The Development of Direct-drive High-speed Brushless Permanent Magnet Motors for Machine Tools

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\textbf{ABSTRACT}

This paper describes the development of surface-mounted permanent magnet (SPM) motors for use in direct-drive high-speed spindle machine tools. For such applications, motors should be highly efficient, have low torque ripple, and minimal package size and weight. Based on average torque, torque ripple, and efficiency, four SPM motor designs were chosen for comparison. Two configurations, 2-pole with 3-slots and 2-pole with 18-slots, were selected for analysis. The Taguchi method was used to refine stator structure and to determined how well the selected motors reduced losses. The effect of magnet span angles and winding arrangements on torque ripple, average torque, and the efficiency of 2-pole/18-slot motors was also investigated. A thermal analysis was conducted to determine temperature distribution within the motors. The motor performance was also studied using a Matlab/Simulink model. Experimental measurements and finite element analysis (FEA) were used to examine and validate the results.

\section{1. Introduction}

Induction motors have been widely used in machine tools for many years due to their rugged rotor structure, low maintenance, and reasonable cost [1–3]. However, the presence of rotor joule losses reduces the overall efficiency and power factor. To satisfy the need to save energy, for higher efficiency, and more precise operation, permanent magnet synchronous motors
Permanent magnet synchronous motors (PMSMs) have become necessary. PMSMs are more efficient and have a better power factor than induction motors, and have been widely used in several industry applications, such as compressors, vacuum pumps, friction welding units, and machine tools. However, the PMSMs used in high speed applications do not come without some drawbacks. The problems are in design where there is a need for sleeves to secure the surface-mounted magnets, and the resulting complexity of rotor structure to ensure proper operation at high speed.

There are several possible PMSMs designs for high-speed operations: 2-pole [4] or 4-pole [5]; 3-slot [4], or 6-slot [4]; overlapping [4] or non-overlapping [4] windings; slotted [3,4,6–9] or slotless stators; and rotors with or without a sleeve retaining the surface-mounted magnets.

In this study four different high speed surface-mounted permanent magnet (SPM) motor designs were compared. The application was for glass grinding in a spindle machine tool and for this particular application, the motor needs to be very efficient and to have low torque ripple in a minimal lightweight package. Two motor designs were chosen from the original four proposed and were refined to achieve the performance required. In addition to the electromagnetic design described in this paper, thermal analysis was also conducted to ensure the design was suitable for the high motor supply frequency. A lumped circuit model was used [6] to assess the rated operation and thermal behavior of the motor. A prototype motor was built and tested to confirm the simulation results and good agreement was found.

2. Initial Design and Comparison

A valid stator slot and rotor pole count combination must be determined and used in the construction of a PMSM. To limit the scope, the design adopted was that of a balanced three-phase motor. The rated power was 1500 W with a rotation speed of 60,000 rpm. A two-pole (2P) motor design (Figure 1(a)) was selected to limit the high fundamental frequency to 1.0 kHz. The combination of stator slots \( N_s \) and rotor poles \( N_p \) of a balanced three-phase motor must satisfy [3].

\[
\frac{N_s}{\text{GCD}(N_s, N_p)} = 3k
\]  

where \( k \) is an integer and GCD the greatest common divisor. The outside diameter of the stator was fixed at 48 mm (Table 1). To ensure enough space was available for three-phase winding and to satisfy (1), four valid pole/slot combinations, 2/3, 2/6, 2/12, and 2/18 were investigated and compared.

Because the ratio of slot opening to slot pitch was low, the losses induced from the stator slot permanent harmonics were very small [3]. Solid steel alloy magnets (ID35 CD4) were used for the rotor yoke. Iron is the dominant contributor to all the losses in a high speed motor [6–9]. Fe-Si laminations with a thickness of 0.35 mm and a saturated flux of 1.7 T were used to limit iron losses in the stator. The iron losses, \( P_{\text{iron}} \) include hysteresis, eddy current (\( P_{\text{edd}} \)), and excess losses and can be computed as [10].

\[
P_{\text{iron}} = \iiint \left[ k_h B_n^2 f + \frac{\pi^2 \sigma d^2}{6} (B_n f)^2 + k_e (B_n f)^{3/2} \times 8.67 \right] k_d d\nu
\]
where \( f \) and \( B_m \) are the frequency and amplitude of the fundamental flux density, \( \sigma \) is the electrical conductivity, \( d \) is the lamination thickness, \( k_f \) is the iron stacking factor, and \( k_h \) and \( k_e \) are the hysteresis loss and eddy current loss coefficients of the stator iron material as listed in Table 1.

The cross sections of the initial four motor designs and the given parameters are shown in Figure 1 and Table 1. 2-D FEA is used to predict the motor performance as shown in Table 2 [5]. In the Table, \( P_{out} \) is the output power, \( T_{avg} \), \( T_{cog} \), and \( T_{rip} \) are average torque, peak-to-peak values of cogging torque, and peak-to-peak values of torque ripple divided...
by average torque, $P_{\text{tot}}$ and $P_{\text{cop}}$ are the total and copper losses, and $\eta$ is the efficiency. It can be seen from Table 2 that all the motor designs meet the output power requirement of 1500 W. But our attention was focused on the 2P/18S and 2P/3S configurations. The 2P/18S configuration has the highest performance of the four and is obviously the best candidate for use in the application. However, the 2P/3S configuration is easy to manufacture and was thought to be another choice if the efficiency could be improved.

3. Performance Improvement for 2P/3S Type

3.1. Optimal Refinement of Stator Structure

Because the 2P/3S configuration had high iron losses, it was necessary to refine the stator structure to improve performance. The Taguchi method \[11\] was used for the purpose.

(1) **Taguchi Methodology:** four factors were chosen to start with, see Table 3. Each factor has three levels, the performance of the motor in the L9 (34) matrix experiments was obtained from 2-D FEA, as shown in Table 4.

(2) **Experimental:** The influence of each factor on the efficiency and torque ripple was as shown in Figure 2. It was observed that factor-level combinations (A3, B3, C3, and D3) contributed to both the maximization of efficiency and the minimization of torque ripple.

(3) **Result Analysis:** 2-D FEA was used again to determine performance of the optimized motor. Table 5 compares the data of initial motor design with that from the Taguchi

**Table 2. Comparison of motor performance.**

| Parameter | 2P3S     | 2P6S     | 2P12S    | 2P18S    |
|-----------|----------|----------|----------|----------|
| $P_{\text{out}}$ (W) | 1573.75  | 1532.72  | 1575.53  | 1580.53  |
| $T_{\text{avg}}$ (N·m) | 0.251    | 0.244    | 0.251    | 0.252    |
| $T_{\text{cog}}$ (mN·m) | 9.06     | 12.39    | 0.74     | 0.11     |
| $\eta$ (%) | 8.06     | 6.71     | 0.43     | 0.38     |
| $P_{\text{tot}}$ (W) | 104.54   | 75.20    | 77.74    | 71.88    |
| $P_{\text{cop}}$ (W) | 22.21    | 19.07    | 19.83    | 20.72    |
| $P_{\text{edd}}$ (W) | 63.25    | 9.41     | 1.01     | 0.41     |
| $P_{\text{tot}}$ (W) | 190.03   | 103.68   | 98.58    | 93.01    |
| $\eta$ (%) | 88.55    | 93.66    | 94.11    | 94.44    |

**Table 3. Parameter definitions and design level classification.**

| Factor | Definition          | Level 1 | Level 2 | Level 3 |
|--------|---------------------|---------|---------|---------|
| A      | Stator tooth width (mm) | 13      | 14      | 15      |
| B      | Stator yoke width (mm)  | 3.5     | 4       | 4.5     |
| C      | Air gap size (mm)   | 0.9     | 1       | 1.1     |
| D      | Slot opening (mm)    | 1.2     | 1.3     | 1.4     |

**Table 4. Motor performance.**

| Exp. | $T_{\text{cp}}$ (%) | $\eta$ (%) | Exp. | $T_{\text{cp}}$ (%) | $\eta$ (%) |
|------|---------------------|------------|------|---------------------|------------|
| 1    | 22.46               | 88.65      | 6    | 4.35                | 90.10      |
| 2    | 21.85               | 89.355     | 7    | 15.96               | 89.37      |
| 3    | 3.09                | 90.445     | 8    | 10.405              | 89.65      |
| 4    | 19.94               | 88.96      | 9    | 3.56                | 90.41      |
| 5    | 6.79                | 89.90      |      |                     |            |
method. It was observed that torque ripple reduction in the initial design was 8.06% compared to 3.02% obtained by the Taguchi method. The efficiency increase in the initial design was 88.85% compared to 90.41% obtained by the Taguchi method. However, the copper losses had increased slightly due to the change in stator tooth and yoke width from the initial 14 mm and 4 mm, to 15 mm and 4.5 mm in the Taguchi method. As a result, the slot cross-sectional area available for windings was decreased; this caused an increase in current density and greater copper losses. It should be noted that a refinement of the stator structure can improve performance, however, the rotor eddy current losses were reduced very little.

3.2. Method of Reducing Rotor Eddy Current Losses

The most straightforward way to limit rotor eddy current losses is to increase the resistivity of the conductive materials. Each rotor pole magnet is divided into six segments (SEGs) around the circumference of the rotor, see LHS in Figure 1(a) [9] which also makes
manufacture easier. Table 6 shows the effects of segmentation on motor performance. While $P_{edd}$ is reduced, other losses are not significantly affected. The simulation results confirm that the technique is effective in reducing $P_{edd}$ and also increased overall motor efficiency.

The efficiency increased from that in the initial design of 88.85%–93.28% (Taguchi method of 90.41%) and the torque ripple in the initial design goes down from 8.06% to 2.76% (Taguchi method of 3.02%) while the output power was increased by about 200 W to meet the required output of 1500 W.

4. Performance Improvement for 2P/18S Type

As described in Section 2, the 2P/18S configuration is a good candidate for high speed machine tool applications. The influence of the two motor design variables, magnet span angle ($\beta$) and winding arrangement, was investigated with a view to improving performance.

The 2P/18S configuration employs a double-layered winding with either lap or concentric connections. Since there are 18 slots with two coil sides per slot, there will be 18 coils in the winding, to give three coils per pole per phase. The coils within each pole are connected in series, and the two-pole coils are connected in parallel, as shown in Figure 3.

4.1. Influence of Lap Windings and Magnet Span Angles

As can be seen in Figure 4, all the coils in the tops of the slots are shifted one slot. Hence, each coil spans 8/9 of one pole pitch, or 160 electrical degrees ($^\circ$E).

![Figure 3.](image-url)  
**Figure 3.** Coil connections for one phase of (a) lap and (b) concentric windings. 
Notes: There are two parallel paths in both windings with each turn formed 0.32 mm diameter wire. For (a), each coil has 13 turns. For (b), coil X has 13 turns, and the number of turns in coils Y and Z may vary.

![Figure 4.](image-url)  
**Figure 4.** A flattened layout of the lap windings.

| SEG | $T_{avg}$ (N·m) | $T_{rip}$ (%) | $P_{out}$ (W) | $P_{edd}$ (W) | $P_{iron}$ (W) | $\eta$ (%) |
|-----|-----------------|---------------|---------------|---------------|----------------|-------------|
| 1   | 0.271           | 3.02          | 1701.68       | 61.95         | 84.17          | 90.41       |
| 2   | 0.273           | 4.61          | 1715.30       | 41.37         | 83.75          | 91.55       |
| 3   | 0.275           | 1.55          | 1726.01       | 26.43         | 83.62          | 92.35       |
| 4   | 0.276           | 2.89          | 1732.07       | 18.29         | 83.49          | 92.83       |
| 5   | 0.2766          | 2.72          | 1735.76       | 13.30         | 83.42          | 93.11       |
| 6   | 0.277           | 2.76          | 1738.08       | 10.31         | 83.37          | 93.28       |
Figure 5 shows the effect of the magnet span angle on $T_{\text{avg}}$, $T_{\text{rip}}$, and $\eta$. It is clear that the required $T_{\text{avg}}$ of 0.24 Nm has been satisfied. It is also clear, from the figure, that as $\beta$ increases, $T_{\text{avg}}$ and $\eta$ increase, and $T_{\text{rip}}$ decreases. This suggests that lap windings $\beta = 180^\circ$E would be the best configuration. Hence, we have $\eta = 94.44\%$ and $T_{\text{rip}} = 0.379\%$.

4.2. Influence of Concentric Windings and Magnet Span Angles

In concentric windings the coil pitch is greater than one slot pitch, see Figure 6. Coil X has 13 turns and but coils Y and Z have different numbers of turns, as shown in Figure 3 (b), but always add up to 26 turns.

Figure 7 shows the variation of $T_{\text{avg}}$, $T_{\text{rip}}$, and $\eta$ with the total number of coil turns per phase and $\beta$. It is observed that the required $T_{\text{avg}}$ of 0.24 Nm is satisfied, and as $\beta$ and the number of turns in the center coil (Y) increase, $T_{\text{avg}}$ and $\eta$ increase, but $T_{\text{rip}}$ almost always decreases.
Key observations derived from Figure 7 include the following.

1. For higher $\eta$, $\beta = 180^\circ$E with a coil turn combination of $(13, 18, 8)$ is selected, as shown in Figure 7(a) and (c). Hence, we have $\eta = 94.61\%$ and $T_{rip} = 0.105\%$. The motor of this selection was prototyped as shown in Figure 8. The simulated and measured torques appear to be the same at 0.256 N·m. The measured efficiency is 94.5%, which is slightly different from the simulated result of 94.61%.

2. For lower $T_{rip}$, $\beta = 180^\circ$E with a coil turn combination of $(13, 17, 9)$ is selected, as shown in Figure 7(a) and (b). Hence, we have $\eta = 94.57\%$, $T_{rip} = 0.065\%$.

Figure 7. (a) $T_{avg}$, (b) $T_{rip}$, and (c) $\eta$ versus coil turns.
To reduce the magnet usage, fractional pitch magnets were considered. We assume a full pole pitch magnet usage of 5233.9 mm³ is equal to 1pu, so that the magnet usages for $\beta = 170, 160$, and 150°E are 0.9444, 0.8889, and 0.8333 pu, respectively. For higher $\eta$, the coil turn combination of (13, 18, 8) was selected, as shown in Figure 7 (c). Results of $\eta$ for $\beta = 170, 160$, and 150°E are 94.48, 94.32, and 94.23%, respectively. For lower Trip, the coil turn combination of (13, 17, 9) was used, as shown in Figure 7 (b). Results of Trip for $\beta = 170, 160$, and 150°E were 2.972, 1.582, and 2.497%.

5. Thermal Analysis

The lumped parameter thermal model was used to assess the rated operation condition and thermal behavior of the motor [9]. To keep the temperature down, the motors have a closed forced fluid cooling system that uses a water emulsion. The coolant flows at 0.6 l/m inside the chamber and follows a helical path around the stator. The inlet coolant temperature was 30 °C and temperatures recorded at some typical positions in the 2P/3S and 2P/18S motors, operating under rated conditions, are plotted in Figures 9 and 10. It can be seen that the normal motor operating temperatures were below critical level.

6. Performance Simulation

A Matlab/Simulink model with sinusoidal wave voltages was used to study motor performance. Since the motor output would be limited by winding inductance, especially at high-speed, it was necessary to investigate the dynamic performance to determine if it could reach the specified speed under the rated load at a given voltage.

6.1. Motor Model

The PMSM in the synchronously rotating reference frame can be described as follows [12,13]:

$$v_{qr} = R_r i_{qr} + L_{qr} i_{qr} + \omega_f (L_{dr} i_{dr} + \lambda_{id})$$

$$v_{dr} = R_r i_{dr} + L_{dr} i_{dr} - \omega_f L_{qr} i_{qr}$$

Figure 8. The prototype built for this study. (a) 18S stator with a coil turn combination of (13, 18, 8). (b) Rotor with carbon fiber sleeve. (c) The motor drive system.
where \( v_{qr} \) and \( v_{dr} \) are the \( d \)-axis and \( q \)-axis stator voltages, \( i_{qr} \) and \( i_{dr} \) are the \( d \)-axis and \( q \)-axis stator currents, \( L_{qr} \) and \( L_{dr} \) are the \( d \)-axis and \( q \)-axis stator inductances \((L_{qr} = L_{dr} = 4.1 \, \text{mH})\), \( \lambda_{fd} \) is the \( d \)-axis permanent magnet flux linkage \((\lambda_{fd} = 0.258 \, \text{Wb})\), \( R_r \) is the stator resistance \((R_r = 0.32 \, \Omega)\), \( \omega_f \) is the electrical rotor angular speed.

The electromagnetic torque \( T_e \) of the motor can be described as

\[
T_e = 3P_r \left[ \lambda_{fd} i_{qr} + \left( L_{dr} - L_{qr} \right) i_{dr} i_{qr} \right] / 4
\]  

(5)

Where \( P_r \) is the number of poles \((P_r = 2)\). The equation of the motor dynamics is

\[
J_r \ddot{\omega}_r + B_r \dot{\omega}_r + T_l = T_e
\]  

(6)

where \( \omega_r = 2\omega / P_r \), \( T_l \) stands for the load torque, \( B_r \) represents the viscous frictional coefficient \((B_r = 0.00126 \, \text{N}\cdot\text{m}/\text{rad})\) and \( J_r \) is the moment of inertia \((J_r = 0.0221 \, \text{N}\cdot\text{m} \cdot \text{s}^2/\text{rad})\). The basic principle in controlling a motor is based on field orientation. Due to \( L_{qr} = L_{dr} \) and \( i_{dr} = 0 \), the second term of (5) is zero. Moreover, \( \lambda_{fd} \) is constant. Rotor flux is produced in the \( d \)-axis only, while the current vector is generated in the \( q \)-axis for the field-oriented control. When the \( d \)-axis rotor flux is a constant and the torque angle is \( \pi/2 \), the maximum torque per ampere can be reached for field-oriented control at \( T_e \) which is proportional to \( i_{dr} \). With the implementation of field-oriented control, the motor drive can be simplified:

\[
T_e = K_t i_{qr} / 4
\]  

(7)

\[
K_t = 3P_r \lambda_{fd} / 4
\]  

(8)
6.2. Simulink Simulation of the Drive System

A Simulink model based on Equations (3)–(6) was created for the motor, see Figure 11. The basic design requirement was for the motor torque output to be 0.24 Nm, at a steady-state speed of 60,000 rpm when the inverter DC bus voltage was $V_{dc} = 514$ V. Using the proposed model, simulation was done under nominal conditions at 0.1s and the 0.24 Nm load was added at 0.5s. Some of the results are plotted in Figures 12–21, showing that the proposed motor could meet the design requirements. The simulated results for three-phase current under nominal conditions at 0.1s with the 0.24 Nm load added at 0.5s and DC bus voltage under many different states are shown in Figures 12–23.

The motor can operate correctly at a steady speed of 60,000 rpm with the rated torque of 0.24 Nm when the input phase voltage is 220 V i.e. the line voltage is RMS 381 V ($220 \times 1.732 = 381$ V). The three-phase diode-rectifier provides DC bus voltage of 514 V ($381 \times 1.35 = 514$ V) and the PWM method shows the maximum inverter sinusoidal line output to be the 514 V. The phase voltage in the PMSM from output line voltage of the inverter is 327 V ($514 \times 2/\pi = 327$ V) maximum and so the motor phase voltage is 231 V RMS ($327/1.414 = 231$ V).

![Figure 11. A Simulink-based model of a PMSM.](image)

![Figure 12. Rotor speed response under nominal conditions at 0.1s and after adding a 0.24 Nm load at 0.5s.](image)
Figure 13. Three-phase currents under nominal conditions at 0.1 s and after adding a 0.24 Nm load at 0.5s.

Figure 14. Three-phase currents in start state under nominal conditions.

Figure 15. Zoomed three-phase currents in start state under nominal conditions.
Figure 16. Zoomed three-phase currents in steady state under nominal conditions.

Figure 17. Zoomed three-phase phase currents in steady state after adding a 0.24 Nm load.

Figure 18. Three-phase line voltages in steady state under nominal conditions.
Figure 19. Three-phase voltages in steady state under nominal conditions.

Figure 20. Three-phase voltages in the steady state under nominal conditions using a second-order low-pass filter.

Figure 21. Simulated results of electromagnetic torque in the steady state under nominal conditions.
6.3. Experimental Validation

A prototype of the designed motor was fabricated and operated successfully with a PMSM control scheme. Extensive experiments were conducted using the prototype and Figure 24 shows the measured rotor speed waveform under nominal conditions at 0.1s and after the addition of a 0.24 Nm load at 0.7s. It can be seen that the measured rotor speed waveform under these conditions was similar to that of the simulated results in Figure 2. Figure 25 shows the measured waveform under the same conditions and it can be seen that they are similar to the simulated results in Figure 11. Figures 26 and 27 are zoomed waveform figures before and after addition of the 0.24 Nm load and the waveforms are also similar to those in Figure 11.

According to (1)–(4), a Simulink model (as shown in Figure 11) can be built for a PM brushless DC motor. The basic design requirements for the motor drive system is an output torque of 1.0 Nm, and a steady-state speed of 6000 rpm when the inverter DC bus voltage is $V_{dc} = 310$ VDC. This model was simulated and some of the results are plotted in Figures 12–27. It was clear the motor could reach the design requirements. The simulated results for start-up speed, electromagnetic torque, phase-a current, phase-b current, phase-c current, three-phase current, back EMF in phase-a, phase-a voltage, phase-b voltage, and phase-c

![Figure 22](image1.png)

*Figure 22.* Simulated results of electromagnetic torque in the steady state after adding a 0.24 Nm load.

![Figure 23](image2.png)

*Figure 23.* Simulated results of DC bus voltage under nominal conditions.
Figure 24. Rotor speed response under nominal conditions at 0.1 s and after adding a 0.24 Nm load at 0.7 s.

Figure 25. Measured result of three-phase phase currents under nominal conditions at 0.1 s and after adding a 0.24 Nm load at 0.7 s.

Figure 26. Zoomed three-phase currents in steady state under nominal conditions.
voltage under nominal conditions are shown in Figures 12–21. The simulated results after the addition of the 1 Nm load are shown in Figures 12–27.

7. Conclusion

This paper has presented two SPM motor designs, 2P/3S and 2P/18S, both suitable for use in high speed spindle machine tools. The key results are as follows:

(1) The 2P/3S motors have higher losses, but the stator is easier to wind. Optimal refinement of the stator structure and rotor magnet segments could reduce the losses and improve motor efficiency.

(2) The 2P/18S motors have the best performance. There are three important requirements for this particular application, high efficiency, low torque ripple, and lower magnet usage, all of which have been addressed and satisfied by the proposed optimal design.

A 2P/18S motor has been constructed, tested with a control scheme, and shown to validate the theoretical design and analysis.

Nomenclature

| Symbol | Description                        |
|--------|------------------------------------|
| $B_m$  | the maximum flux density (T)       |
| $d$    | thickness of lamination (mm)       |
| $\theta$ | electrical degrees                |
| $f$    | frequency (Hz)                     |
| $k_e$  | stator iron eddy current loss coeff |
| $k_h$  | stator iron hysteresis loss constant |
| $k_{exc}$ | excess loss constant             |
| $k_f$  | the iron stacking factor          |
| $N_p$  | number of rotor poles             |
| $N_s$  | number of stator slots            |

Figure 27. Zoomed three-phase currents in steady state after adding a 0.24 Nm load.
$P_{\text{cop}}$ copper loss (W)
$P_{\text{edd}}$ eddy current loss (W)
$P_{\text{iron}}$ iron loss (W)
$P_{\text{out}}$ output power (W)
$P_{\text{tot}}$ total loss (W)
$T_{\text{avg}}$ average torque (N·m)
$T_{\text{cog}}$ cogging torque (N·m)
$T_{\text{rip}}$ torque ripple (N·m)
$\beta$ magnet span angles (°E)
$\sigma$ electrical conductivity (S/m)
$\eta$ efficiency

**Acknowledgement**

The authors would like to express their sincere gratitude to Mr J W Jiang, of the Anderson Group, Taichung, for his most valuable technical comments and suggestions.

**Disclosure statement**

No potential conflict of interest was reported by the authors.

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