A bearingless motor utilizing a permanent magnet free structure for disposable centrifugal blood pumps

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Abstract
There are a number of problems with disposable centrifugal blood pumps (DCBPs) that need to be addressed. First, there is the high cost of the disposable pump head due to the use of rare earth magnets; secondly, the large size of the reusable part due to the additional torque transmission structure; and thirdly, the complex shape of the secondary flow channel due to the space allocated to the magnetic bearing and the magnetic coupling. In this paper, we propose a bearingless motor utilizing a compact permanent magnet free structure, which can be applied to a DCBP and also satisfies the requirements of having a low cost disposable pump head and a smooth secondary flow channel. The radial motion and angular velocity of the rotor are actively controlled, and the axial and tilt motions are passively stabilized by a magnetic reluctance force. In a prototype bearingless motor, the permanent magnet free rotor could be levitated and rotated at angular velocities of up to 3400 rpm with a rotational positioning accuracy of less than ±27 μm which is sufficient for extracorporeal circulation support. The experimental results demonstrate the feasibility of using the proposed permanent magnet free bearingless motor in a DCBP.

Key words: Bearingless motor, Magnetic bearing, Permanent magnet free, Disposable centrifugal blood pump, Secondary flow channel

1. Introduction

To assist the blood circulation in patients who suffer from serious heart disease, various types of blood pumps have been developed and used for clinical support (Schmid, et al, 2004; Thalmann, et al, 2005; Kherani, et al, 2004). In the past decade, significant progress in the research and practical use of rotary blood pumps that generate continuous flow by rotating an impeller has been made (Takatani, 2006; Chan, et al, 2015; Hijikata, et al, 2011).

Up till now, disposable centrifugal blood pumps (DCBPs) with single-use pump heads that can be easily attached to and detached from a reusable unit, have been widely used for circulation support in cardiopulmonary bypass operations, extracorporeal membrane oxygenation, and emergency circulatory resuscitation (De Robertis, et al, 2006; Noon, et al, 1999; Mehlhorn, et al, 2005). DCBPs not only need to have particular pressure-flow characteristics, but also to be durable. Moreover, the destruction of blood cells (hemolysis) needs to be minimized and thrombus formation avoided. Therefore, frictional parts, such as ball and slide bearings must be eliminated. Furthermore, in DCBPs, a cost-effective disposable pump head is required, and the mechanical structure of the DCBP should be as compact as possible so that it can be used as a portable instrument outside hospital for emergencies.

Contactless support of the impeller by a magnetic bearing has been identified as a promising method to meet the requirements of high durability, low hemolysis and avoidance of thrombus formation (Hijikata, et al, 2008; Someya, et al, 2009; Nishinaka, et al, 2006). Our group has developed a DCBP employing a two-degrees-of-freedom magnetic bearing (Hijikata, et al, 2008). The radial motion of the impeller of this DCBP is actively controlled, and the axial and tilt motions are passively supported by the bias magnetic flux of the magnetic bearing. The impeller is driven by an
additional motor through a contactless torque transmission mechanism. Nevertheless, the shape of the secondary flow channel is very complicated due to the space allocated to the outer radial magnetic bearing and the inner contactless torque transmission mechanism as shown in Fig. 1. The complex shape of the secondary flow channel requires a small manufacturing tolerance and accurate fabrication process. Thus, simplifying the secondary flow channel by eliminating the inner torque transmission mechanism would alleviate these constraints.

A promising way of simplifying the secondary flow channel is to have a bearingless motor which combines a non-contact magnetic bearing and torque generation together in a single motor unit (Silber, et al, 2005; Asama, et al, 2012). A DCBP utilizing bearingless motor technology has been proposed (Schoeb and Barletta, 1997). Only the radial motion and angular velocity of the impeller of this DCBP are actively controlled. The shape of the secondary flow channel is simpler due to the reduced space allocated to the bearingless motor structure. However, a rare earth permanent magnet is used in the disposable pump head, which means the cost of this DCBP is high. A magnetically levitated stepping motor, which has a permanent magnet free rotor, has also been developed for industrial applications (Higuchi, 1990). However, the tilt and axial motions of the outer type rotor are actively controlled by an additional magnetic bearing, which leads to a complicated structure and complex control system.

In this paper, a novel mechanism for a permanent magnet free bearingless motor for potential applications in a DCBP is proposed. To completely eliminate rare earth permanent magnets from the disposable rotor, a stator consisting of separate cores of soft magnetic material, control coil windings for active positioning and rotational control, a support coil winding for passive levitation, and a rotor made of soft magnetic material surrounded by the stator were employed. Only the radial motion and angular velocity of the rotor are actively controlled. The bearingless motor was designed using magnetic field finite element method (FEM) analysis to achieve the target passive stiffness and torque. The magnetic suspension characteristics and rotation performance of the fabricated prototype bearingless motor were experimentally evaluated.
2. Basic mechanism of the bearingless motor and its rotation and levitation principles

2.1 Configuration of the proposed bearingless motor

Figure 2 shows the configuration of the proposed bearingless motor which is a hybrid of a two-degrees-of-freedom actively controlled and three-degrees-of-freedom passively supported magnetic bearing and a 3-phase 12/8 pole switched reluctance motor. The double flanged rotor is made of a soft magnetic material and has eight pairs of teeth. The stator core consists of twelve C-shaped cores made of a soft magnetic material placed around the rotor at even intervals. Wound around each core is a pair of square-shaped control coils placed symmetrically in the axial direction of the motor.

A control magnetic flux is generated between the C-shaped stator cores and the rotor by applying current to the square-shaped control coils in order to determine radial positioning and rotate the rotor. A large circular support coil in the stator cores, concentric with the rotor, is used to generate a bias magnetic flux between the C-shaped stator cores and the rotor by applying a constant current to it. This unique structure enables the proposed bearingless motor to be free of permanent magnets, to be compact and to have a two-degrees-of-freedom actively controlled noncontact support.

2.2 Principle of rotation

As shown in Fig. 3, the twelve separate stator cores are divided into four groups (Xp, Xn, Yp and Yn) for radial positioning control. Each group has a set of three phases, U, V and W, for rotation control. During rotation of the rotor, the current in each control coil is the sum of the positioning current and the motor current.

When group A of the rotor is aligned with the four cores of the V phase group, as shown in Fig. 3(a), the motor current is switched to the four pairs of coils in the W phase group to generate rotational torque in the clockwise direction. When group B of the rotor is aligned with the four cores of the W phase group, the rotor having moved 15 degrees clockwise, as shown in Fig. 3(b), the motor current is switched from the W to the V phase. When group A of the rotor is aligned with the four cores of the V phase group, another 15 degrees further clockwise, as shown in Fig. 3(c), the motor current is switched from the V to U phase. When group B is aligned with the four cores of the U phase group, yet another 15 degrees further clockwise, the motor current is switched from the U to the W phase. By repeating this phase switching process, the rotor will continuously rotate in the clockwise direction.

The motor currents in the four pairs of control coils that belong to the same phase are equal. Under ideal conditions, no radial force, only rotational torque, is ever generated by the motor current while the rotor is positioned at the center of the stator. This is due to the symmetrical arrangements of the stator cores for each phase and each group of teeth in the rotor.
2.3 Principles of radial force generation and passive stabilization

As shown in Fig. 4(a), the solid and dashed lines indicate the control and bias magnetic fluxes, respectively. When the rotor moves radially away from the center of the motor in the positive Y direction, the bias magnetic flux will be weakened by the magnetic flux generated by the three pairs of coils in the Yp group. On the opposite side, the bias magnetic flux will be strengthened by the magnetic flux generated by the three pairs of coils in the Yn group. This magnetic flux difference will generate a force in the negative Y direction. As a result, the rotor will be pulled back and kept in the radial center of the motor. In the X direction, the coils in groups Xp and Xn are used.

![Diagram of radial force generation and passive stabilization](image)

Fig. 4 Control principle of proposed bearingless motor. (a) Radial active control. (b) Axial passive control. (c) Tilt passive control.

As shown in Figs. 4(b) and (c), the axial and tilt motions of the rotor are passively stabilized by the reluctance force generated by the sum of the bias and control magnetic fluxes. The axial and tilt stiffness are enhanced during rotation of the rotor because the additional motor current used to rotate the rotor enhances the magnetic flux between the rotor and the stator.

3. Prototype design
3.1 Design target

Standard DCBPs are required to yield a flow rate of more than 5 L/min against a head pressure of more than 250 mm Hg (33.3 kPa) for extracorporeal circulation support with the use of an artificial lung (Hijikata, et al, 2010). Previous work in our laboratory on a magnetically-levitated DCBP with a 50 mm diameter impeller (Hijikata, et al, 2008), showed that an angular velocity of over 3000 rpm and a transmitted torque of over 0.069 Nm are necessary. In order to keep the same pump performance, we followed the design features of the previous research and employed a rotor with a diameter of 50 mm. The targets for the maximum angular velocity and the maximum transmitted torque were set to 3000 rpm and 0.069 Nm, respectively. In order to decrease the blood damage of the future DCBP, the secondary flow channel should be shortened. Thus, the maximum rotor height was set to 15 mm, following the previous design.

Sufficient passive stiffness in the axial and tilt directions can prevent contact between the impeller and the pump housing during levitation and rotation. Referring to a previously proposed bearingless motor for a blood pump (Asama, et al, 2009) which uses a permanent magnet in the rotor, the target passive stiffness in the axial and tilt directions were set to be 9.7 N/mm and 2.3 Nm/rad, respectively. As with the previous DCBP made in our laboratory (Hijikata, et al,
2008), the thickness of the plastic molding of the impeller, the thickness of the housing wall of the pump head and the fluid clearance between the impeller and the housing wall of the DCBP were set to 0.3 mm, 0.9 mm and 0.3 mm, respectively. Thus, the magnetic gap between the rotor and the stator cores was set to 1.5 mm.

In this paper, the proposed bearingless motor is for assessing the principle and evaluate the feasibility of application of DCBPs. Therefore, with regard to the pump size, we follow that of the previous design. Design optimization and miniaturization will be proposed in the future work. Thus, the target maximum height and diameter were set to 100 mm and 120 mm, respectively.

3.2 Simulation of the passive stiffness and torque generation

The passive stiffness of the proposed bearingless motor at an angular velocity of 0 rpm was evaluated utilizing a magnetic field simulator (Maxwell, ANSYS Inc., USA) and the model shown in Fig. 5. The passive stiffness was calculated from the axial force and restoring tilt torque generated over a small working range (0 – 0.3 mm for the axial stiffness and 0 mrad – 60 mrad for the tilt stiffness). Various bias currents were applied to the support coil and curves were fitted to the simulated data, as shown in Fig. 6. The axial and tilt stiffness are proportional to the square of the bias current due to the characteristics of the reluctance force. For the axial and tilt stiffness to meet the target requirements, the bias current has to be greater than 1100 A-turns. However, in order to suppress heat from being generated in the support coil, the bias current should be as small as possible. Thus, we decided to set the initial experimental bias current to 700 A-turns, so that the designed axial and tilt stiffness of the motor would be 3.50 N/mm and 0.87 Nm/rad, respectively.

![Fig. 5 Simulation model of the proposed bearingless motor for FEM analysis.](image)

![Fig. 6 Simulated passive stiffness of proposed bearingless motor using FEM analysis. (a) Axial stiffness with different bias currents. (b) Tilt stiffness with different bias currents.](image)
The simulated output torque of the motor fluctuates during rotation, as shown in Fig. 7(a). In this simulation, the switching of the motor current is ideal and eddy currents are ignored. As expected, the torque waveform is cyclic with the pattern repeated every 15 degrees, which is equal to the working cycle of each phase.

As shown in Fig. 7(b), the relationship between the average torque and the motor current is almost linear when the motor current is less than 400 A-turns. Deviation from this linearity occurs above 400 A-turns due to saturation of the stator and rotor cores. According to the simulation results, the design target for the average torque of 0.069 Nm can be met when the motor current is 400 A-turns with a bias current of 700 A-turns.

3.3 Control algorithm for radial positioning and angular velocity

Usually, in order to radially position the rotor in one direction, the bias magnetic flux on one side of the rotor is weakened by subtracting the positioning current from the bias current and that on the other side is strengthened by adding the positioning current to the bias current (Hijikata, et al, 2008). This positioning method, which is called the push-pull method, has some advantages in that it is highly robust and the control force over a small working range is linear. The motor proposed here utilizes this push-pull method to position the rotor.

The proposed bearingless motor is similar to an ordinary three-phase 12/8 pole switched reluctance motor. The working phase is determined by the rotational direction and angular position of the rotor. The angular velocity is controlled by changing the control voltage of the working phase.

4. Fabrication of prototype
4.1 Prototype bearingless motor for a DCBP
4.1.1 Mechanical structure of the prototype

As shown in Figs. 8 and 9, the origin of the coordinate system corresponds to the geometric center of the stator. The height of the rotor is 15 mm. Due to elimination of the contactless torque transmission mechanism, the height of the motor is 57 mm which is significantly smaller than the height of previous DCBP made in our laboratory (Hijikata, et all, 2008). The diameter of the motor is 123 mm which is approximately the same with the previous one. The mass of the rotor, which is made of soft iron, is calculated to be 0.074 kg based on the precise dimensions of the rotor. The magnetic gap between the rotor and the stator cores is 1.5 mm. In order to decrease the eddy current losses, laminated steel with a thickness of 0.35 mm is used to fabricate the main core of the C-shaped stator. The stator tip of the
C-shaped stator is made of soft iron. The numbers of turns in the control and support coils are 75 and 196, respectively, and the diameters of the wires in the control and support coils are 0.6 mm and 0.95 mm, respectively. In order to obtain a radial air gap of 0.3 mm in the prototype, a poly carbonate spacer with a thickness of 1 mm is placed between the rotor and the stator. There is an air clearance of 0.2 mm between the outer surface of the spacer and the stator tip in order to facilitate assembly and disassembly of the spacer.

4.1.2 Radial displacement measurements

Three eddy current displacement sensors (PU-05A, AEC Corp., Japan) are placed on the inner side of the rotor ring in order to detect the radial motion of the rotor. The arrangement using three sensors, as shown in Fig. 8, can effectively compensate the temperature drift and reject the common mode noise. The displacement of the rotor in the X and Y directions, x and y, are calculated using Eqs. (1) and (2), respectively.

\[
x = \frac{(V_{S1} - V_{S2})}{\sqrt{2}}
\]

\[
y = \frac{(V_{S2} - V_{S3})}{\sqrt{2}}
\]

Where \(V_{S1}, V_{S2}\) and \(V_{S3}\) are the output signals from displacement sensors S1, S2 and S3, respectively. For the future DCBP, the displacement sensors will be placed on the outer side of the rotor in order to achieve a smooth secondary flow channel.

![Fig. 8 Mechanical structure.](image1)

![Fig. 9 Bearingless motor prototype.](image2)

![Fig. 10 Arrangement of the displacement sensors for measurement of the rotor motion in axial and tilt directions](image3)
4.1.3 Axial and tilt displacement measurements

In order to obtain the tilt and axial motions of the rotor, three fiber displacement sensors (RC20, PHILTEC Corp., USA) arranged as shown in Fig. 10 are used to detect the movement of the upper surface of the rotor. The tilt motions, \( \theta_x \) and \( \theta_y \), and the axial motion, \( z \), are calculated from Eqs. (3)-(5).

\[
\begin{align*}
    z &= (V_{SA} + V_{SC})/2 \\
    \theta_x &= \tan^{-1}[(V_{SA} - V_{SC})/2L] \\
    \theta_y &= \tan^{-1}[(V_{SB} - z)/L]
\end{align*}
\]

Where \( V_{SA} \), \( V_{SB} \) and \( V_{SC} \) are the output signals from displacement sensors SA, SB and SC, respectively.

4.1.4 Angular velocity measurements

Hall sensors are used to detect the angular position of the rotor. As shown in Fig. 11(a), when an object made of magnetic material moves above a permanent magnet, the magnetic flux passing through the Hall sensor increases. As a result, the output of the Hall sensor increases. In this way, the object can be detected. As shown in Figs. 8 and 11(b), three Hall sensors (HQ-0811, AKM Corp., Japan) are fixed on a sensor board placed beneath the spacer. The three Hall sensors are each placed in line with the centers of the stator cores. Small NdFeB magnets (2x2x2 mm, Magfine Corp., Japan) are placed directly under each Hall sensor.

![Fig. 11 Angular position detection of rotor. (a) Principle of magnetic material detection. (b) Sensor arrangement.](image)

Fig. 12 Hall sensor U signal binarization process. (a) Original signal after LPF. (b) Binarized signal.
The angular position of the rotor can be determined from the output of the Hall sensors after passing through a low pass filter with a cut-off frequency of 1 kHz, as shown in Fig. 12(a). The three phase input voltages for U, V and W are varied based on the phase changing table shown in Table 1 and the binarized output signals from the Hall sensors shown in Fig. 12. Furthermore, the binarized signal from the Hall sensor at U is also used to calculate the angular velocity. When the rotor tooth starts to pass above the Hall sensor, a step change occurs in the binarized signal of the Hall sensor as shown in Fig. 12. Since the rotor has eight slots, the angular velocity of the rotor, \( \omega \), can be calculated by capturing the sampling time, \( \Delta t \), between two adjacent step changes as follows,

\[
\omega = \frac{\pi}{4\Delta t}
\]  

**Table 1**  

| Clockwise (1) | (2) | (3) |
|--------------|-----|-----|
| Hall sensor U | 0/1 | 1   | 0   |
| Hall sensor V | 0   | 0/1 | 1   |
| Hall sensor W | 1   | 0   | 0/1 |
| Phase U      | 1   | 0   | 0   |
| Phase V      | 0   | 1   | 0   |
| Phase W      | 0   | 0   | 1   |
| Mechanical angle | 0-15° | 15°-30° | 30°-45° |

Ideally, when the rotor slot passes above the Hall sensor, the magnetic path between the rotor and stator and the magnetic path between the small permanent magnet and the rotor are separated. Thus, interference between the variation in the current in the control coils and the magnetic flux which passes through the Hall sensor can be neglected. Nevertheless, when the rotor tooth passes above the Hall sensor, there is some interaction between these two magnetic paths. This interference will be tested experimentally.

![Fig. 13 Signal flow of thirteen electromagnets. (a) Signal flow and state variable. (b) Alignment of EMs.](image)

**4.1.5 Power amplifier**

Thirteen linear operational amplifiers (PA04, Apex Microtechnology, Inc., USA) are used to power the twelve pairs of control coils and the single support coil. Each operational amplifier works with a current sensor (LA 25-NP, LEM Corp., Switzerland) in order to measure the current.
4.2 Control system

4.2.1 Signal flow of the electromagnets

As mentioned before, the axial and tilt motions of the motor are passively stabilized by the reluctance force generated by the bias magnetic flux. Only the radial motion needs to be actively controlled due to the negative stiffness in the radial direction.

Figure 13(a) shows the signal flow and state variables of the twelve control electromagnets and the support electromagnet. All the electromagnets are controlled separately and the twelve control electromagnets are aligned as shown in Fig. 13(b). The interference between the radial motion and rotation and the gyro effect are not taken into consideration. The control signals for the positioning control voltages, $V_x$ and $V_y$, and the motor control voltage, $V_m$, are calculated separately and then summed before being inputted into the power amplifier. In each radial direction, the positioning control voltage is applied to the positive side. On the opposite side the polarity of the control voltage is reversed. A bias magnetic flux, $\phi_b$, is generated by applying a bias voltage, $V_b$, to the support electromagnet. $K_s$ is the current-flux coefficient. $C_x$, $C_y$ and $M$ are the damping coefficients in the radial directions and the mass of the rotor, respectively. $C_\theta$ and $J$ are the damping coefficient for rotation and the moment inertia of the rotor about the Z axis, respectively. The radial forces, $F_x$ and $F_y$, and the rotational torque, $T_\theta$, are influenced by the bias magnetic flux, $\phi_b$, the angular position of the rotor, $\theta_z$, the radial position of the rotor, $x$ and $y$, and the back electro-motive force (EMF) due to the radial velocities, $\dot{x}$ and $\dot{y}$, and the angular velocity, $\dot{\theta}_z$. The bias magnetic flux is influenced by the back EMF induced by the control magnetic fluxes, $\phi_i$ and $\phi_j$.

4.2.2 Controllers

In designing the positioning controller, the back EMF due to the radial and angular velocities is neglected. The nonlinear properties of the electromagnets such as the relationships between the current and the force and the gap, and the saturation of the magnetic material are not taken into consideration. For simplification, in modeling the system, the bias magnetic flux is assumed to be constant. Thus, the motion of the rotor in the X direction can be described as in Fig. 14(a). The negative stiffness, $K_{xx}$, and the current-force coefficient, $K_{ix}$, are set to constant values. The motion of the rotor in the Y direction is similar to that in the X direction. $R_s$ and $L_s$ are the average resistance and inductance of the pairs of control coils in the X direction, respectively. Based on the modeling of the rotor, a pole-placement controller for radial positioning is used to set all the minor-loop poles of the positioning system in one pre-determined location in the s-plane to achieve the desired characteristics of the response of the positioning system. In order to suppress the vibration amplitude and eccentricity of the rotor which causes hemolysis and thrombus formation in DCBP, a PI controller is used. As shown in Fig. 14(b), to achieve a stable angular velocity, a PI controller is also used to control the angular velocity. In order to suppress the fluctuations in the bias current through the support coil due to back EMF, current feedback control with a PI controller is used as shown in Fig. 15.
The negative stiffness, \( K_{nx} \), and the current-force coefficient, \( K_{ix} \), are obtained by simulation with a bias current of 700 A-turns at an angular velocity of 0 rpm. The mass of the rotor, \( M \), the average resistance, \( R_c \), and the inductance, \( L_c \), of the pairs of control coils in the X direction are measured by experiment. These parameters are shown in Table 2.

### Table 2 Parameters of rotor dynamics in the X direction

| Symbol | Description          | Value       |
|--------|----------------------|-------------|
| \( K_{ix} \) | Current-force coefficient | 5.5 N/A     |
| \( K_{nx} \) | Negative stiffness    | -15.36 N/mm |
| \( R_c \)    | Average resistance    | 0.60 Ω      |
| \( L_c \)    | Average inductance    | 0.0023 H    |
| \( C_x \)    | Damping coefficient   | 0 Ns/m      |
| \( M \)     | Mass of rotor         | 0.074 kg    |

### 5. Evaluation

#### 5.1 Magnetic levitation

The positioning accuracy and passive stiffness of the rotor were experimentally evaluated in air at an angular velocity of 0 rpm. All of the minor-loop poles of the positioning system are set to -500 rad/s. By tuning the position control parameters experimentally (\( K_{Px} = 0.075 \), \( K_{Ix} = 37 \); \( K_{Py} = 0.075 \), \( K_{Iy} = 37 \); \( K_{Pb} = 26.432 \), \( K_{Ib} = 2607.5 \)), the rotor can be stably levitated without physical contact.

![Fig. 16 Levitation performance without rotation. (a) Radial vibration. (b) Axial vibration. (c) Tilt vibration.](image)

![Fig. 17 Natural frequency measurement in the Z direction. (a) Axial vibration. (b) Resonant frequency.](image)

To obtain the initial experimental bias current of 700 A-turns in the 196 turns of the support coil, the bias current in the support coil was set to 3.6 A. As shown in Fig. 16(a), the positioning accuracies of the rotor in the X and Y directions during magnetic levitation are ±2.9 μm and ±3.1 μm, respectively, which are equivalent to the noise levels in the eddy current displacement sensors. As shown in Fig. 16(b) and (c), the axial and tilt vibrations about the X and Y axes are ±0.7 μm, ±0.04 mrad and ±0.05 mrad, respectively, which are equivalent to the noise levels in the fiber
displacement sensors.

The resonant frequencies were measured using the measured impulse responses and the results of fast Fourier transforms. Fig. 17 shows these results in the Z direction. Utilizing the measured resonant frequencies, the passive axial stiffness \( K_z \) and tilt stiffness, \( K_{\theta x} \) and \( K_{\theta y} \), of the motor were calculated using Eqs. (7), (8) and (9), respectively.

\[
\begin{align*}
    f_z &= \sqrt{\frac{K_z}{m}} \frac{1}{2\pi} \\
    f_{\theta x} &= \sqrt{\frac{K_{\theta x}}{J_{\theta x}}} \frac{1}{2\pi} \\
    f_{\theta y} &= \sqrt{\frac{K_{\theta y}}{J_{\theta y}}} \frac{1}{2\pi}
\end{align*}
\]  

(7)  

(8)  

(9)

Where \( f_z \), \( f_{\theta x} \) and \( f_{\theta y} \) are the natural frequencies in the axial and tilt directions, respectively. The rotor mass \( m \) is 0.074 kg. The moment of inertia of the rotor about the X axis, \( J_{\theta x} \), and also that about the Y axis, \( J_{\theta y} \), is 1.82 kg.mm\(^2\), calculated by CAD software (SolidWorks 2013, Dassult Systems SolidWorks Corp.).

Table 3 summarizes the measured and simulated passive stiffness. The experimental results are almost the same as the simulated ones. By changing the bias current, the simulated and measured axial and tilt stiffness increase in proportion with the square of the bias current as shown in Fig.18.

5.2 Rotation performance

By tuning the rotational control parameters appropriately (\( K_{\theta x}=0.003, K_{\theta y}=0.760 \)), the rotor can be rotated at a reference speed. The maximum angular velocity, positioning accuracy and power consumption of the prototype bearingless motor were measured and evaluated when the rotor was levitated in air with a bias current of 3.6 A and rotated without a load. The power consumption of the motor was calculated using the measured current and voltage of each coil at a constant angular velocity.

![Fig. 18 Static stiffness comparison with different bias current. (a) Axial stiffness with different bias currents. (b) Tilt stiffness with different bias currents.](image-url)
As shown in Fig. 19, the maximum vibration amplitude of the rotor in the radial direction during rotation is less than ±27 μm which is sufficiently small compared with the radial mechanical clearance of 300 μm between the rotor and the housing surface of the DCBP. The maximum vibration amplitudes in the axial and tilt directions are less than ±127 μm and ±10 mrad, respectively. The axial mechanical clearance of 1 mm between the rotor and the bottom of the housing of the DCBP and the maximum tilt tolerance of 43 mrad are each sufficient for the vibration amplitudes in the axial and tilt directions, respectively. There is a sharp peak at 250 rpm in both the axial and tilt directions. As shown in Fig. 20, the axial vibration frequency of the rotor at 250 rpm is around 34 Hz which is close to the resonant frequency in the axial direction as shown in Table 3. This resonance frequency is about eighth times the rotational frequency (4.166 Hz) and arises because the rotor has eight teeth and there are eight working cycles in one revolution of the rotor. For the same reason there is also a peak at 250 rpm in the tilt direction.

As shown in Fig. 21, the maximum angular velocity of the rotor is 3400 rpm, which is limited by saturation of the power amplifier. For safety, the maximum output voltage of the power amplifiers supplying current to the coils was set to 2.5 V. The total power consumption of the proposed bearingless motor at the maximum speed is 27.7 W without a load. Over 58% of the power is consumed by the support coil due to the high applied bias current needed to generate sufficient bias magnetic flux.

6. Discussion

The large power consumption of the support coil will cause thermal dissipation problems and shorten the battery life for future DCBPs when it is used as a portable device, such as for emergency treatment in an ambulance. Nevertheless, the axial stiffness and tilt stiffness are enhanced during rotation of the rotor because the motor current to rotate the rotor increases the magnetic coupling between the rotor and the stator. The dynamic passive stiffness in the axial and tilt directions was measured when the rotor was levitated at different angular velocities with a bias current of 3.6 A. The experimental setup and data processing method were the same as those for measurement of the static passive stiffness. A puff from a bottle of compressed air was utilized to apply an impulse force to the rotor during rotation.

As shown in Fig. 22(a), the axial dynamic passive stiffness increases by 23% at an angular velocity of 3000 rpm.
During rotation, the natural frequency of the rotor in the tilt directions separates into forward and backward frequencies, $f_f$ and $f_b$, due to the gyroscopic effect (Muszynska, 2005). Normally, the average tilt mode frequencies, $f_f$ and $f_b$, remain constant. Nevertheless, an increase in the average frequency was observed due to the motor current which enhances the passive stiffness in the tilt directions, as shown in Fig. 22(b). This increase in the passive stiffness of the rotor during rotation provides a promising way to decrease the bias current in the support coil.

![Fig. 22 Dynamic stiffness and natural frequencies of tilt mode at different angular velocity. (a) Axial dynamic passive stiffness. (b) Natural frequency separation in tilt direction.](image)

Additionally, in a DCBP the pump head is filled with blood, which can provide an additional damping force to the rotor. Consequently, the bias current can be decreased and hence, the power consumption of the support coil can be decreased.

Another reason for the high power consumption of the proposed motor is the low motor efficiency at high speed. As shown in Fig. 21, the control power consumption increases rapidly when the angular velocity is greater than 2000 rpm due to eddy current losses caused by high speed phase commutation and the error in measuring the angular position using the Hall sensors. The stator tips of the C-shaped stator core and the rotor body, which are made of soft iron, make the main contribution to the eddy current losses. Utilizing a laminated steel or powder core for the stator tips and the rotor body is a promising way to reduce these losses.

During rotation of the rotor, when a rotor tooth passes over a Hall sensor, the control current disturbs the magnetic flux passing through the sensor. Additionally, when the axial position at which the rotor is balanced, which is determined by the weight of the rotor and the axial pull generated by the bias flux, changes due to a change in the control current, the gap between the bottom surface of rotor and the Hall sensors changes and this also disturbs the magnetic flux passing through the Hall sensors. As shown in Fig. 23, when the rotor tooth and the slot pass above a Hall sensor at 3000 rpm, the output of the sensor increases by 12% and 3% compared with those at 1000 rpm, respectively. This variation in the Hall sensor output leads to errors in the angular position resulting in improper phase commutation and a decrease in motor efficiency. A different angular measurement method that improves the robustness to interference by external magnetic fields needs to be considered in future work.

![Fig. 23 Hall sensor U output under different angular velocity.](image)
7. Conclusion

A bearingless motor for a DCBP with a compact permanent magnet free structure was designed and fabricated. The positioning and rotation performance of the motor were evaluated. The rotor can be levitated and rotated at a maximum angular velocity of up to 3400 rpm with a positioning error of less than ±27 µm. The power consumption of the proposed bearingless motor is 27.7 W without a load at the maximum angular velocity. Experimental measurements of the static and dynamic passive stiffness of the fabricated prototype were made.

Future work is to reduce the bias current in the support coil, improve the motor efficiency, miniaturize the design and fabricate a DCBP using this proposed bearingless motor.

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