural parameters from different PMDC motor configurations are then input into the trained random forest model. After adequate training, the model predicts the output characteristics of the PMDC motor on the test set. The correlation coefficients (R^2) between predicted and simulated values for air gap magnetic flux density, cogging torque, and output torque reach 0.899, 0.814, and 0.953, respectively, across both training and test sets, demonstrating excellent model accuracy and strong generalization capability.

Figure 9b–d show the fitting results of the forest-based prediction method for air gap magnetic density, cogging torque, and output torque on both the training and test datasets, demonstrating the high predictive accuracy of the model for the output characteristics of various PMDC motors. Furthermore, using the random forest method, we developed predictive models between the pole arc coefficient, eccentricity, and cogging torque, air gap

magnetic flux density, and output torque. The model predicted a minimal cogging torque of 0.278 N·m when the pole arc coefficient and eccentricity were set to 0.82 and 7 mm, respectively. As shown in Table 2, under these structural parameters, the predicted air gap magnetic flux density and output torque ripple were 0.699 T and 21.12 N·m, respectively, closely aligning with the results obtained from the response surface method based on finite element analysis. Notably, the finite element simulation results in this study show strong agreement with experimental data, further validating the reliability of the proposed random forest prediction approach.

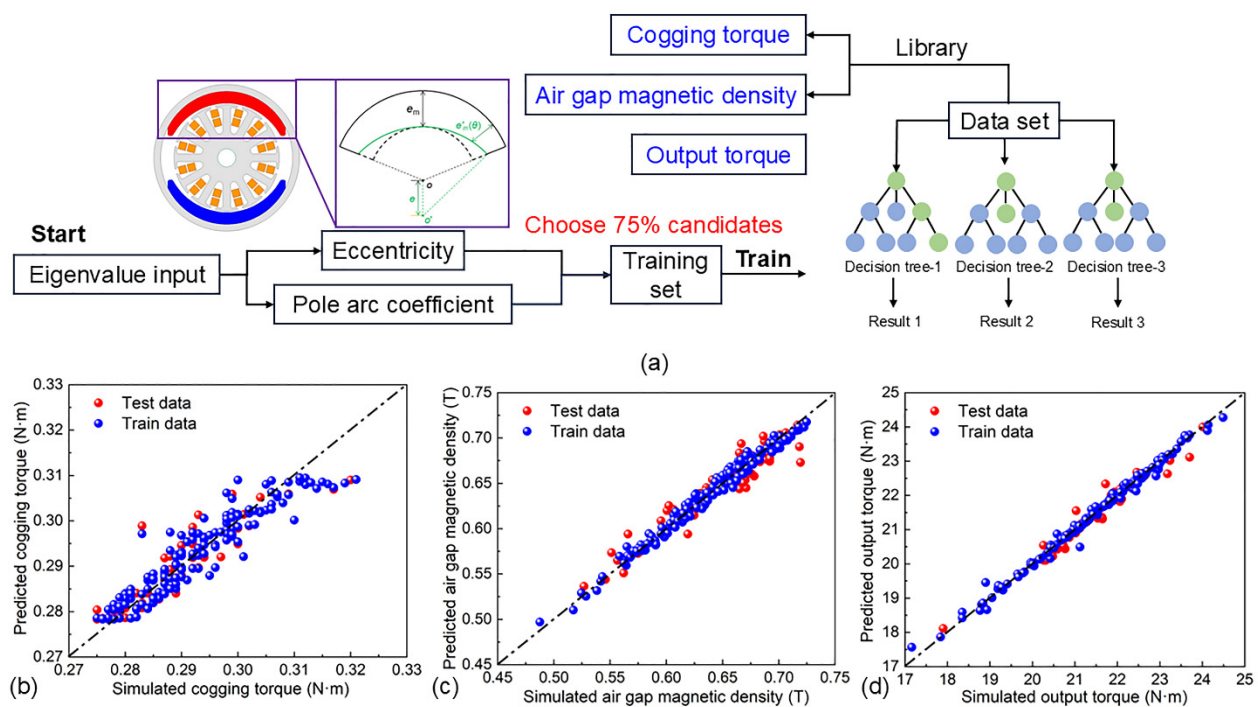


Figure 9. (a) Schematic diagram of the search algorithm based on the random forest model. The predicted fitness values for the test sets in terms of (b) cogging torque, (c) air gap magnetic flux density, and (d) output torque ripple characteristics.

Table 2. The design parameters of pole arc coefficient (α_p), eccentricity (e , mm) for minimal cogging torque (T_c , N·m) with corresponding air gap magnetic density (B_r , T) and output torque ripple (T , N·m) of PMDC motors predicted by the response surface method (RSM) and random forest method (RFM).

	Predicted α_p	Predicted e	Predicted T_c	Predicted B_r	Predicted T
RSM	0.825	6	0.277	0.603	21.02
RFM	0.82	7	0.278	0.699	21.12

6. Conclusions

This study presents a systematic approach for reducing vibration and noise in a 12-slot, two-pole PMDC motor by targeting the suppression of cogging torque through geometric optimization. A prototype was fabricated and tested in a semi-anechoic chamber, where a prominent narrowband peak at 7500 Hz with sound pressure levels between 61.69–65.40 dB was identified and effectively suppressed after optimization, corresponding to the dominant resonance frequency observed in numerical acoustic tests.

Using response surface methodology, the effects of pole arc coefficient and permanent magnet eccentricity on cogging torque were thoroughly investigated. The optimal configuration was determined to be $\alpha_p = 0.825$ and $e = 6$ mm, under which the peak-to-peak

cogging torque was reduced from 0.568 N·m to 0.548 N·m, and the output torque ripple dropped from 21.98 N·m to 21.02 N·m. Vibration simulation results showed a significant reduction in amplitude within the 7400–7600 Hz range. To further improve design efficiency, a random forest machine learning model was developed to predict cogging torque, air gap magnetic flux density, and output torque across various structural configurations. The model achieved R^2 values of 0.814, 0.899, and 0.953, respectively, on the test dataset. It predicted a minimal cogging torque of 0.278 N·m at $\alpha_p = 0.82$ and $e = 7$ mm, results closely aligned with RSM predictions. These findings validate the reliability of combining physics-based modeling with data-driven prediction, offering a robust framework for the NVH optimization of low-cost PMDC motors in automotive applications.

Building on the work presented in this paper, our future research will focus on extended studies to further enhance the engineering applicability and methodological completeness of the proposed data-driven optimization approach: we will complete the fabrication of the optimized motor prototype and full-operating-condition experimental tests to comprehensively validate the effectiveness of the proposed optimization scheme, explore lightweight deep learning models for multi-parameter optimization scenarios to further improve the rapid design efficiency of automotive PMDC motors, and develop a global multi-physics optimization framework with electromagnetic–thermal–structural coupling to realize the collaborative optimization of motor vibration suppression, performance and efficiency enhancement, and heat dissipation improvement.

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