

Article

Dynamic Characteristics and Feedforward Control Methods of Magnetic Bearing Flywheels Under Moving Base Conditions

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Abstract

Magnetic bearing flywheels, characterized by frictionless operation and long service life, are increasingly recognized as promising actuators for spacecraft attitude control. Understanding their dynamic behavior under moving-base conditions is therefore essential. In this study, the Lagrange method is employed to derive the dynamic equations of a magnetic-bearing flywheel subject to base motion. By incorporating the dynamics of electromagnetic bearings, a unified electromechanical-dynamic control model is established. Simulations are conducted to examine the system's response during rapid maneuvers, with a focus on the effects of base moment of inertia, rotor speed, and maneuver angular rate on flywheel performance. Based on the analysis, a feedforward compensation strategy utilizing the angular acceleration of the moving base is proposed to suppress the influence of base motion. Simulation results validate the effectiveness of the proposed method, offering technical support for the future application of magnetically levitated flywheels in ultra-stable, fast-maneuvering satellites.

Keywords: magnetic bearing flywheel; rapid maneuver; moving base; dynamic characteristics; feedforward control

1. Introduction

The most significant difference between magnetic bearing flywheels and conventional flywheels lies in the adoption of magnetic bearings for support. By completely eliminating mechanical contact, magnetic bearings enable friction-free, wear-free, and lubrication-free operation, making them particularly suitable for vacuum and ultra-clean environments. Moreover, magnetic bearing flywheels are capable of operating at extremely high rotational speeds, which significantly reduces energy consumption and maintenance costs while offering a long service life [1]. In addition, magnetic bearings feature actively controllable stiffness and damping, allowing effective vibration suppression through an optimized control algorithm [2–5]. Spacecraft, in particular, demand long-life, lubrication-free, and ultra-clean technologies, making magnetic-bearing flywheels a promising choice for attitude control and energy storage. Consequently, ensuring their stable operation has become a prominent research focus [6–8].

Current research on magnetic-bearing flywheels primarily addresses vibration sources internal to the system, with significant attention devoted to vibrations induced by rotor imbalance and sensor runout. Studies on imbalance-induced vibrations have examined the underlying principles and identification methods for mass imbalance [9], derived corresponding unbalanced dynamic equations, and proposed Least Mean Squares (LMS)-based



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active vibration control strategies and feedback control laws, which have proven effective [10,11]. The introduction of cross-feedback control has further enhanced gyroscopic effect rejection, thereby improving stability margins [12].

The notch filter approach has been widely adopted to control imbalance-induced vibrations, with continuous refinements improving vibration suppression accuracy [13–18]. To further enhance performance, harmonic vibrations caused by sensor runout have been incorporated into control designs [19]. A generalized notch filter was proposed [20], achieving effective suppression through parallel or series configurations of multiple notch filters for harmonic current attenuation [21,22].

To address the complexity of tuning parameters in these methods, repetitive control has been applied to vibration suppression in magnetically bearing flywheels. Variants such as high-order repetitive control [23], composite repetitive control [24], odd-harmonic repetitive control [25,26], and fractional-order repetitive control [27] have demonstrated effective suppression of both mass-imbalance-induced and control-system-originated harmonic vibrations.

However, prior research has primarily focused on vibration suppression within the flywheel system itself. In practice, the influence of external loads on magnetic bearings is critical. Reference [28] studied a 2-degree-of-freedom (2-DOF) magnetic-bearing flywheel mounted on a mobile platform, analyzing the effects of base motion and proposing a sliding-mode control (SMC) method for stable operation under such conditions. Reference [29] examined a rotor system model on a rotating platform, assessing the impact of external loads on rotor-stator clearance. Additionally, research on magnetically bearing flywheels in marine vessels explored how pitch angle variations affect system stability [30]. Collectively, these studies demonstrate that base motion significantly impacts the performance of magnetic-bearing flywheels.

In the context of spacecraft, rapid maneuvers represent a form of external load. Such maneuvers may degrade control performance, induce rotor vibrations, or even reduce rotor-stator clearance to a level that risks instability. Therefore, understanding the dynamic characteristics of magnetic-bearing flywheels under moving-base conditions is essential for their reliable use in rapidly maneuvering spacecraft.

To address this, the present study models rapid spacecraft maneuvers as a moving-base problem and investigates the coupled dynamics of the base–flywheel system. The main contributions are as follows:

1. **Contextualized Modeling:** Using rapid spacecraft maneuvers as the operational background, the Lagrange method is applied to develop a moving-base dynamic model of a magnetic-bearing flywheel. Electromagnetic bearing forces are incorporated, resulting in a unified electromechanical–dynamic model.
2. **Control Model and Parameter Analysis:** Based on the integrated model, a control framework for the magnetic-bearing flywheel is established. The effects of various moving-base parameters on the performance of the levitation controller are systematically analyzed.
3. **Novel Vibration Suppression Strategy:** A feedforward vibration suppression controller, utilizing moving-base angular deceleration measurements, is proposed. Its effectiveness is verified through simulation studies.

The remainder of this paper is organized as follows: Section 2 develops the dynamic model of the magnetic-bearing flywheel under moving-base conditions. Section 3 presents the integrated electromechanical–dynamic model incorporating base excitations. Section 4 analyzes dynamic responses through simulations, highlighting the relationships between spacecraft maneuver parameters and electromagnetic control variables. Section 5 proposes

and validates a suppression method for moving-base effects. Finally, Section 6 summarizes the findings and conclusions.

2. Dynamic Model of the Magnetic Bearing Flywheel Considering Moving Base Excitations

The four-degree-of-freedom (4-DOF) magnetic bearing flywheel model examined in this study is illustrated in Figure 1. It comprises a rotor, stator, base, and upper and lower magnetic bearings. Radial translation and rotation of the rotor are actively controlled by the magnetic bearings, while rotation about the central axis is driven by the motor. Since the magnetic-bearing flywheel will be evacuated after manufacturing, and this study primarily targets space applications, the effects of air friction and gravity are neglected in the present model.

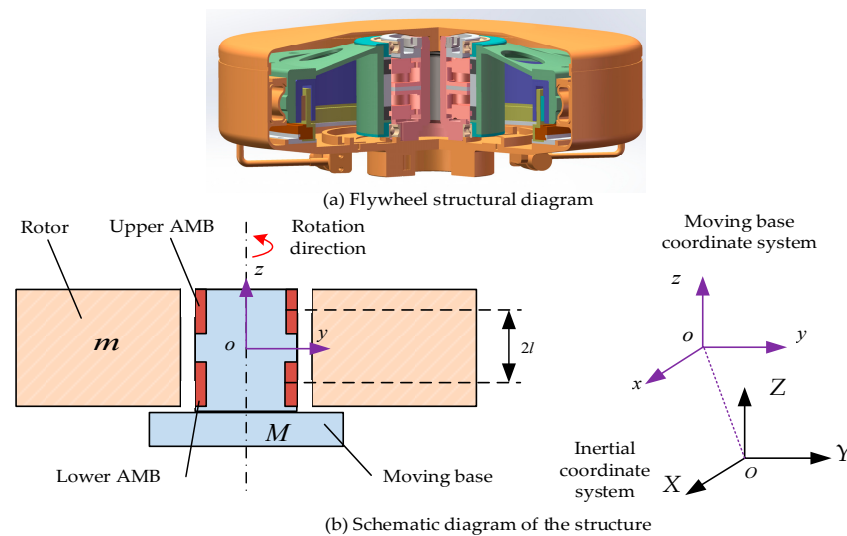


Figure 1. Schematic of the moving base.

For modeling purposes, the stator–base connection is assumed to be rigid. The spacecraft’s maneuver pivot is defined at the rotor center, and the analysis focuses on the effects of maneuver-induced rotational motion about this axis on the magnetically levitated rotor. Three reference frames were established, as shown in Figure 2: the inertial coordinate frame $OXYZ$, moving base frame $ox_gy_gz_g$, and rotor coordinate system $o\zeta\eta\zeta$.

The transformation relationships between coordinate systems are defined as follows:

- (1) Inertial Coordinate System: $OXYZ$ fixed in space.
- (2) Moving Base Frame: $ox_gy_gz_g$; the moving base and stator are treated as rigid bodies M . For convenience of modeling, the center of this rigid body is positioned at the stator center. This frame underwent rotational motion about the X -, Y -, and Z -axes relative to the inertial coordinate system, with coincident origins in the initial state.
- (3) Rotor Coordinate System: $o\zeta\eta\zeta$, fixed to the rotor; $o\zeta$ rotates with the rotor about its center of mass and aligns with the rotor’s principal axes of inertia. In the initial state, their origins coincide. The transformation from the moving base frame to the rotor frame is illustrated in Figure 2: First, a rotation by angle β about the y_g axis yields the $o\zeta_1\eta_1\zeta_1$ frame. Next, a rotation by angle α about the ζ_1 intermediate axis gives the $o\zeta_2\eta_2\zeta_2$ frame. Finally, a rotation by angle φ about the ζ_2 axis results in the rotor coordinate system $o\zeta\eta\zeta$.

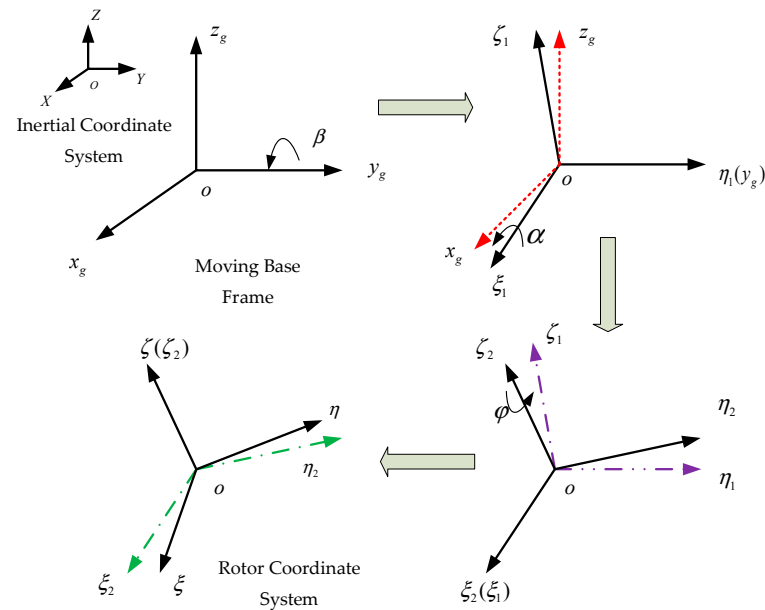


Figure 2. Coordinate transformations between inertial, moving base, and rotor reference frames.

2.1. Rotor Angular Velocity

Based on the defined coordinate systems, the angular velocities of the magnetic-bearing flywheel components are described as follows.

- (1) For the moving base frame $oxyz$, its angular velocity components relative to the inertial coordinate system, projected onto the inertial frame axes, are denoted as $\dot{\theta}_x$, $\dot{\theta}_y$, and $\dot{\theta}_z$.
- (2) For the $o\zeta_1\eta_1\zeta_1$ coordinate system, the angular velocity relative to the inertial space, with components projected onto its own axes, is expressed as

$$\begin{bmatrix} \omega_{\zeta_1} \\ \omega_{\eta_1} \\ \omega_{\zeta_1} \end{bmatrix} = \begin{bmatrix} \cos \beta & 0 & -\sin \beta \\ 0 & 1 & 0 \\ \sin \beta & 0 & \cos \beta \end{bmatrix} \begin{bmatrix} \dot{\theta}_x \\ \dot{\theta}_y \\ \dot{\theta}_z \end{bmatrix} + \begin{bmatrix} 0 \\ \dot{\beta} \\ 0 \end{bmatrix} = \begin{bmatrix} \dot{\theta}_x \cos \beta - \dot{\theta}_z \sin \beta \\ \dot{\theta}_y + \dot{\beta} \\ \dot{\theta}_x \sin \beta + \dot{\theta}_z \cos \beta \end{bmatrix} \quad (1)$$

- (3) For the $o\zeta_2\eta_2\zeta_2$ coordinate system, the components of its angular velocity relative to the inertial space, resolved along its own axes, are expressed as

$$\begin{bmatrix} \omega_{\zeta_2} \\ \omega_{\eta_2} \\ \omega_{\zeta_2} \end{bmatrix} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \alpha & \sin \alpha \\ 0 & -\sin \alpha_g & \cos \alpha_g \end{bmatrix} \begin{bmatrix} \omega_{\zeta_1} \\ \omega_{\eta_1} \\ \omega_{\zeta_1} \end{bmatrix} + \begin{bmatrix} \dot{\alpha} \\ 0 \\ 0 \end{bmatrix} = \begin{bmatrix} \omega_{\zeta_1} + \dot{\alpha}_g \\ \omega_{\eta_1} \cos \alpha + \omega_{\zeta_1} \sin \alpha \\ -\omega_{\eta_1} \sin \alpha + \omega_{\zeta_1} \cos \alpha \end{bmatrix} \quad (2)$$

- (4) For the $o\zeta\eta\zeta$ coordinate system, the components of its angular velocity relative to the inertial space, resolved along its own axes, are expressed as

$$\begin{bmatrix} \omega_{\zeta} \\ \omega_{\eta} \\ \omega_{\zeta} \end{bmatrix} = \begin{bmatrix} \cos \varphi & \sin \varphi & 0 \\ -\sin \varphi & \cos \varphi & 0 \\ 0 & 0 & 1 \end{bmatrix} \begin{bmatrix} \omega_{\zeta_2} \\ \omega_{\eta_2} \\ \omega_{\zeta_2} \end{bmatrix} + \begin{bmatrix} 0 \\ 0 \\ \dot{\varphi} \end{bmatrix} = \begin{bmatrix} \omega_{\zeta_2} \cos \varphi + \omega_{\eta_2} \sin \varphi \\ -\omega_{\zeta_2} \sin \varphi + \omega_{\eta_2} \cos \varphi \\ \omega_{\zeta_2} + \dot{\varphi} \end{bmatrix} \quad (3)$$

Substituting Equation (2) into Equation (3) yields the expression for the angular velocity of the rotor relative to the inertial space, which is resolved in the inertial coordinate system as follows:

$$\begin{cases} \omega_{\xi 2} = \dot{\alpha} + \cos \beta \dot{\theta}_x - \sin \beta \dot{\theta}_z \\ \omega_{\eta 2} = \cos \alpha \dot{\beta} + \sin \alpha \sin \beta \dot{\theta}_x + \cos \alpha \dot{\theta}_y + \cos \beta \sin \alpha \dot{\theta}_z \\ \omega_{\zeta 2} = -\sin \alpha \dot{\beta} + \cos \alpha \sin \beta \dot{\theta}_x - \sin \alpha \dot{\theta}_y + \cos \alpha \cos \beta \dot{\theta}_z \end{cases} \quad (4)$$

Typically, the rotor deflection angles are significantly smaller than the maneuver angles. Therefore, $\sin \alpha \approx \alpha$, $\cos \alpha \approx 1$, $\sin \beta \approx \beta$, and $\cos \beta \approx 1$; by neglecting the higher-order term $\sin \alpha \sin \beta \dot{\theta}_x$, Equation (4) can be approximated and simplified as

$$\begin{cases} \omega_{\xi 2} = \dot{\alpha} + \dot{\theta}_x - \beta \dot{\theta}_z \\ \omega_{\eta 2} = \dot{\beta} + \dot{\theta}_y + \alpha \dot{\theta}_z \\ \omega_{\zeta 2} = -\alpha \dot{\beta} + \beta \dot{\theta}_x - \alpha \dot{\theta}_y + \dot{\theta}_z \end{cases} \quad (5)$$

2.2. Equations of Motion for the Magnetic-Bearing Flywheel

The kinetic energy of the rotor can be expressed as $T_R = T_{r1} + T_{r2} + T_{r3}$, where

$$\begin{cases} T_{r1} = \frac{1}{2} J_d \omega_{\xi 2}^2 \\ T_{r2} = \frac{1}{2} J_d \omega_{\eta 2}^2 \\ T_{r3} = \frac{1}{2} J_p (\omega_{\zeta 2} + \Omega)^2 \end{cases} \quad (6)$$

where T_{r1} and T_{r2} represent the rotational kinetic energies of the equatorial axes; T_{r3} denotes the rotational kinetic energy about the polar axis; J_d is the equatorial moment of inertia; J_p is the polar moment of inertia of the rotor. Substituting Equation (5) into Equation (6) yields

$$\begin{cases} T_{r1} = \frac{1}{2} J_d \omega_{\xi 2}^2 = \frac{1}{2} J_d (\dot{\alpha}^2 + \dot{\theta}_x^2 + 2\dot{\alpha}\dot{\theta}_x + 2\dot{\alpha}\dot{\theta}_z\beta + 2\dot{\theta}_x\dot{\theta}_z\beta + \dot{\theta}_z^2\beta^2) \\ T_{r2} = \frac{1}{2} J_d \omega_{\eta 2}^2 = \frac{1}{2} J_d (\dot{\beta}^2 + \dot{\theta}_y^2 + 2\dot{\beta}\dot{\theta}_z + 2\dot{\beta}\dot{\theta}_z\alpha + 2\dot{\theta}_y\dot{\theta}_z\alpha + \dot{\theta}_z^2\alpha^2) \\ T_{r3} = \frac{1}{2} J_p (\omega_{\zeta 2} + \Omega)^2 = \frac{1}{2} J_p (\Omega^2 + \dot{\theta}_z^2 + 2\Omega\dot{\theta}_z - 2\Omega\dot{\beta}\alpha - 2\Omega\alpha\dot{\theta}_y + 2\Omega\beta\dot{\theta}_x - 2\dot{\beta}\dot{\theta}_z\alpha - 2\dot{\theta}_y\dot{\theta}_z\alpha + 2\dot{\theta}_x\dot{\theta}_z\beta) \end{cases} \quad (7)$$

Neglecting the fourth-order terms and the relatively small third-order terms in (7), the expression can be simplified as follows:

$$\begin{cases} T_{r1} = \frac{1}{2} J_d \omega_{\xi 2}^2 = \frac{1}{2} J_d (\dot{\alpha}^2 + \dot{\theta}_x^2 + 2\dot{\alpha}\dot{\theta}_x) \\ T_{r2} = \frac{1}{2} J_d \omega_{\eta 2}^2 = \frac{1}{2} J_d (\dot{\beta}^2 + \dot{\theta}_y^2 + 2\dot{\beta}\dot{\theta}_y) \\ T_{r3} = \frac{1}{2} J_p (\omega_{\zeta 2} + \Omega)^2 = \frac{1}{2} J_p (\Omega^2 + \dot{\theta}_z^2 + 2\Omega\dot{\theta}_z - 2\Omega\dot{\beta}\alpha - 2\Omega\alpha\dot{\theta}_y + 2\Omega\beta\dot{\theta}_x) \end{cases} \quad (8)$$

The kinetic energy of the rotor can be expressed as

$$\begin{aligned} T_R = & \frac{1}{2} J_d (\dot{\alpha}^2 + \dot{\theta}_x^2 + 2\dot{\alpha}\dot{\theta}_x + \dot{\beta}^2 + \dot{\theta}_y^2 + 2\dot{\beta}\dot{\theta}_y) \\ & + \frac{1}{2} J_p (\Omega^2 + \dot{\theta}_z^2 + 2\Omega\dot{\theta}_z - 2\Omega\dot{\beta}\alpha - 2\Omega\alpha\dot{\theta}_y + 2\Omega\beta\dot{\theta}_x) \end{aligned} \quad (9)$$

and the translational kinetic energy is

$$T_P = \frac{1}{2} m (\dot{x}^2 + \dot{y}^2) \quad (10)$$

where $\dot{x}$ and $\dot{y}$ denote the velocity components of the center of mass of the rotor along the x and y axes, respectively, and m denotes the mass of the rotor.

Since this study focuses on the effects of base rotational maneuvers on the magnetic-bearing flywheel while neglecting translational base motion, the kinetic energy of the moving base can be expressed as

$$T_D = \frac{1}{2} J_D (\dot{\theta}_x^2 + \dot{\theta}_y^2 + \dot{\theta}_z^2) \quad (11)$$

where J_D represents the moments of inertia of the stator–base assembly about the x -, y -, and z -axes.

The total kinetic energy of the system is then the sum of the rotor’s rotational energy, the rotor’s translational energy, and the base’s rotational energy:

$$T = T_R + T_P + T_D \quad (12)$$

That is,

$$\begin{aligned} T = & \frac{1}{2} J_d (\dot{\alpha}^2 + \dot{\theta}_x^2 + 2\dot{\alpha}\dot{\theta}_x + \dot{\beta}^2 + \dot{\theta}_y^2 + 2\dot{\beta}\dot{\theta}_y) \\ & + \frac{1}{2} J_p (\Omega^2 + \dot{\theta}_z^2 + 2\Omega\dot{\theta}_z - 2\Omega\dot{\beta}\dot{\alpha} - 2\Omega\dot{\alpha}\dot{\theta}_y + 2\Omega\dot{\beta}\dot{\theta}_x) \\ & + \frac{1}{2} m (\dot{x}^2 + \dot{y}^2) + J_D (\dot{\theta}_x^2 + \dot{\theta}_y^2 + \dot{\theta}_z^2) \end{aligned} \quad (13)$$

In magnetic-bearing control systems, gravitational effects encountered during ground-based testing can be compensated by applying a bias current; however, such effects are absent in aerospace applications. Therefore, gravitational influence is neglected in this study, and the system’s potential energy is treated as constant.

$$U = 0 \quad (14)$$

The generalized coordinates of the system are defined as

$$q_1 = x, q_2 = \beta, q_3 = y, q_4 = \alpha, q_5 = \theta_x, q_6 = \theta_y, q_7 = \theta_z$$

The generalized forces are defined as

$$\begin{cases} Q_1 = F_{xu} + F_{xd} \\ Q_2 = F_{xu}l_{mu} - F_{xd}l_{md} \\ Q_3 = F_{yu} + F_{yd} \\ Q_4 = F_{yd}l_{md} - F_{yu}l_{mu} \\ Q_5 = 0, Q_6 = 0, Q_7 = 0 \end{cases} \quad (15)$$

where F_{xu} and F_{yu} represent the electromagnetic forces in the x and y directions, respectively, generated by the upper bearing (located in the positive z -axis direction). F_{xd} and F_{yd} represent the electromagnetic forces in the x - and y -directions generated by the lower bearing, respectively, whereas l_{mu} and l_{md} define the axial separations from the upper and lower bearing centers to the rotor center, respectively.

The equations of motion are derived using Lagrange’s equation, expressed as

$$\frac{d}{dt} \left(\frac{\partial T}{\partial \dot{q}_i} \right) - \frac{\partial T}{\partial q_i} + \frac{\partial U}{\partial q_i} = Q_i \quad (i = 1, 2, 3, \dots) \quad (16)$$

By substituting the expressions for kinetic energy, potential energy, generalized coordinates, and generalized forces into Lagrange’s Equation (16), the following equations of motion are obtained:

$$\begin{cases} m\ddot{x} = F_{xu} + F_{xd} \\ J_d\ddot{\beta} + J_d\ddot{\theta}_y - J_p\Omega\dot{\alpha} - J_p\Omega\dot{\theta}_x = F_{xu}l_{mu} - F_{xd}l_{md} \\ m\ddot{y} = F_{yu} + F_{yd} \\ J_d\ddot{\alpha} + J_d\ddot{\theta}_x + J_p\Omega\dot{\beta} + J_p\Omega\dot{\theta}_y = F_{yd}l_{md} - F_{yu}l_{mu} - F_{xu} \\ J_d\ddot{\theta}_x + J_d\ddot{\alpha} + J_p\Omega\dot{\beta} + J_D\ddot{\theta}_x = 0 \\ J_d\ddot{\theta}_y + J_d\dot{\beta} - J_p\Omega\dot{\alpha} + J_D\ddot{\theta}_y = 0 \\ J_p\ddot{\theta}_z + J_D\ddot{\theta}_z + J_p\dot{\Omega} = 0 \end{cases} \quad (17)$$

By subtracting the fifth equation from the fourth equation in Equation (17), we obtain $\ddot{\theta}_x$; through the second and sixth equations, we derive $\ddot{\theta}_y$, that is,

$$\begin{cases} \ddot{\theta}_x = \frac{J_p\Omega}{J_D}\dot{\theta}_y - J_D(F_{xu}l_{mu} - F_{xd}l_{md}) \\ \ddot{\theta}_y = -\frac{J_p\Omega}{J_D}\dot{\theta}_x - J_D(F_{yd}l_{md} - F_{yu}l_{mu}) \end{cases} \quad (18)$$

Substituting Equation (18) into Equation (17) and simplifying it yields

$$\begin{cases} m\ddot{x} = F_{xu} + F_{xd} \\ J_d\ddot{\beta} - J_p\Omega\dot{\alpha} - (J_p + \frac{J_dJ_p}{J_D})\Omega\dot{\theta}_x = (1 + \frac{J_d}{J_D})(F_{xu}l_{mu} - F_{xd}l_{md}) \\ m\ddot{y} = F_{yu} + F_{yd} \\ J_d\ddot{\alpha} + J_p\Omega\dot{\beta} + (J_p + \frac{J_dJ_p}{J_D})\Omega\dot{\theta}_y = (1 + \frac{J_d}{J_D})(F_{yd}l_{md} - F_{yu}l_{mu}) \\ J_D\ddot{\theta}_x - J_p\Omega\dot{\theta}_y = -(F_{yd}l_{md} - F_{yu}l_{mu}) \\ J_D\ddot{\theta}_y + J_p\Omega\dot{\theta}_x = -(F_{xu}l_{mu} - F_{xd}l_{md}) \\ (J_p + J_D)\ddot{\theta}_z + J_p\dot{\Omega} = 0 \end{cases} \quad (19)$$

From the equations of motion for the magnetic-bearing flywheel under base rotation about the x - and y -axes, the base motion can be regarded as an external disturbance when analyzing the dynamic response of the moving base. This disturbance can be expressed as

$$W_{D2} = \left(J_p\Omega + \frac{J_dJ_p\Omega}{J_D} \right) \dot{\theta}_x, \quad W_{D4} = \left(J_p\Omega + \frac{J_dJ_p\Omega}{J_D} \right) \dot{\theta}_y \quad (20)$$

3. Electromechanical-Dynamic Model Under Moving Base Conditions

From the dynamic equations of the magnetic-bearing flywheel, it is evident that control primarily targets the axial and radial degrees of freedom. Accordingly, response control is considered mainly within the radial plane. The governing dynamic equation is given by

$$\begin{cases} m\ddot{x} = F_{xu} + F_{xd} \\ J_d\ddot{\beta} - J_p\Omega\dot{\alpha} = (1 + \frac{J_d}{J_D})(F_{xu}l_{mu} - F_{xd}l_{md}) + (J_p + \frac{J_dJ_p}{J_D})\Omega\dot{\theta}_x \\ m\ddot{y} = F_{yu} + F_{yd} \\ J_d\ddot{\alpha} + J_p\Omega\dot{\beta} = (1 + \frac{J_d}{J_D})(F_{yd}l_{md} - F_{yu}l_{mu}) - (J_p + \frac{J_dJ_p}{J_D})\Omega\dot{\theta}_y \end{cases} \quad (21)$$

From the first and third equations of (21), it can be seen that the rotation of the moving base has no effect on the translation of the rotor but mainly affects the radial rotation of the rotor. From the second and fourth formulas, it can be seen that owing to the movement of the base, two additional torque terms, $(J_p + J_dJ_p/J_D)\Omega\dot{\theta}_x$ and $(J_p + J_dJ_p/J_D)\Omega\dot{\theta}_y$, are generated, which in turn affect the rotation of the y -axis and the x -axis, respectively.

Equation (21) is rewritten into matrix form as follows:

$$\hat{M}\ddot{\mathbf{q}} + \hat{\mathbf{G}}\dot{\mathbf{q}} = \hat{\mathbf{H}}\mathbf{F} + \hat{\mathbf{W}} \quad (22)$$

where

$$\hat{M} = \begin{bmatrix} m & 0 & 0 & 0 \\ 0 & J_d & 0 & 0 \\ 0 & 0 & m & 0 \\ 0 & 0 & 0 & J_d \end{bmatrix}, \hat{G} = J_p \Omega \begin{bmatrix} 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 1 \\ 0 & 0 & 0 & 0 \\ 0 & -1 & 0 & 0 \end{bmatrix}$$

$$T_m = \begin{bmatrix} 1 & l_{mu} & 0 & 0 \\ 1 & -l_{md} & 0 & 0 \\ 0 & 0 & 1 & l_{md} \\ 0 & 0 & 1 & -l_{md} \end{bmatrix}, \hat{H} = \begin{bmatrix} 1 & 1 & 0 & 0 \\ l_{mu} & -l_{mu} & 0 & 0 \\ 0 & 0 & 1 & 1 \\ 0 & 0 & l_{md} & -l_{md} \end{bmatrix}$$

$$\hat{W} = \begin{bmatrix} 0 \\ (J_p + \frac{J_d J_p}{J_D}) \Omega \dot{\theta}_x \\ 0 \\ -(J_p + \frac{J_d J_p}{J_D}) \Omega \dot{\theta}_y \end{bmatrix}, q = \begin{bmatrix} x \\ \beta \\ y \\ -\alpha \end{bmatrix}, F = \begin{bmatrix} F_{xu} \\ F_{xd} \\ F_{yu} \\ F_{yd} \end{bmatrix}$$

Perform Laplace transformation on Equation (22) to obtain

$$\begin{cases} ms^2 x(s) = F_{xu}(s) + F_{xd}(s) \\ J_d s^2 \beta(s) - J_p \Omega s \alpha(s) = (1 + \frac{J_d}{J_D})(F_{xu}(s)l_{mu} - F_{xd}(s)l_{md}) + (J_p + \frac{J_d J_p}{J_D}) \Omega \dot{\theta}_x \\ ms^2 y(s) = F_{yu}(s) + F_{yd}(s) \\ J_d s^2 \alpha(s) + J_p \Omega s \beta(s) = (1 + \frac{J_d}{J_D})(F_{yd}(s)l_{md} - F_{yu}(s)l_{mu}) - (J_p + \frac{J_d J_p}{J_D}) \Omega \dot{\theta}_y \end{cases} \quad (23)$$

Note $\hat{P} = (\hat{M}s^2 + \hat{G}s)^{-1}$. By adding a power amplifier G_w , controller G_c , current coefficient K_i and displacement coefficient K_s and then following the method of reference [31], the above dynamic equations are converted into a control system block diagram, as shown in Figure 3.

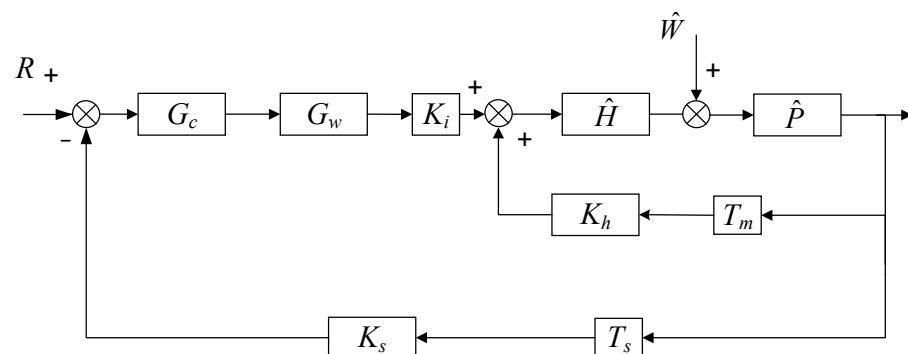


Figure 3. Structural Block Diagram of the Magnetic Float Flywheel System.

When translational motion of the moving base is neglected and the magnetic-bearing flywheel is assumed to have a symmetric structure, the geometric parameters of the upper and lower bearing displacement sensors are identical; that is, $l_{mu} = l_{md} = l_m$, $l_{su} = l_{sd} = l_s$. The rotation control system for the x - and y -axes is illustrated in Figure 4, where the red box highlights additional terms introduced by the moving-base conditions. It can be seen that base motion alters the control system's characteristics and generates an interference torque.

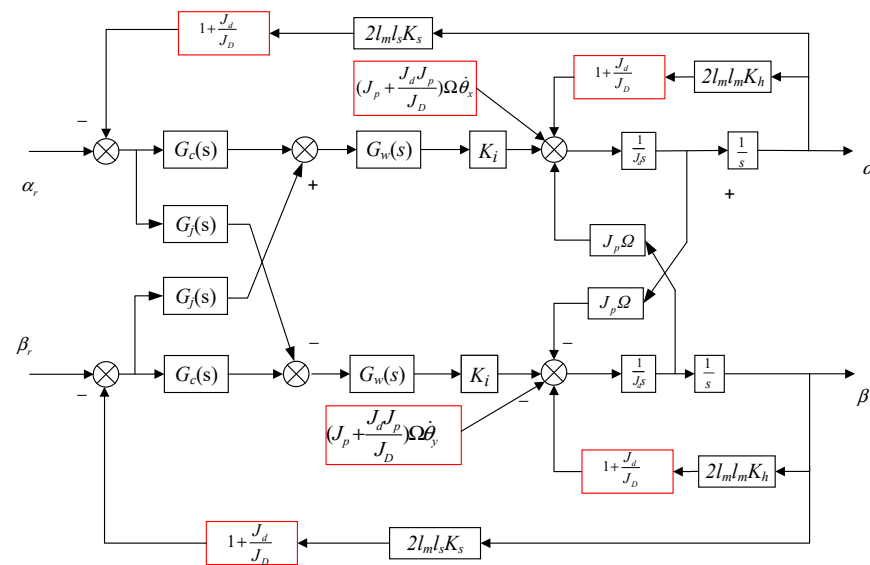


Figure 4. Control block diagram of the moving base interference system.

4. Dynamic Response Simulation Analysis of Magnetic Flywheel

4.1. Simulation Parameter Settings

This section presents the simulation of the moving base using a laboratory-designed space flywheel. The simulation parameters are listed in Table 1. As shown in Figure 4, in addition to the intrinsic parameters of the magnetic-bearing flywheel, the primary factors influencing the moving-base response include the base's angular rate ($\dot{\theta}_x, \dot{\theta}_y$), the rotor's rotational speed (Ω), and the base's rotational inertia (J_D). The effects of these parameters on the dynamic characteristics of the magnetic-bearing flywheel were investigated through simulation.

Table 1. Simulation analysis system parameters.

Parameter	Symbol	Unit	Value	Parameter	Symbol	Unit	Value
Rotor equatorial moment of inertia	J_p	kg·m ²	0.0458	Controller parameters	k_p	/	15
Rotor extreme moment of inertia	J_d	kg·m ²	0.0237	Controller parameters	k_I	/	0.5
Maximum of inertia of the base	J_D	kg·m ²	125	Controller parameters	k_D	/	0.02
Current stiffness coefficient	K_i	N·A ⁻¹	125	Incomplete differential parameters	N	/	10,000
Displacement stiffness coefficient	K_h	N·m ⁻¹	50,000	Distance of magnetic bearing from centroid	l_m	m	0.020
Amplifier gain	K_w	A·V ⁻¹	0.000037	Distance from sensor to centroid	l_s	m	0.024
Amplifier time constant	τ_w	/	0.000071	Unbalanced torque phase	φ_d	deg	0
Sensor sensitivity	k_s	V·rad ⁻¹	8000	Unbalanced torque mass	m_d	kg	0.028
Sampling Gain	K_{ad}	V ⁻¹	3276.8	Unbalanced torque slewing radius	m_r	m	0.05

Two maneuver profiles, instructions A and B, as shown in Figure 5, were used in the simulation. Instruction A applies an angular acceleration of 0.1 rad/s² starting at 4 s,

maintaining this acceleration until the angular rate reaches 0.1 rad/s. At 10 s, the maneuver reverses direction with the same magnitude of angular acceleration until the angular rate reaches -0.1 rad/s. After stabilizing, steering continues until the angular position returns to 0° . Instruction B follows a similar pattern, whereas it has a larger acceleration of 0.2 rad/s^2 than Instruction A. Consequently, Instruction B requires less time to accelerate to the same angular velocity, indicating a more agile maneuver.

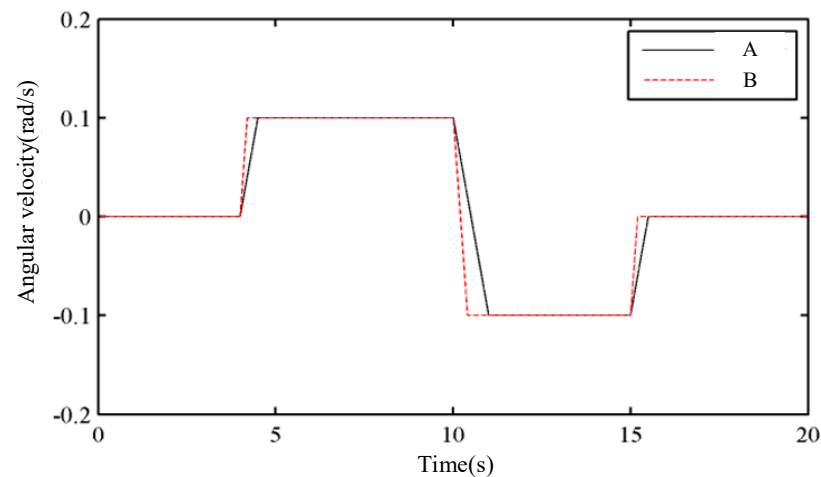


Figure 5. Mobile instructions A and B.

4.2. Coupling Effects Between Maneuver Angular Rates $\dot{\theta}_x$ and $\dot{\theta}_y$

For simplicity, the base and inertial coordinate systems were initially aligned so that the relative angle between them was zero. In practical scenarios, however, the stator and base usually have some initial angular offset relative to the inertial frame, resulting in components along both the x - and y -axes. This study examines the effect of rotating the two control channels when the maneuver occurs along only one axis. Using Instruction A, with the maneuver directed along the x -axis and the rotor speed set to 3000 rpm, simulation results are presented in Figures 6 and 7.

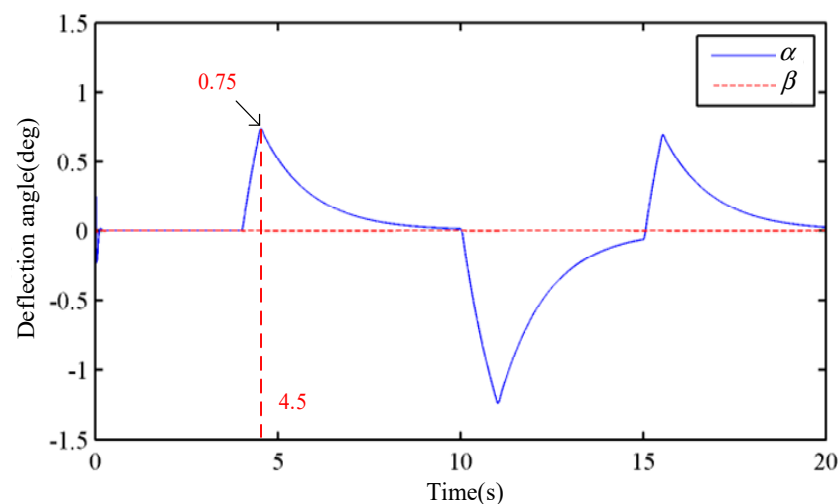


Figure 6. Diagram of the rotation angle of the motorized rotor in the x -direction alone.

As shown in Figure 6, following disturbance from the maneuver, the deflection angle in the x -direction peaks at approximately 0.75° around 4.5 s. During this period, the angular rate command remains constant, and the deflection gradually decreases, returning to the center position after roughly 10 s. Thus, the flywheel rotor experiences deflection during

maneuvering, but once the acceleration ceases, the rotor restores its original position within 5.5 s. This deflection induces changes in angular momentum, representing the moving base effect. When maneuvering exclusively along the x -axis, deflections and fluctuations in the y -direction are negligible.

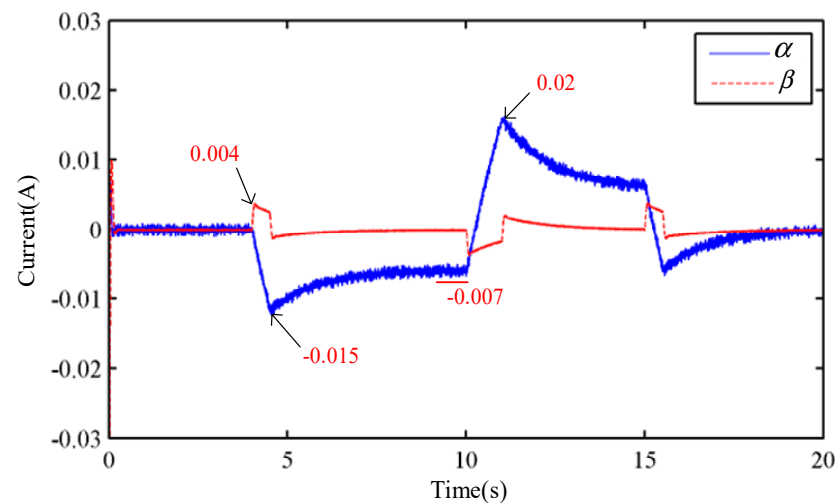


Figure 7. Control current by the single x -directional maneuverable deflection channel.

Figure 7 illustrates the control current variations in the magnetic bearing flywheel's deflection channels during the x -axis maneuver. The control current in the x -channel peaks at 0.015 A at 4.5 s before gradually declining to 0.007 A. Although the y -deflection channel experiences no direct external disturbance, its control current rises rapidly to 0.004 A due to coupling effects and then diminishes to zero. This indicates notable coupling between control channels, where current variations in one channel influence the other. Nevertheless, the controller promptly compensates for these changes, limiting the rotor's deflection. Consequently, the overall impact of the deflection angle on system performance can be considered negligible.

4.3. The Influence of Maneuvering Angular Acceleration

To investigate the effect of angular acceleration, the rotor speed was fixed at 3000 rpm, and simulations were conducted using maneuver commands A and B, applied to the x - and y -deflection channels. Figure 8 compares the deflection angles induced by these two commands. Higher angular acceleration resulted in greater deflection angles. Specifically, Command A produced a peak deflection of 1.24° at 11.04 s, while Command B reached a higher peak of 1.52° , exceeding Command A by 0.28° . During the initial maneuver phase, starting at 4 s, Command A caused a smaller peak deflection of 0.74° at 4.54 s, whereas Command B induced a larger peak of 0.82° at 4.25 s.

As shown in Figures 8 and 9, under the higher acceleration command B, the control current exhibits more pronounced oscillations, reaching peaks of 0.014 A and 0.019 A. Despite this, the current stabilizes to similar steady-state values once the maneuver is complete. The results indicate that greater angular acceleration leads to larger rotor deflection angles relative to the base. However, after acceleration ends, the current required to maintain a constant angular velocity remains essentially unchanged. Therefore, faster maneuvers induce greater rotor deflections in the magnetic-bearing flywheel.

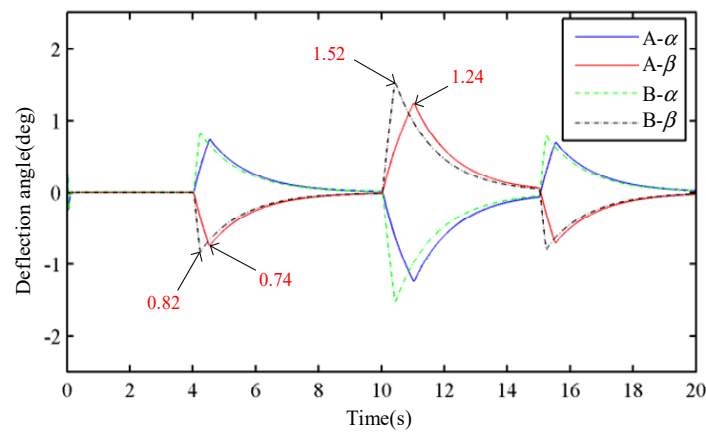


Figure 8. Comparison of deflection angles in α and β directions under A and B instructions.

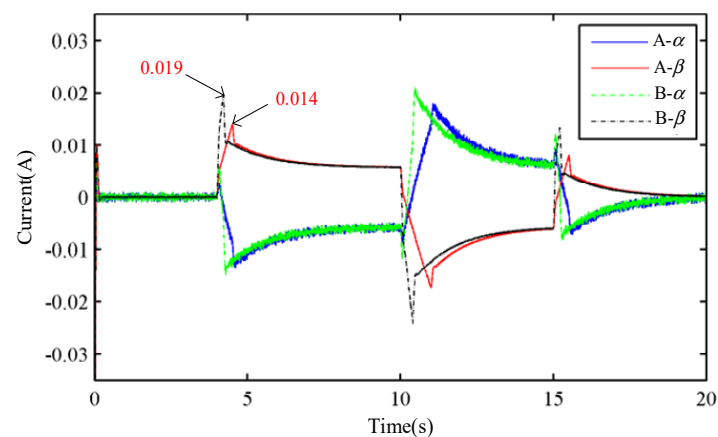


Figure 9. Comparison of currents in α and β directions under A and B instructions.

4.4. The Influence of Rotor Speed

Using command A, the dynamic response of the magnetic-bearing flywheel was simulated at rotor speeds of 2000 rpm and 4000 rpm. Figure 10 shows the rotor deflection angle over time. During the acceleration phase, the deflection angles reached 0.45° at 2000 rpm (Ω_1) and 0.98° at 4000 rpm (Ω_2), representing a 77.36% increase as the speed doubled. This demonstrates that rotor deflection becomes more pronounced at higher speeds. Correspondingly, Figure 11 illustrates that the control current increases with speed, stabilizing at 0.004 A for Ω_1 and 0.008 A for Ω_2 . Thus, higher speeds result in larger deflections and greater steady-state control currents.

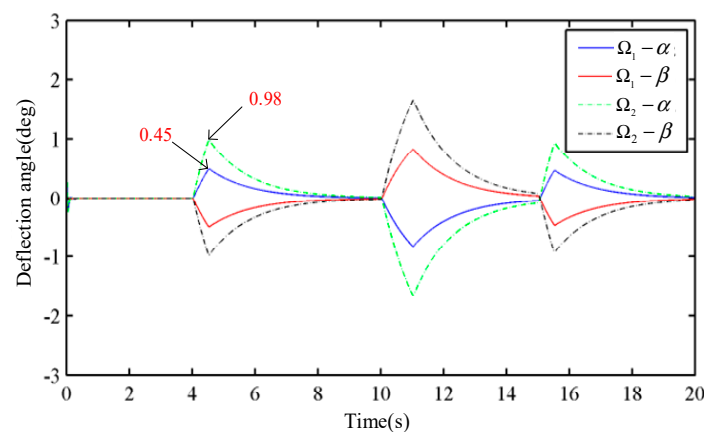


Figure 10. Comparison of deflection angles in α and β directions under rotational speeds Ω_1 and Ω_2 .

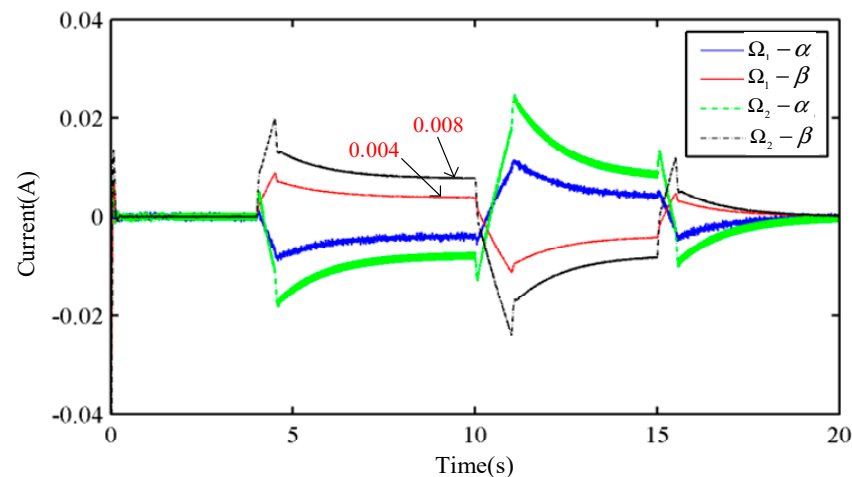


Figure 11. Comparison of currents in α and β directions under rotational speeds Ω_1 and Ω_2 .

4.5. Influence of Moment of Inertia of Moving Base J_D

According to its dynamic equation and control block diagram, the rotational inertia of the base primarily affects the characteristics of the system through $(1 + J_d/J_D)$. In $J_D \gg J_d$ and $(1 + J_d/J_D) \rightarrow 1$, the influence of the rotational inertia of the base was not considered, this situation is recorded as C. For the convenience of comparison, another ideal extreme value $J_d/J_D = 0.2$ is taken, that is, $(1 + J_d/J_D) = 1.2$ is recorded as D. The rotor speed was fixed at 3000 rpm, and simulations were conducted under command A. The results, shown in Figures 12 and 13, indicate that the deflection angles at points C and D are 0.735° and 0.737° , respectively, with both reaching approximately 1.24° near 11 s. These findings suggest that, when the moving base acceleration is constant, the rotor deflection angle is minimally affected by variations in rotational inertia.

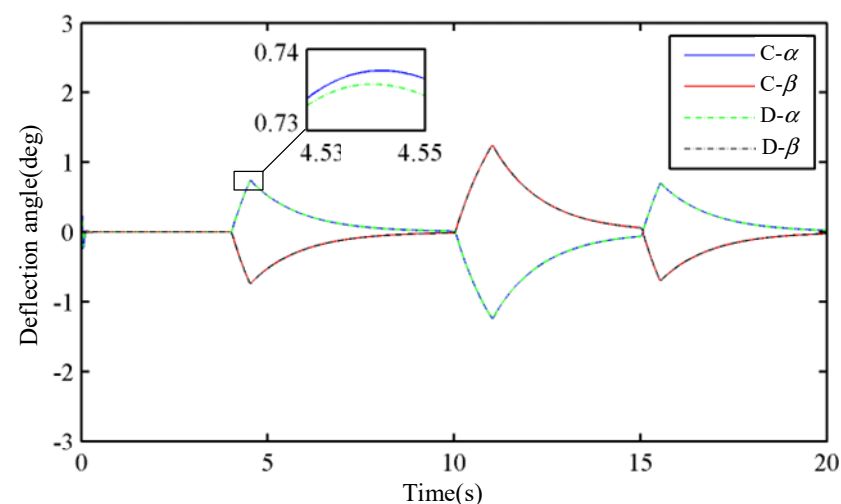


Figure 12. Comparison of deflection angles in the α and β directions under moments of inertia C and D.

Figure 13 shows that when the base's moment of inertia is significantly larger than that of the flywheel rotor, the stabilized control current is approximately 0.006 A. Conversely, when the base's moment of inertia is very small, the steady-state current rises to 0.007 A, an increase of 0.001 A. As the base's moment of inertia decreases, the control current correspondingly increases by a factor of 1.26 relative to the original inertia. This indicates that the base's moment of inertia influences the system's control current: a smaller ratio of base inertia to rotor inertia results in higher stabilization currents. However, this effect has a negligible impact on rotor deflection.

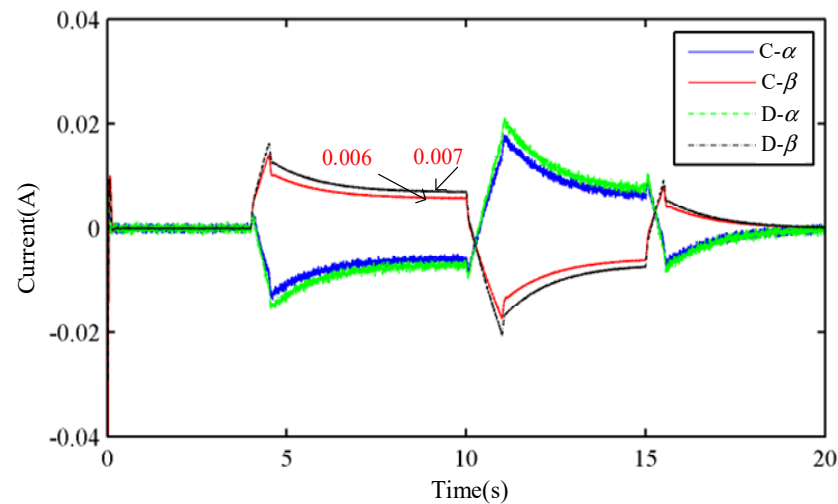


Figure 13. Comparison of currents in the α and β directions under moments of inertia C and D.

5. Method for Suppressing the Effect of Moving Base

The motion of the moving base primarily arises from external disturbances. To mitigate these base-induced effects, this study employs a feedforward compensation strategy. The control block diagram illustrating this suppression method is presented in Figure 14. Initially, the base's angular velocity is measured, and the resulting data are fed into the control system for compensation. For complete compensation, the compensation function G_{FC} must satisfy the following form:

$$\hat{H}K_iG_WG_{FC} = \hat{W} \quad (24)$$

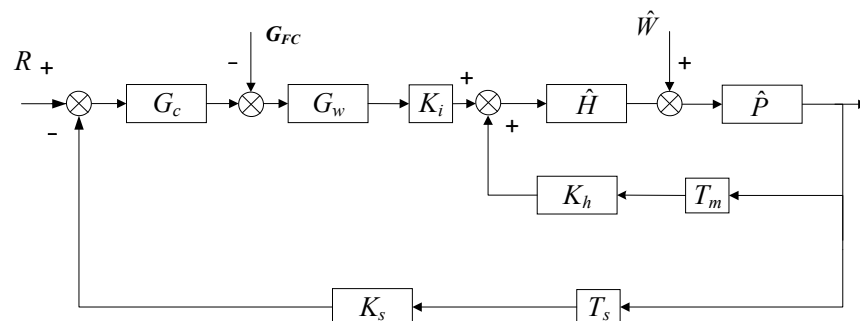


Figure 14. Feedforward suppression method control block diagram.

That is,

$$G_{FC} = (\hat{H}K_iG_W)^{-1}\hat{W} \quad (25)$$

Due to the inability to directly measure angular acceleration and precisely determine model parameters, the base angular rate feedback suppression method requires the introduction of a compensation factor to account for model uncertainties. At a rotor speed of 3000 rpm under Command A, compensation factors λ of 0, 0.5, and 1 were tested to evaluate their effectiveness, as shown in Figure 15. A compensation factor of 1 achieves full suppression, resulting in no rotor deflection during maneuver execution. With a compensation factor of 0.5, the deflection angle was reduced to 0.37° during the initial maneuver, compared to 0.74° without compensation.

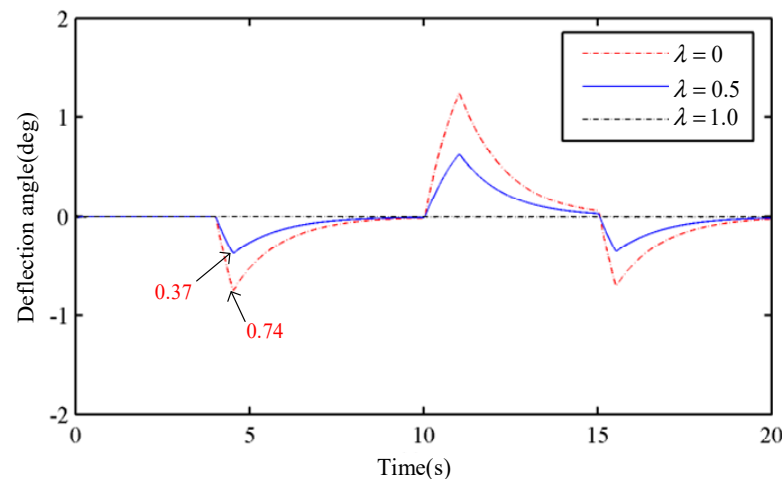


Figure 15. Comparison of declining angles for different compensation factors.

Figure 16 illustrates that the peak control current during the first maneuver dropped from 0.012 A without feedforward compensation to 0.009 A with a compensation factor of 0.5. Full compensation eliminated current fluctuations entirely. During acceleration, the control current gradually increased; once acceleration ceased, the current stabilized. The interference torque current required to counteract the maneuvering angular rate was approximately 0.006 A. Complete compensation aligns the control current closely with the command signal, effectively suppressing interference caused by base motion, maintaining zero rotor deflection throughout maneuvers, and ensuring accurate flywheel output torque.

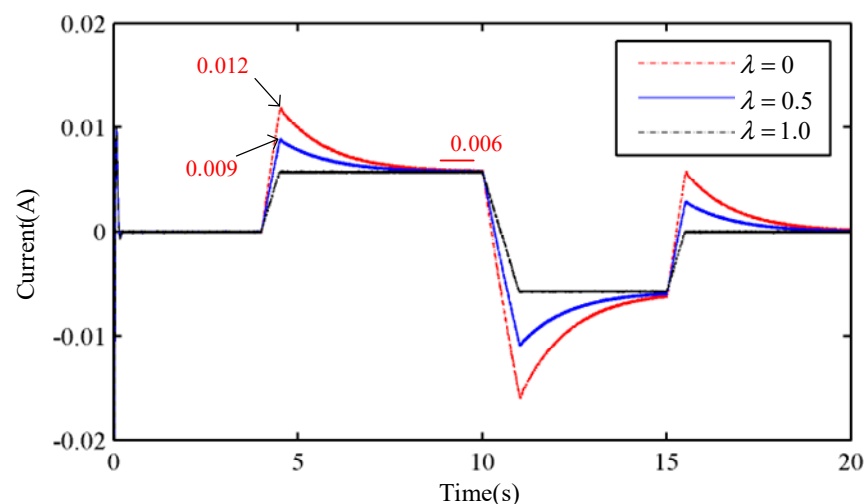


Figure 16. Comparison of currents of different compensation factors.

In practical engineering applications, the optimal compensation factor λ can be identified through iterative tuning to minimize the effects of moving base disturbances.

6. Conclusions

In this study, the dynamic equations under moving base conditions were first derived using the Lagrangian formulation. An integrated electromagnetic-dynamic model was subsequently developed by incorporating the characteristics of the magnetic bearings, leading to a comprehensive control block diagram of the system. Simulations were performed to investigate the effects of spacecraft maneuver angular acceleration, flywheel speed, and base moment of inertia on the rotational dynamics and control performance of the magnetic-bearing flywheel.

The results show that increasing the flywheel speed intensifies the moving base effect, while higher angular acceleration results in larger rotor deflection angles and peak control currents. In contrast, the base moment of inertia has a relatively minor influence on rotor deflection, although it affects the control current to a certain extent; specifically, a higher base-to-rotor inertia ratio reduces the impact on the control current.

Furthermore, a feedforward compensation method based on the angular velocity of the moving base was proposed to suppress rotor vibrations. Simulation results demonstrate that this approach effectively mitigates deflection vibrations of the magnetic-bearing flywheel rotor during spacecraft maneuvers. These studies have laid a technical foundation for the application of magnetically levitated flywheels in space. Further research will be conducted to advance experimental validation under representative space environment conditions.

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