Flux-Modulating Hybrid-Field Consequent Pole Motor

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In this paper, a new type of axial gap motor, named the “flux-modulating hybrid-field consequent pole motor (FHCM)”, is proposed. To enable flux modulation, the FHCM comprises rotor iron pieces sandwiched between two stators, the armature, and field poles. The field poles comprise permanent magnet poles, iron poles, and ring field windings. The magnetic flux of the field poles can be adjusted by varying the field current. The electromagnetic performance of the FHCM, including the air-gap magnetic flux density, back-electromotive force, torque, and torque ripple, was theoretically analyzed, and the results were verified through three-dimensional finite element analysis and experiments. By utilizing the inner space of the machine, the proposed FHCM could achieve a higher torque density than that of the conventional flux-modulating hybrid field motor.

Keywords: AC motors, axial gap motors, synchronous motors, consequent pole, flux modulation

1. Introduction

In Japan and around the world, active research in recent years has addressed hybrid excitation motors (HEMs) whose field source consists of permanent magnets (PMs) and wound-fields (1)–(3). HEMs are motors with intermediate characteristics between a PM motor and a field winding motor, and applications of them are expected when reduced PM use and field weakening are needed (4) (5). HEM composition is mainly classified into the following three types based on the arrangement of field sources (3): 1) composition with a PM and field winding in the rotor; 2) composition with a PM in the rotor and field winding in the stator; and 3) composition with a PM and field winding in the stator. Item 1) requires brushes and slip rings to supply DC current to the field winding, which suffers from issues of durability. Therefore, items 2) and 3) are advantageous in terms of field source composition. In particular, since PM and field winding are both placed in the stator for item 3), cooling is simplified, and the temperature can be measured directly and accurately, allowing for monitoring and driving until the PM is demagnetized.

We previously proposed an axial gap motor (6) (7) as a new motor type classified under item 3), called a flux-modulating hybrid motor (FHM), and experimentally clarified its basic characteristics by three-dimensional finite element analysis (3-D FEA). Fig. 1 shows the FHM structure. This FHM uses a flux modulation principle (8)–(14) to place in the stator both the PM and field winding, both of which serve as field sources. The armature and the field pole are arranged as separate stators at both ends in the direction of the rotation axis, and an axial gap structure is adopted that sandwiches the rotor between both stators; this structure simplifies holding the rotor iron pieces compared to the radial gap structure in Refs. (12)–(14).

One issue with FHM is that the rotating shaft’s inner diameter region is ineffectively used as a magnetic circuit; its torque density can be improved. Therefore, this paper proposes a flux-modulating hybrid-field consequent pole motor (FHCM) (15) (16) that is arranged as a ring-shaped field winding on the motor’s inner diameter side to improve the FHCM’s torque density. First, the FHCM’s structure and operating principle are shown, followed by theoretical discussions of its electromagnetic characteristics: magnetic flux density, back electromotive force, torque, and torque ripple. The electromagnetic characteristics are also evaluated with an experi-
mental machine, and the validity of the evaluation results is verified from three-dimensional finite element analysis and theoretical discussions. Furthermore, we clarified the torque improvement effects of the FHCM by comparing back electromotive force and torque with an FHM of the same body shape.

2. FHCM Structure and Operating Principle

2.1 Structure Fig. 2 shows the proposed FHCM structure. The FHCM consists of two stators and one rotor. Of the two stators, the stator armature has teeth wound with the armature winding $W_a$ and field winding $W_f$ in a ring shape on the inner diameter side in the teeth’s circumferential direction. The stator field pole has polar teeth arranged circumferentially consisting of a consequent pole by arranging an iron pole and every other PM pole magnetized circumferentially in the same direction. As with the stator armature, a $W_f$ was formed in a ring shape on the inner diameter side of the polar teeth. In the rotor, iron pieces for flux modulation are embedded in a retaining disk created from non-magnetic material in the circumferential direction. The retaining disk and magnetic shaft are also connected. Such a configuration allows for the field winding to be arranged on the motor’s inner diameter side, which does not greatly contribute to the torque. Therefore, effective use of the motor volume is expected to improve the torque density.

2.2 Operating Principle

2.2.1 Field magnetic flux adjustment principle due to field current Fig. 1 shows with a dashed line the flow of field magnetic flux by field current $I_f$ supplied to field winding $W_f$ for the FHM. In the FHM, field magnetic flux is generated by a $W_f$ wound around each iron pole, which then interlinks with the armature teeth through the gap and the rotor iron piece. The flow returns to the iron pole next to the circumferential direction through the armature coreback and the armature teeth, gap, and rotor piece next to the circumferential direction. Fig. 2 shows with a dashed line the flow of the field magnetic flux due to the $I_f$ supplied by $W_f$ for the proposed FHCM. In the FHCM, field magnetic flux flowing in the shaft in the rotational axis direction is generated by the $W_f$ wound in a ring-shaped manner. It then passes through the armature coreback in the radial direction and interlinks with the iron pole of the field pole through the armature teeth, gap, and rotor iron piece. The flow then returns to the shaft through the field pole coreback. Compared to the FHM, FHCM’s composition has the following characteristics:

- The area on the inner diameter side of the stator, which makes a small contribution to torque, can be used as a $W_f$ area.
- The shaft on the inner diameter side of the stator, which makes a small contribution to torque, can be used as a magnetic path for the field magnetic flux.

The field magnetic flux increases if $I_f$ is passed so that the iron pole has opposite polarity to the PM pole ($S$ pole in Fig. 2) and decreases if $I_f$ is passed so that the iron pole has the same polarity as the PM pole ($N$ pole in Fig. 2). The field magnetic flux can be adjusted in this way by changing the value of $I_f$, and the electromotive force induced by armature winding $W_a$ also changes.

2.2.2 Torque generation principle due to flux modulation In this section, the principle by which torque is generated by flux modulation is shown. The generation of torque requires a configuration where the relationship of $p_r = p_n + p_f^{(2n+1)}$ is established, where $2p_n$ is the number of poles of armature winding $W_a$, $2p_f$ is the number of field poles, and $p_r$ is the number of rotor iron pieces. Fig. 2 shows an example where $2p_n = 8$, $2p_f = 12$, and $p_r = 10$. Focusing on the field pole side, the field pole has a static magnetic field of $2p_f = 12$ poles. Additionally, focusing on the armature side, the 12-pole static magnetic field of the field pole experiences flux modulation with rotor iron piece $p_r = 10$ and becomes an 8-pole ($2p_f - 2p_r = 20 - 12$) rotating magnetic field. This is theoretically shown below by Eq. (7) in Section 3.1.1. This rotating magnetic field changes in a sinusoidal shape with a mechanical angle of $360°/p_r = 36°$ as one period. As shown by the FHCM 3 model linear development view (a) in Fig. 3, when the rotor position is set at a mechanical angle of $0°$ where the magnetic flux interlinking the A-phase winding is at a positive maximum, then the magnetic flux interlinking the A-phase winding is at a negative maximum at a mechanical angle of $18°$ (Fig. 3(b)). As a result, the magnetic flux changes with a mechanical angle of $36°$ as one period. In other words, this results in a rotating magnetic field with a space order of $2p_n = 8$ poles and a time order of $2p_r = 20$ poles for $2p_f = 12$ poles. By setting the current that energizes armature winding $W_a$ to a period corresponding to 20 poles, the magnetic field due to the field matches the space order ($p_n = 4$th order) and the time order ($p_r = 10$th order), and a synchronous torque is generated. Simultaneously, a
rotating magnetic field of \(2p_a = 8\) poles is modulated where armature winding \(W_a\) is the magnetomotive force due to rotor iron piece \(p_r = 10\), and a static magnetic field of 12 poles \((2p_r - 2p_a = 20 - 8)\) is formed on the field pole side in Fig. 3. Theoretically, this is shown below by Eq. (4) in Section 3.1. This static magnetic field has a space order of \(p_f = 6\) and a time order of 0 (static magnetic field), and an electromagnetic force is generated between the magnetomotive force of the field pole with the same space and time orders, generating a synchronous torque. In other words, the FHCM in Fig. 2 uses both sides of the rotor to generate torque and operates as a 20-pole synchronous motor.

3. Theoretical Discussion of Electromagnetic Characteristics

3.1 Gap Magnetic Flux Density

We discuss the gap on the field pole side in Fig. 2. The field pole has a PM and field winding \(W_f\) that together form a static magnetic field where the number of poles \(2p_f = 12\) is the fundamental wave. The consequent field pole causes the harmonics creating multiple numbers of poles \(2p_f = 12\). The magnetomotive force of the field pole, which is the sum of magnetomotive force \(F_m\) due to the PM and magnetomotive force \(F_f\) due to \(W_f\), is expressed by the following equation:

\[
F_m + F_f = \sum_{\alpha_m=1}^{\infty} F_{\alpha_m} \cos(6\alpha_m\theta) + \sum_{\alpha_f=1}^{\infty} F_{\alpha_f} \cos(6\alpha_f\theta)
\]

(1)

Here \(\alpha_m\) and \(\alpha_f\) are the orders of the spatial harmonics, both of which are natural numbers. \(\theta\) is the mechanical angle. Henceforth, the orders of the spatial harmonics shown in this paper are the orders at a mechanical angle of 360°. Focusing on the rotor in Fig. 2, the permeance \(\Lambda_r\) per unit area of the iron piece is the permeance variation where the number of poles corresponds to \(p_r = 10\). When the harmonic components are included, permeance variation can be expressed:

\[
\Lambda_r = \sum_{\alpha_r=0}^{\infty} \Lambda_{\alpha_r} \cos(10\alpha_r \theta - \alpha_r \omega t)
\]

(2)

Here \(\alpha_r\) is the order of the spatial harmonics of the permeance variation of the rotor and an integer greater than or equal to zero. The order of the time harmonics of the permeance variation of the rotor is identical to the order of the spatial harmonics. The order of the time harmonics in this paper sets the time order of the 20-pole rotating magnetic field to 1. Magnetic flux density \(B_{m,f}(\theta, t)\) due to the magnetomotive force of the field pole in Fig. 2 is the product of magnetomotive forces \(F_m + F_f\) and \(\Lambda_r\), and so the following equation is obtained from Eqs. (1) and (2) as well as the product-to-sum equations of the trigonometric functions:

\[
B_{m,f}(\theta, t) = \sum_{\alpha_m=1}^{\infty} \sum_{\alpha_r=0}^{\infty} B_{\alpha_m,\alpha_r} \cos(6\alpha_m \pm 10\alpha_r \theta - \alpha_r \omega t)
\]

(3)

In this paper, space order \(\kappa_m,f\) and time order \(\nu_{m,f}\) in Eq. (3) are expressed:

\[
\kappa_{m,f}, \nu_{m,f} = \pm \left[ \frac{6\alpha_m \pm 10\alpha_r}{\alpha_r} \right] \pm \left[ \frac{6\alpha_f \pm 10\alpha_r}{\alpha_r} \right]
\]

(4)

Focusing on the gap on the armature side, the armature forms a rotating magnetic field with poles \(2p_a = 8\) as the fundamental wave with armature winding \(W_a\). Armature-based magnetomotive force \(F_a\) (which includes harmonics) is expressed as follows:

\[
F_a = \sum_{\alpha_a=1}^{\infty} \sum_{\gamma_a=0}^{\infty} F_{\alpha_a,\gamma_a} \cos(4\alpha_a \theta - \gamma_a \omega t - \xi_{\alpha_a,\gamma_a})
\]

(5)

Here \(\alpha_a\) is the space order of the magnetomotive force harmonics, and \(\gamma_a\) is the time order corresponding to each magnetomotive force harmonic. \(\omega\) is the rotation angular frequency, \(t\) is the time, and \(\xi_{\alpha_a,\gamma_a}\) is the phase angle. Slot harmonics are caused by the teeth’s permeance pulsation in the gap on the armature side, but they are omitted from this paper. Since magnetic flux density \(B_a(\theta, t)\) due to the armature magnetomotive force is the product of magnetomotive force \(F_a\) and permeance \(\Lambda_r\), the following equation is obtained from Eqs. (2) and (5):

\[
B_a(\theta, t) = \sum_{\alpha_a=1}^{\infty} \sum_{\gamma_a=0}^{\infty} B_{\alpha_a,\gamma_a} \cos((4\alpha_a \pm 10\alpha_r) \theta - (\gamma_a \pm \alpha_r) \omega t)
\]

(6)

Therefore, space order \(\kappa_a\) and time order \(\nu_a\) in Eq. (6) are expressed:

\[
\kappa_a = \pm \left[ \frac{4\alpha_a \pm 10\alpha_r}{\alpha_r} \right]
\]

\[
\nu_a = \pm \left[ \frac{\gamma_a \pm \alpha_r}{\alpha_r} \right]
\]

(7)

In Section 4.2 below, Eqs. (4) and (7) show that the theoretical values for the space and time orders in the gap magnetic flux density are equivalent to the respective orders of the analysis results by FEA.

3.2 Back Electromotive Force

We examined the back electromotive force induced by armature winding \(W_a\). Back electromotive force \(e_b(t)\) is expressed using \(B_a(\theta, t)\):

\[
e_b(t) = -n \int S B_a(\theta, t) dS\]

\[
= \alpha_r \sum_{\alpha_a=1}^{\infty} \sum_{\gamma_a=1}^{\infty} E_{\alpha_a,\gamma_a} \cos(\alpha_a \omega t)
\]

\[
+ \alpha_r \sum_{\alpha_f=1}^{\infty} \sum_{\gamma_f=0}^{\infty} E_{\alpha_f,\gamma_f} \cos(\alpha_f \omega t)
\]

\[
+ (\gamma_a \pm \alpha_r) \sum_{\alpha_a=1}^{\infty} \sum_{\gamma_a=0}^{\infty} E_{\alpha_a,\gamma_a} \cos((\gamma_a \pm \alpha_r) \omega t)
\]

(8)

Here \(n\) is the number of windings of \(W_a\), and \(S\) is the interlinkage flux of the magnetic field. From Eq. (8), the time order of the back electromotive force is \(\gamma_a \pm \alpha_r\). In Section 4.2 below, Eq. (8) is used to analyze the space and time orders in the gap magnetic flux density that contributes to the time order of the back electromotive force.
3.3 Torque and Torque Ripple

The effects of the spatial and time harmonics of the gap magnetic flux density on the field pole side and the armature side on the torque and torque ripple are discussed. Since electromotive force $e_\alpha(t)$ at any given point is generally proportional to the square of the magnetic flux density, it is expressed:

$$e_\alpha(t) = \frac{B^2(t, \theta)}{2\mu_0}. \tag{9}$$

Here $\mu_0$ is the vacuum permeability. Magnetic flux density $B(t, \theta)$ is a series of trigonometric functions from Eqs. (3) and (6), and so the space and time orders of $\sigma$ are the sums of differences of each term in Eqs. (4) and (7). The torque is derived from $\sigma$ whose space and time orders are respectively $\kappa_\alpha = 0$ and $\nu_\alpha = 0$; $n^{th}$ torque ripple is derived from the $\sigma$ whose space and time orders of are respectively $\kappa_\alpha = 0$ and $\nu_\alpha = n$. For example, for the order of the magnetic flux density that contributes to torque generation in the gap on the armature side, when the double-sign in Eq. (3) is negative, $\alpha_m = 1$, $\gamma_\alpha = 1$, and when $\alpha_r = 1$, $\gamma_\alpha = 1$, and $\alpha_r = 0$ in Eq. (6), then the spatial harmonic order is $\kappa_\alpha = 4$, and the time harmonic order is $\nu_\alpha = 1$. The concepts presented in this section are applied to the analysis of the space and time orders of the gap magnetic flux density that contributes below to the torque ripple in Section 4.3.

4. Evaluation of Electromagnetic Characteristics by Prototype

4.1 Prototype and Its Evaluation Equipment

Fig. 4 shows photographs of a manufactured prototype, and Table 1 shows its specifications. The number of poles in the prototype is 20 poles, a continuous rated output power of 0.8 kW, and a base/maximum speed of 900/2700 min$^{-1}$. The number of armature poles $2p_\alpha$ is 8, the number of field poles $2p_f$ is 12, and the number of rotor iron pieces $p_r$ is 10. The prototype was air-cooled, and the maximum current densities of armature winding $W_a$ and field winding $W_f$ are set to 5 A/mm$^2$ in a continuous rating. The armature current vector magnitude at this time was $I_a = 13.9$ A, and the field current magnitude was $I_f = 4$ A. $W_a$ was arranged in a circumferential direction in the order of A, B, and C phases in a 2 : 3 series with 1.5 slots per magnetic pole. The teeth of the armature and field pole used electrical steel sheets (10JNEX900) and were laminated in a concentric manner with a rotational axis at the center. The coreback that connects the teeth of the armature and field pole was stacked parallel to the axis of rotation. The iron piece had a dust core (HB2). Reinforced resin (PPS) was used for the retaining disk of the iron piece, and both sides of the retaining disk were coated with an epoxy adhesive (Araldite) to ensure retention of the iron piece. A neodymium magnet (N40) with a residual magnetic flux density of 1.25 T was used for the PM. The relative angle $\gamma$ of the armature and the field pole was set to $\frac{\pi}{4}$ of the tooth interval to suppress the effects of magnetic saturation and d-q-axis interference due to current increases.

Fig. 5 shows the configuration of the evaluated prototype. It had an encoder attached to the shaft and was driven by current vector control with rotation speed $N$ and current phase angle $\beta$ as command values. A powder brake was used for the load, and the load torque was adjusted accordingly.

4.2 Back Electromotive Force

For the measurements of phase back electromotive force $e_\alpha$, the powder brake of the load in Fig. 5 was replaced with an induction motor, which rotated the prototype’s rotor. Fig. 6 shows the $e_\alpha$ (the measured value and the 3-D FEA result) of armature winding $W_a$ at rotation speed $N = 900$ min$^{-1}$ when field current $I_f = 4$ A, 0 A, and $-4$ A. Fig. 6(a) is the waveform of $e_\alpha$, and Fig. 6(b) is the Fourier spectrum of $e_\alpha$. Fig. 7 shows the amplitudes of $I_f$ on the horizontal axis and $e_\alpha$ on the vertical axis.

| Number of poles | 20   |
|-----------------|------|
| Rated output power [kW] | 0.8  |
| Base / Maximum speed [min$^{-1}$] | 900 / 2700 |
| Outer diameter (including the end windings) [mm] | 200  |
| Axial length [mm] | 75.8 |
| Air-gap length [mm] | 1.5 |
| Number of winding turns per phase for $W_a$ | 256  |
| Number of winding turns for $W_f$ | 225  |
| Maximum armature current density [A/mm$^2$] | 5.0  |
| Maximum field current density [A/mm$^2$] | 5.0  |
| Stator core material | 10JNEX900 |
| Rotor iron piece material | HB2   |
| Retaining disk material | PPS   |
| Magnet material | N40   |
axis (effective values). \( \epsilon_p \) can be adjusted when \( I_f \) is changed from a positive to a negative range. As shown in Fig. 6, good agreement is seen in Figs. 6(a) and (b) when comparing the measured EMF value with the 3-D FEA results. Fig. 7 shows the phase back-EMF versus \( I_f \). Fig. 8 shows the magnetic flux density contour diagram when \( I_f \) is changed to 0 A, 4 A, and \(-4\) A. The shaft’s magnetic saturation is most prominent when negative field current \( I_f \) is passed, probably because the magnetic flux of the magnet and the field magnetic flux flow in a direction where they strengthen each other on the shaft. The torque might also decrease due to the magnetic saturation when the current density of the field winding is increased.

Next, we analyzed the cause of the second-order time harmonic of the back electromotive force by FEA. Armature winding \( W_a \) in a 2 : 3 series (where the number of slots per magnetic pole is 1.5) has a space order of \( 3n \pm 1 \) times fundamental wave order \( p_a = 4 \) (17). In other words, space order \( \kappa_a \) and time order \( \nu_a \) of the magnetic flux density, which induces the back electromotive force of the second-order time order in \( W_a \), is \((\kappa_a, \nu_a) = (8, 2)\) from Eq. (8). In other words, this is when \( \alpha_a = 2 \), \( \alpha_r = 0 \), and \( \gamma_a = 1 \) in Eq. (7). Next, we examined the factors that cause the space and time orders of this magnetic flux density. Fig. 10 shows the spatial and time components of the gap magnetic flux density on the field pole side at field currents \( I_f = 4 A \), \( I_f = 0 A \), \( I_f = -4 A \) and at the open-winding time. The following are space order \( \kappa_{m,f} \) and time order \( \nu_{m,f} \) of the gap magnetic flux density on the field pole side in descending order:

\[
\begin{bmatrix}
\kappa_{m,f} \\
\nu_{m,f}
\end{bmatrix} =
\begin{bmatrix}
6 & 12 & 16 & 18 \\
0 & 0 & 1 & 2
\end{bmatrix}
\]

(11)

Each order in Eq. (11) is equivalent to the theoretical equation in Eq. (4).

Permeance space order \( \kappa_r \) and time order \( \nu_r \) of the iron piece are expressed by the following equation:

\[
\begin{bmatrix}
\kappa_r \\
\nu_r
\end{bmatrix} =
\begin{bmatrix}
10 & 20 & 30 \\
1 & 2 & 3
\end{bmatrix}
\]

(12)

From Eq. (4), the gap magnetic flux density on the field pole side is magnetically modulated by the permeance variation of the iron piece. The combination of low-order space and time orders, where space order \( \kappa_r \) and time order \( \nu_r \) of the magnetic flux density are \((\kappa_r, \nu_r) = (8, 2)\), are as follows from Eqs. (11) and (12):
Flux-Modulating Hybrid-Field Consequent Pole Motor (Hiroshi Mitsuda et al.)

Fig. 9. Magnetic flux density harmonics in air gap between stator armature and rotor

(a) $I_f = 4\,A$

(b) $I_f = 0\,A$

(c) $I_f = -4\,A$

Fig. 10. Magnetic flux density harmonics in air gap between stator field pole and rotor

(a) $I_f = 4\,A$

(b) $I_f = 0\,A$

(c) $I_f = -4\,A$

\[
\begin{pmatrix}
\kappa_d \\
\nu_d
\end{pmatrix} = \begin{pmatrix}
\kappa_r \\
\nu_r
\end{pmatrix} - \begin{pmatrix}
\kappa_{m,f} \\
\nu_{m,f}
\end{pmatrix} = \begin{pmatrix}
20 \\
12
\end{pmatrix} - \begin{pmatrix}
8 \\
2
\end{pmatrix} = \begin{pmatrix}
12 \\
10
\end{pmatrix} = \begin{pmatrix}
(6,0) \\
(8,2)
\end{pmatrix}.
\]

In other words, this result signifies that $(\kappa_{m,f}, \nu_{m,f}) = (12, 0)$, which is the spatial harmonic that is double the field pole magnetomotive force, and $(\kappa_r, \nu_r) = (20, 2)$, which is the permeance of the iron piece, are predominant in the second-order time harmonics of the back electromotive force. When focusing on $(\kappa_f, \nu_f) = (12, 0)$, which is the spatial harmonic that is double the field magnetic flux in Fig. 10, this result becomes smaller as $I_f$ increases. By increasing the field current results in the $N$ pole by PM and the $S$ pole formed by the field magnetic path that has a more similar value, the spatial harmonic is reduced, which is double the field magnetic flux. This result is correlated with the fact that the second-order component in Fig. 6 increases when the field current is set from $I_f = 4\,A$, $I_f = 0\,A$, and $I_f = -4\,A$.

4.3 Torque and Torque Ripple

Fig. 11 shows average torque $T_{ave}$ (the measured value and the 3-D FEA result) with respect to current phase angle $\beta$ for different values of field current $I_f$ when armature current vector magnitude $I_a$ is constant at 13.9 A and the rotation speed is $N = 900\,\text{min}^{-1}$.

Fig. 11 shows that $T_{ave}$ increases or decreases depending on $I_f$. The maximum torque is reached at $\beta = 0^\circ$ at any given $I_f$ value because FHCM uses the flux modulation principle,
Flux-Modulating Hybrid-Field Consequent Pole Motor  

Hiroshi Mitsuda et al.

Fig. 12. Torque ripple thereby generating hardly any reluctance torque (9)–(14). When comparing the 3-D FEA and measured values of $T_{\text{ave}}$, the measured value was slightly lower than the 3-D FEA result when q-axis current $i_q$ and $\beta$ are large and when $I_f = -4$ A; but both values overall had good agreement.

Fig. 12 shows the torque waveform and Fourier spectra at $i_q = 16$ A, $I_f = 4$ A, $I_f = 0$ A, $I_f = -4$ A, and $\beta = 0^\circ$. Focusing on the torque ripple order, the third order was the largest. Here the third-order torque ripple was electromagnetic force $\sigma$ of space order $\kappa_{\text{ripple}} = 0$ and time order $\nu_{\text{ripple}} = 3$ (Section 3.3), and the low-order space order and time order combination satisfies the following equation:

$$\begin{bmatrix} \kappa_{\text{ripple}} \\ \nu_{\text{ripple}} \end{bmatrix} = \begin{bmatrix} 0 \\ 3 \end{bmatrix} = \begin{bmatrix} 8 \\ -1 \end{bmatrix}$$

(14)

Here $(\kappa_s, \nu_s) = (8, -1)$ is a harmonic that is double the armature magnetomotive force. $(\kappa_r, \nu_r) = (20, 2)$ (Section 4.2), and the reduction of their harmonics probably effectively suppresses the torque ripple.

4.4 Comparison of FHM and FHCM Characteristics

Fig. 13 shows the changes in average torque $T_{\text{ave}}$ with respect to q-axis current $i_q$ for a conventional-structure FHM (6) (7) and when the rotation speed is set to $N = 900 \text{ min}^{-1}$, d-axis current $i_d = 0$ A, and field current $I_f = 4.0$ A. The size, gap, and current density of FHM and FHCM are identical. As seen in Fig. 13, the 3-D FEA and experimental results of $T_{\text{ave}}$ are in good agreement. When compared under identical current conditions, FHCM had a higher $T_{\text{ave}}$ than the FHM and improved the torque density by approximately 29.2% when the maximum current was applied.

Table 2 shows the FHCM and FHM loss and efficiency at maximum torque when the rotation speed was set to $N = 900 \text{ min}^{-1}$, d-axis current $i_d = 0$ A, q-axis current $i_q = 13.9$ A, and field current $I_f = 4.0$ A. The other losses in the 3-D FEA are the sum of the iron and mechanical losses calculated by the theoretical equation (10). Torque improvement was confirmed when comparing the FHCM and FHM output (Fig. 13). The decrease in the copper loss in the field winding was also confirmed because the field winding resistance was reduced by setting the field winding around the stator’s inner diameter side in the FHCM. Efficiency was raised by the above output improvement and loss reduction effects.

5. Conclusions

We proposed a flux-modulating hybrid-field consequent pole motor (FHCM) for increasing the torque of the FHM by utilizing its internal space. Our FHCM had a new structure in which the field pole was set as a consequent pole and ring-shaped field winding was placed on the inner diameter side that does not contribute to torque generation. We also evaluated the electromagnetic characteristics with a prototype, verified the evaluated results with three-dimensional finite element analysis and theoretical discussion, and obtained the following results:

1. The back electromotive force can be adjusted by changing the value of the field current from positive to negative. Second-order time harmonics, which are generated in the back electromotive force, are reduced by running a positive field current.

2. The second-order time harmonics contained in the back electromotive force are caused by the second-order harmonics of the field pole magnetomotive force and the permeance variation that are double that of the iron piece.

3. The torque can be controlled by changing the armature current vector and the field current.

4. The largest third-order torque ripple is caused by the second-order spatial harmonics of the field pole.
magnetomotive force and the permeance variation that are double that of the iron piece, similar to that for the back electromotive force.

(5) Compared with the FHM of the conventional structure, the proposed FHCM increased the torque density.

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References

(1) R.L. Owen, Z.Q. Zhu, J.B. Wang, D.A. Stone, and I. Urquhart: “Review of Variable-Flux Permanent Magnet Machines”, Journal of International Conference on Electric Machines and Systems, Vol. 1, No.1, pp.23–31 (2012)
(2) H. Yang, Z.Q. Zhu, H. Lin, and W. Chu: “Flux Adjustable Permanent Magnet Machines: A Technology Status Review”, Chinese Journal of Electrical Engineering, Vol. 2, No.2, pp.14–30 (2016)
(3) Z.Q. Zhu and S. Cai: “Overview of Hybrid Excited Machines for Electric Machines”, Proceedings of the Institution of Civil Engineers, Vol. 183, No.1, pp.3–41 (2019)
(4) A. Salmi, H. Sami, A. Yacine, and G. Mohamed: “Study of a hybrid excitation synchronous machine: Modeling and experimental validation”, Mathematical and Computational Applications, Vol.24, No.2, No.34, pp.1–27 (2019)
(5) S. Hlioui, M. Gabsi, H. Ben Ahmed, G. Barakat, Y. Amara, F. Chabour, and J.J. H. Pauleides: “Hybrid Excited Synchronous Machines”, IEEE Transactions on Magnetics (2021) (to be published)
(6) N. Jike, T. Fukami, M. Koyama, H. Mitsuda, K. Ito, and M. Yamada: “Principle and Basic Characteristics of a Flux-Modulating Hybrid Field Motor”, IEEE Transactions on Industry Applications, Vol.140, No.7, pp.542–549 (2020) (in Japanese)
(7) H. Hongo, T. Fukami, M. Koyama, H. Mitsuda, and K. Ito: “Machine Parameters of Flux-Modulating Hybrid Field Motors”, IEEE Transactions on Industry Applications, Vol.5, No.5, pp.1–8 (2021) (in Japanese)
(8) A.R.W. Broadway: “Cageless induction machine”, Proceedings of the Institution of Electrical Engineers, Vol.118, No.11, pp.1593–1600 (1971)
(9) T. Fukami, Y. Matosuura, K. Shima, M. Momiyama, and M. Kawamura: “A Multipole Synchronous Machine With Nonoverlapping Concentrated Armature and Field Windings on the Stator”, IEEE Transactions on Industrial Electronics, Vol.59, No.6, pp.2583–2591 (2012)
(10) X. Liu and Z.Q. Zhu: “Electromagnetic Performance of Novel Variable Flux Reluctance Machines With DC-Field Coil in Stator”, IEEE Transactions on Magnetics, Vol.49, No.6, pp.3020–3028 (2013)
(11) T. Raminosoa, D. Torrey, A. El-Refaie, K. Grace, D. Pan, S. Grubic, K. Bodla, and K. Huh: “Sinusoidal reluctance machine with DC winding: An attractive non-permanent magnet option”, IEEE Transactions on Industry Applications, Vol.52, No.3, pp.2129–2137 (2016)
(12) Z.Q. Zhu, Z.Z. Wu, and X. Liu: “A Partitioned Stator Variable Flux Reluctance Machine”, IEEE Transactions on Energy Conversion, Vol.31, No.1, pp.78–92 (2016)
(13) H. Hua and Z.Q. Zhu: “Novel Parallel Hybrid Excited Machines With Separate Stators”, IEEE Transactions on Energy Conversion, Vol.33, No.3, pp.1212–1220 (2016)
(14) H. Hua, Z.Q. Zhu, and H. Zhan: “Novel Consequent-Pole Hybrid Excited Machine With Separated Excitation Stator”, IEEE Transactions on Industrial Electronics, Vol.63, No.8, pp.4718–4728 (2016)
(15) S. Yamamoto, T. Fukami, M. Koyama, H. Mitsuda, and K. Ito: “A Flux-Modulation Hybrid-Field Consequent Pole Motor”, The Annual Meeting of I.E.E. Japan, No.5-047 (2020)
(16) H. Mitsuda, K. Ito, S. Yamamoto, T. Tobinga, T. Fukami, and M. Koyama: “Fabrication and Testing of a Flux-Modulation Hybrid-Field Consequent Pole Motor”, The Annual Meeting I.E.E. Japan, No.5-055 (2021)
(17) H. Hongo, H. Nakai, Y. Kan, E. Yamada, and R. Minutani: “Proposal and Feasibility Study of the Integrated Diode Synchronous Motor”, IEEE Transactions on Industry Applications, Vol.137, No.1, pp.54–60 (2016) (in Japanese)
(18) J. Takeuchi: “Daigaku-Katei Denki Sekkei Gaku, 3rd edition”, Ohmsha, p.7 (2016) (in Japanese)