Model-Based Commutation of a Long-Stroke Magnetically Levitated Linear Actuator

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Abstract—This paper concerns a commutation and switching algorithm for multi-degree-of-freedom (DOF) moving-magnet actuators with permanent magnets and integrated active magnetic bearing. Because of the integration of long-stroke actuation and an active magnetic bearing, DQ-decomposition cannot be applied. Therefore, these actuators are a special class of synchronous machines. A newly developed model-based commutation algorithm for linear and planar actuators enables long-stroke motion by switching between different sets of coils. Moreover, the ohmic losses in the coils are minimized. Three different versions of the algorithm are compared and successfully implemented on a three-DOF magnetically levitated linear actuator.

Index Terms—Commutation, linear synchronous motors, magnetic levitation, permanent-magnet motors.

I. INTRODUCTION

IN RECENT YEARS, magnetically levitated planar actuators have been developed as an alternative to $xy$-drives constructed of stacked linear actuators. Although the translator of these actuators can move over relatively large distances in the $xy$-plane only, it has to be controlled in six degrees-of-freedom (DOFs) because of the active magnetic bearing. These actuators have either moving coils and stationary magnets [1] or moving magnets and stationary coils [2], [3]. The coils in the actuator are simultaneously used for propulsion in the $xy$-plane as well as for the four-DOF active magnetic bearing. To integrate the long-stroke propulsion and the active magnetic bearing of a moving-magnet actuator, new design [4] and control methods [5] have been developed.

Both planar actuator configurations enable long-stroke movement in the $xy$-plane. The main advantage of the moving-magnet configuration over the moving-coil configuration is that the coils, and, therefore, the cooling and the power supply, are on the stationary part of the actuator. Consequently, there is no need for a cable to the translator. The stroke of the moving-magnet planar actuator is limited by the amount of stationary coils. Only the coils below the translator of the moving-magnet structure significantly contribute to its propulsion and its active magnetic bearing. Consequently, during the movement of the translator, the set of active coils needs to be switched. The decoupling of the DOFs that can be achieved by optimization of the moving-magnet actuator is fundamentally limited when using standard DQ-decomposition, since the design symmetries needed for decoupling do not exist [5] (as opposed to the moving-coil topology [1]). Due to the fundamentally larger coupling between the DOFs in combination with the need to switch between active coils, Park-transformation (DQ0-decomposition) and vector control cannot be applied. Therefore, a nonlinear model-based commutation algorithm has been derived [5] which controls each coil individually. The algorithm enables long-stroke motion, without compromising the decoupling of the DOFs.

This paper provides an implementation of the new commutation algorithm on a long-stroke three-DOF magnetically levitated linear actuator [4]. A simplified real-time model [6] of the three-DOF actuator has been used for model-based control. A comparison of three implementations of the model-based commutation has been made using different sizes of active coil sets. The three-DOF linear actuator incorporates the same integration of propulsion and active magnetic bearing as a six-DOF long-stroke moving-magnet planar actuator, which is the final goal of this paper.

II. TOPOLOGY

The bottom and side views of the three-DOF actuator are shown in Fig. 1. The three DOFs, propulsion in the $x$-direction, levitation in the $z$-direction, and rotation of the translator about the $y$-axis, are indicated in the side view. The translator is a Halbach magnet array. The magnets in the array are rotated $45^\circ$ with respect to the coils. The stator consists of ten coils. Five adjacent coils are simultaneously energized to control the three DOFs. The total stroke in the $x$-direction is approximately 191 mm. The distance between the coils is $3/2 r_n$. The definition of the pole pitch $r_n$ is shown in Fig. 1. The vector $\vec{p} = [p_x, p_y, p_z]^T$ indicates the position of the translator with respect to the origin of the global coil-coordinate system. The actuator can be classified as an ac-synchronous machine. However, due to
systems.

\[ \vec{F}_{\text{c}} \]

force equation can be reduced to a closed line integral by reducing the model complexity, the volume integral of the Lorentz force on the magnets is opposite to the force on the coils. To be calculated with the Lorentz force method. However, the model which is used to derive the forces and torque of one can be obtained by neglecting the higher space harmonics of the magnetic-flux-density distribution \( \vec{B} \) (as defined in Fig. 1). The error which is introduced by the nonsinusoidal commutation, it is not a polyphase actuator. Therefore, each coil is connected to a single-phase amplifier.

III. MODEL

A. Single-Coil Real-Time Model

To derive a model-based commutation algorithm, an analytical real-time model of the three-DOF actuator is required. The model which is used to derive the forces and torque of one coil is presented in [6]. The force and torque on the translator can be calculated with the Lorentz force method. However, the force on the magnets is opposite to the force on the coils. To reduce the model complexity, the volume integral of the Lorentz force equation can be reduced to a closed line integral by modeling the coil as four filaments as shown in Fig. 2. The force \( \vec{F}_{\text{trans}} \) and the torque \( \vec{T}_{\text{trans}} \) on the translator, expressed in the global coil-coordinate system (defined in Fig. 1 and indicated by superscript \( c \)), then become

\[
\begin{align*}
\vec{F}_{\text{trans}} &= - \oint_{\text{c}} \vec{c} \times \vec{c} \vec{B} \, dl \\
\vec{T}_{\text{trans}} &= - \oint_{\text{c}} \vec{r} \times (\vec{c} \vec{i}_f \times \vec{c} \vec{B}) \, dl
\end{align*}
\]

where \( \vec{c} \vec{i}_f \) is the filament current vector in ampere-turns, \( \vec{c} \vec{B} \) is a magnetic-flux-density distribution in coil coordinates, and \( \vec{c} \vec{r} \) is the distance to the mass center point of the translator then become [6]

\[
\begin{align*}
\vec{F}_{\text{trans}} &= \begin{bmatrix} c F_x \\ c F_y \\ c F_z \end{bmatrix}^T \\
&= \begin{bmatrix}
N4B_2 \tau_n e^{-\lambda x} \sin \left( \frac{\pi(p_x - c_x)}{\tau_n} \right) i \\
0 \\
N4\sqrt{2B_{xy}} \tau_n e^{-\lambda y} \cos \left( \frac{\pi(p_y - c_y)}{\tau_n} \right) i
\end{bmatrix}
\end{align*}
\]

\[ (3) \]

\[
\begin{align*}
\vec{T}_{\text{trans}} &= \begin{bmatrix} c T_x \\ c T_y \\ c T_z \end{bmatrix}^T \\
&= \begin{bmatrix}
N2\sqrt{2B_{xy}} \tau_n e^{-\lambda z} \sin \left( \frac{\pi(p_z - c_z)}{\tau_n} \right) i - \frac{c_x}{c_y} \vec{F} \\
(c_x - c_y) \vec{F}_z - c_y \vec{F}_x \\
(c_y - c_z) \vec{F}_x - c_z \vec{F}_y
\end{bmatrix}
\end{align*}
\]

\[ (4) \]

where \( (c_x, c_y, 0) \) is the position of the center of the coil in global coil coordinates as defined in Fig. 2, \( c_x \) and \( c_y \) indicate the translator position in global coil coordinates as defined in Fig. 1, \( i \) is the coil current in amperes, \( \tau_n \) is the new effective pole pitch after rotation of the magnet array, \( \lambda = \pi/\tau_n \) is a constant representing the decline of the magnetic flux density, \( N \) is the number of turns, and \( B_{xy} \) and \( B_z \) are the effective amplitudes of the sinusoidal flux density at \( m z = 0 \). It is also possible to include the angle \( \theta \) in the model [6]. For simplicity, the force and torque equations are rewritten into

\[
\vec{w} = \begin{bmatrix} c F_x \\ c F_z \\ c T_y \end{bmatrix}^T = \gamma \begin{bmatrix} p_x \\ p_z \\ c x \end{bmatrix} i
\]

\[ (5) \]

where the wrench vector \( \vec{w} \) is a vector containing the force and torque components which are nonzero after assuming \( c_y = 0 \), and \( c_x \) is the location of the center of the coil along the \( x \)-axis of the global coil-coordinate system.

B. Full Three-DOF Actuator Model

So far, the influence of only one coil has been considered. The total wrench exerted on the translator is calculated using superposition of each individual coil, assuming rigid-body dynamics. After superposition, the wrench vector \( \vec{w} \) can be obtained by multiplying a \([3 \times 10]\) position-dependent matrix \( \vec{r} \vec{R}_c \) by a current vector \( i = [i_1 \ i_2 \ \cdots \ i_{10}]^T \) which contains the individual currents of the ten coils, as shown by (6) at the bottom of the next page. This model does not contain the end effects of the magnet array. Therefore, it is only valid for the coils which are not near or outside the periphery of the magnet.
array. This does not present a big disadvantage, since only the coils near the center of the translator will be excited.

C. Simplified Three-DOF Actuator Model

The three-DOF actuator is controlled using only a subset of the ten coils. The smallest simplified model contains at least five simultaneously energized adjacent coils. Due to the repeating pattern of the stationary coils in the \( x \)-direction, only five coils need to be modeled simultaneously. Therefore, a simplified model is proposed, as shown by (7) at the bottom of the page, in which case \( \tau \mathbf{T}_r \) becomes a \([3 \times 6]\) matrix with \( \alpha \in \{-2, -1, 0, 1, 2\} \) and \( p_x \in [-3/4 \tau_n, 3/4 \tau_n] \), as shown at the bottom of the page, and a model using seven active coils, shown by (9) at the bottom of the page, in which case \( \tau \mathbf{T}_r \) becomes a \([3 \times 7]\) matrix with \( \alpha \in \{-2, -1, 0, 1\} \) and \( p_x \in [0, 3/2 \tau_n] \), as shown at the bottom of the page. The inaccuracy introduced by the edge effect can be reduced by applying correction terms to the model of the coils located near the edge of the magnet array. It is also possible to use data-based methods to derive the correct values of the wrench as a function of the currents through the coils in proximity of the edges of the magnet array. However, this is beyond the scope of this paper.

IV. COMMUTATION AND SWITCHING

A. Short-Stroke Direct Wrench-Current Decoupling

Since the force distribution over the translator has to be taken into account, each active coil has to be controlled individually. Therefore, linearization by feedback is used to linearize and

\[
\dot{\mathbf{w}} = \tau \mathbf{T}_r(r p_x, r p_z) \dot{\mathbf{i}}
\]

\[
= \left[ \gamma(r p_x, r p_z, -\frac{9}{2} \frac{3 \tau_n}{2}) \quad \gamma(r p_x, r p_z, -\frac{7}{2} \frac{3 \tau_n}{2}) \quad \cdots \quad \gamma(r p_x, r p_z, \frac{9}{2} \frac{3 \tau_n}{2}) \right] \dot{\mathbf{i}}
\]

(6)

\[
\dot{\mathbf{w}} = \tau \mathbf{T}_r(r p_x, r p_z) \dot{\mathbf{i}}_{\alpha}
\]

\[
= \left[ \gamma(r p_x, r p_z, -\frac{3 \tau_n}{2}) \quad \gamma(r p_x, r p_z, -\frac{1}{2} \frac{3 \tau_n}{2}) \quad \cdots \quad \gamma(r p_x, r p_z, \frac{5 \frac{3 \tau_n}{2}}{2}) \right] \dot{\mathbf{i}}_{\alpha}
\]

(7)

\[
\dot{\mathbf{w}} = \tau \mathbf{T}_r(r p_x, r p_z) \dot{\mathbf{i}}_{\alpha}
\]

\[
= \left[ \gamma(r p_x, r p_z, -\frac{5 \frac{3 \tau_n}{2}}{2}) \quad \gamma(r p_x, r p_z, -\frac{3 \frac{3 \tau_n}{2}}{2}) \quad \cdots \quad \gamma(r p_x, r p_z, \frac{5 \frac{3 \tau_n}{2}}{2}) \right] \dot{\mathbf{i}}_{\alpha}
\]

(8)

\[
\dot{\mathbf{w}} = \tau \mathbf{T}_r(r p_x, r p_z) \dot{\mathbf{i}}_{\alpha}
\]

\[
= \left[ \gamma(r p_x, r p_z, -\frac{5 \frac{3 \tau_n}{2}}{2}) \quad \gamma(r p_x, r p_z, -\frac{3 \frac{3 \tau_n}{2}}{2}) \quad \cdots \quad \gamma(r p_x, r p_z, \frac{5 \frac{3 \tau_n}{2}}{2}) \right] \dot{\mathbf{i}}_{\alpha}
\]

(9)
decouple the DOFs, instead of using DQ0-transformation [5]. The advantage of this approach, when compared with the DQ0-approach, is that it results in a decoupling of both the forces and the torque. The three-DOF actuator is clearly overactuated (there are more active coils than DOFs). Consequently, there is a set of transformations that lead to a proper linearization and decoupling of the DOFs. Therefore, the ohmic losses in the coil can be minimized leading to the following optimization problem:

$$\min_{\mathbf{r}_{p}, \mathbf{r}_{z}} \| \mathbf{i}_{a} \| = \| \mathbf{r}_{a} \|$$  \hspace{0.5cm} (10)

where the two-norm of the active current vector $\mathbf{i}_{a}$, i.e., the ohmic losses in the coils, is minimized while maintaining the relation between the current and the wrench vector given by (7). Equation (10) can be solved by using the generalized inverse of a minimum-norm solution of a consistent equation [7], resulting in

$$\mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}) = \mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}) (\mathbf{\Gamma}_{r}(\mathbf{r}_{p}, \mathbf{r}_{z}) \mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}))^{-1}.$$  \hspace{0.5cm} (11)

### B. Long-Stroke Direct Wrench-Current Decoupling

In order to achieve long-stroke movement, it is necessary to switch between active coils. Therefore, (11) only presents a solution to the minimization of the ohmic losses in the coils (10) for short-stroke motion, since it does not incorporate any switching constraints.

Due to the finite voltage specification of the amplifiers which drive the individual coils, the time derivative of the currents through the coils is limited. Therefore, an additional constraint has to be added to ensure smooth switching of the coils. A possible strategy can be to switch a coil off when the current through that coil becomes zero. However, this does not guarantee that the coil can be switched off before it leaves the surface of the translator, at which point, the model is not valid anymore (as explained in Section III-C). When there is a priori information about the trajectory, it is possible to check whether (11) can be used for that trajectory, i.e., the trajectory in combination with (11) should always result in a zero crossing of the currents through the coils that need to be switched off. However, this leads to a difficult and very time consuming optimization (e.g., mixed-integer quadratic programming) which has to be calculated offline for each trajectory. A more practical solution, which does not need a priori information about the trajectory, is to actively force the currents through the coils near the edge of the translator to zero by using smooth position-dependent weighing functions which penalize each current.

When minimizing the ohmic losses, including the switching constraints, the following optimization needs to be solved:

$$\min_{\mathbf{r}_{p}, \mathbf{r}_{z}} \| \mathbf{r}_{a} \| = \| \mathbf{\Delta}_{r}^{-1}(\mathbf{r}_{p}) \| = \| \mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}) \mathbf{\Delta}_{r}(\mathbf{r}_{p}) \|$$  \hspace{0.5cm} (12)

where the weighted two-norm of the current vector $\mathbf{r}_{a}$ is minimized, using a current-vector weighing matrix $\mathbf{\Delta}_{r}(\mathbf{r}_{p})$ while maintaining the relation between the current and the wrench-vector given by (7). When using five active currents, the weighing matrix $\mathbf{\Delta}_{r}(\mathbf{r}_{p})$ consists of the following structure:

$$\mathbf{\Delta}_{r}(\mathbf{r}_{p}) = \begin{bmatrix} e_{f_{r, w_{1}}(\mathbf{r}_{p})} & 0 & 0 & 0 & 0 \\ 0 & 1 & 0 & 0 & 0 \\ 0 & 0 & 1 & 0 & 0 \\ 0 & 0 & 0 & 1 & 0 \end{bmatrix}$$  \hspace{0.5cm} (13)

with

$$e_{f_{r, w_{1}}(\mathbf{r}_{p})} = \frac{1}{2} + \frac{1}{2} \cos \left( \frac{2\pi}{3\tau_{n}} \mathbf{p}_{x} \right)$$  \hspace{0.5cm} (14)

$$e_{f_{r, w_{2}}(\mathbf{r}_{p})} = \frac{1}{2} - \frac{1}{2} \cos \left( \frac{2\pi}{3\tau_{n}} \mathbf{p}_{x} \right)$$  \hspace{0.5cm} (15)

where the elements on the diagonal of the matrix represent a penalty to each individual active coil current. If the elements are equal to one, there is no penalty, and if they are equal to zero, the corresponding active coil current is forced to zero. Since the active set of coil currents must be smoothly switched from one set to another to achieve long-stroke motion, the outermost coils of the active coil-current set need to be penalized. As can be seen from (13), the first and last elements contain functions which are dependent on the repeating $x$-position $\mathbf{p}_{x}$ [(14), (15)], which is illustrated in Fig. 3. Naturally, the same can be done for six or seven active coils.

Again, (12) can be solved by using the generalized inverse of a minimum-norm solution of a consistent equation [7], resulting in

$$\mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}) = \mathbf{\Delta}_{r}(\mathbf{r}_{p}) \mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}) (\mathbf{\Gamma}_{r}(\mathbf{r}_{p}, \mathbf{r}_{z}) \mathbf{\Delta}_{r}(\mathbf{r}_{p}) \mathbf{\Gamma}_{r}^{-1}(\mathbf{r}_{p}, \mathbf{r}_{z}))^{-1}.$$  \hspace{0.5cm} (16)

### C. Reduction of Calculation Time

Equation (16) has been implemented using two different strategies. The most straightforward implementation strategy is to derive the analytical solution of (16) and use the solution to calculate the position-dependent currents. A second implementation method is to perform a real-time numerical matrix inverse operation. The amount of calculation time needed to solve all trigonometric operations of the analytical solution is much longer than the amount of calculation time needed for the numerical real-time matrix inverse. Moreover, the implementation method using a real-time matrix inverse enables easy offset correction of the coil locations.

### V. SIMULATION AND EXPERIMENTAL VERIFICATION

A three-DOF moving-magnet linear preprototype which incorporates the same integration of propulsion and active magnetic bearing as a six-DOF moving-magnet planar actuator has been designed, optimized, and manufactured [4]. The sizes of the actuator are summarized in Table I. Figs. 4 and 5 show a photograph of the stator and of the translator, respectively.

To test this three-DOF actuator, the translator is attached to a mechanical H-drive with three coupled linear motors. The
### Table I

<table>
<thead>
<tr>
<th>Dimension</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Height of magnet array</td>
<td>8 mm</td>
</tr>
<tr>
<td>Size of square magnets</td>
<td>20x20 mm²</td>
</tr>
<tr>
<td>Size of side magnets</td>
<td>20x10 mm²</td>
</tr>
<tr>
<td>Height of coils</td>
<td>8 mm</td>
</tr>
<tr>
<td>Conductor bundle width</td>
<td>11 mm</td>
</tr>
<tr>
<td>Number of turns, N</td>
<td>348</td>
</tr>
<tr>
<td>Translator mass</td>
<td>5.6 kg</td>
</tr>
<tr>
<td>Pole pitch, τ_p</td>
<td>21.21 mm</td>
</tr>
<tr>
<td>Coil width</td>
<td>4τ_p</td>
</tr>
</tbody>
</table>

H-drive (or gantry) allows positioning of the translator of the three-DOF actuator in three DOFs with respect to the stator coils. For this experiment, the three-DOF actuator is rotated 90° about its x-axis, i.e., put on its side. Due to this rotation, the DOFs of both systems match. A schematic top view and a photograph of the three-DOF actuator and the H-drive are shown in Figs. 6 and 7(a), respectively. A six-DOF load cell (JR3 45E15A4-I63-S 100N10) is mounted between the translator of the H-drive and the translator of the three-DOF actuator to measure the forces and torques on the translator. The commutation and switching algorithm has been tested in open loop.

While the H-drive moves the translator in the x-direction along the stator coils at 0.02 m/s, with a mechanical clearance of 2 mm and θ = 0 rad, a constant wrench of \( cF_x = 10 \text{ N}, \ cF_z = 10 \text{ N}, \ cT_y = 0 \text{ Nm}, \) a mechanical clearance of 2 mm, and θ = 0 rad. The areas in which the different coil sets are active are indicated with the absolute-position counter \( \alpha \).

Fig. 8. Simulated current waveforms using five active coils, \( cF_x = 10 \text{ N}, \ cF_z = 10 \text{ N}, \ cT_y = 0 \text{ Nm}, \) a mechanical clearance of 2 mm, and θ = 0 rad. The areas in which the different coil sets are active are indicated with the absolute-position counter \( \alpha \).

Because the commutation is measured in open loop, the result is very sensitive to power-amplifier offsets, variation in the magnet properties, and tolerances in the exact coil locations. Therefore, all power-amplifier offsets and coil locations have been individually adjusted in the algorithm. This has been done by measuring the force produced by a single coil at a constant current. According to (3), the force along the x-axis \( cF_x \) depends sinusoidally on the difference between the coil offset \( c_x \) and the translator position \( c_{px} \). Therefore, when the coil offset is correct, the measured force is zero for \( c_{px} = c_x \).
Fig. 9. Simulated current waveforms using six active coils, \( cF_x = 10 \text{ N}, cF_z = 10 \text{ N}, cT_y = 0 \text{ Nm}, \) a mechanical clearance of 2 mm, and \( \theta = 0 \text{ rad}. \) The areas in which the different coil sets are active are indicated with the absolute-position counter \( \alpha. \)

Fig. 10. Simulated current waveforms using seven active coils, \( cF_x = 10 \text{ N}, cF_z = 10 \text{ N}, cT_y = 0 \text{ Nm}, \) a mechanical clearance of 2 mm and \( \theta = 0 \text{ rad}. \) The areas in which the different coil sets are active are indicated with the absolute-position counter \( \alpha. \)

The individual coil-offset adjustments result in small changes of the current waveforms, which is shown in Figs. 11 and 12.

Figs. 13–18 are three sets of two figures showing the predicted and measured force and torque waveforms of the commutation using five, six and seven active coil currents, respectively. The measured data of the forces \( cF_x, cF_z \) and torque \( cT_y \) as a function of time have been filtered offline using an eighth-order anticausal Butterworth filter with a bandwidth of 25 Hz. Apart from an increased force and torque ripple, the measurements are in good agreement with the predicted values.

The measured force ripple is close to the measurement accuracy of the load cell. Moreover, the predicted force and torque ripple is calculated using ideal coil and magnet dimensions. The measured ripple using the commutation algorithm is minimized by adjusting the individual coil and amplifier offsets. However, the amount of harmonic disturbance of each individual coil, resulting from the tolerances in the magnet and coil dimensions, leads to a higher commutated force and torque ripple. Figs. 13–18 clearly illustrate that the desired
values of the reference forces and torques are obtained. The repeating patterns also coincide with the amount of switching moments (six periods when using five active coils, five periods when using six active coils, and four periods when using seven active coils). Increasing the amount of active currents beyond the minimum of five (which is necessary for controllability [5]) reduces not only the power dissipation but also the stroke. The higher measured force and torque ripple still contains the same periodic behavior as the switching moments because of the position-dependent sensitivity to model disturbances of the commutation algorithm. This position-dependent sensitivity is caused by the switching between active coils as well as the changing force distribution over the translator as a function of position [8]. When using seven active coils, the model of the outer coils which need to be switched on or off is not accurate anymore. However, since these coils are heavily penalized, the model errors of these coils do not significantly contribute to the total decoupling of the commutation algorithm. Errors in the total decoupling occur when the amount of simultaneously

Fig. 14. Predicted and measured open-loop torque using five active coils during commutation $cF_x = 10$ N, $cF_z = 10$ N, $cT_y = 0$ Nm, a mechanical clearance of 2 mm, and $\theta = 0$ rad.

Fig. 15. Predicted and measured open-loop forces using six active coils during commutation $cF_x = 10$ N, $cF_z = 10$ N, $cT_y = 0$ Nm, a mechanical clearance of 2 mm, and $\theta = 0$ rad.

Fig. 16. Predicted and measured open-loop torque using six active coils during commutation $cF_x = 10$ N, $cF_z = 10$ N, $cT_y = 0$ Nm, a mechanical clearance of 2 mm, and $\theta = 0$ rad.

Fig. 17. Predicted and measured open-loop forces using seven active coils during commutation $cF_x = 10$ N, $cF_z = 10$ N, $cT_y = 0$ Nm, a mechanical clearance of 2 mm, and $\theta = 0$ rad.

Fig. 18. Predicted and measured open-loop torque using seven active coils during commutation $cF_x = 10$ N, $cF_z = 10$ N, $cT_y = 0$ Nm, a mechanical clearance of 2 mm, and $\theta = 0$ rad.
active coils is increased beyond seven. These errors are a result of the influence of the unmodeled-edge effects of the magnet array on the active coils which are not yet penalized by the window functions. The measurement results are in good agreement with the simulation results and, therefore, elucidate the good performance of the model-based commutation strategy.

The success of the experiments on the open-loop behavior of the direct wrench-current decoupling resulted in the application of the new commutation algorithm onto a six-DOF planar actuator with moving magnets [8], [9]. The tracking error of this planar actuator is 30 μm and 100 μrad at full speed (1.4 m/s) and full acceleration (14 m/s²) [10].

VI. CONCLUSION

A new commutation algorithm, which enables combined long-stroke propulsion and active magnetic-bearing control of an ironless multi-DOF moving-magnet actuator, including smooth switching of the currents through the active coil sets, has been successfully implemented. The algorithm is model based and makes use of the repeating coil-structure of the moving-magnet actuator. Measurements show that the overall error on the model-based commutation caused by neglecting the edge effect is small, as long as the active coils located near the edges of the magnet array are properly penalized in the commutation algorithm. The measurement results are in good agreement with the simulation results and, therefore, elucidate the good performance of the model-based commutation strategy.

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Dr. van den Bosch has been a recipient of several prizes for his educational activities and several patents. He has served on the Boards of journals (Journal A) and conference committees.

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