Characterization and Control of a New High-Torque Motor for Autonomous Wearable Robotics

by

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Abstract
A new ‘axial-transverse flux’ motor (ATFM) topology is of interest to autonomous lower-extremity robotics designers for its high torque density and low winding resistance. Unfortunately, deliberate asymmetries in the design make finite-element modeling of this topology largely intractable. An ATFM prototype was characterized experimentally using a custom dynamometer and controller. The prototype was found to have a torque constant $K_t$ of 7.26 Nm/A and a per-phase winding resistance of 0.59 Ohms. It is characterized by high AC and DC zero-current torque, as well as significant torque ripple (M: 12.9%, SD: 0.6%) when driven with balanced three-phase sinusoidal commutation. A set of optimized commutation waveforms are developed based on an independent phase control strategy, and it is shown that this strategy can eliminate ripple in simulation and reduce it in practice (M: 7.8%, SD: 0.5%), without reduction of mean torque or increased conduction losses relative to sinusoidal commutation.
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Chapter 1

Introduction

Section 1.1 of this chapter presents the motivation for this thesis, explaining why electric motor research is of interest to designers of autonomous lower-extremity prostheses and other wearable devices. Section 1.2 gives an overview of types of electric motors that are appropriate for such applications. Section 1.3 describes the motor of interest in this thesis and presents some of its advantages and disadvantages. Section 1.4 reviews relevant prior work on the problem of ripple reduction for permanent magnet synchronous motors. Section 1.5 presents a summary of subsequent chapters.

1.1 Challenges for autonomous lower-extremity wearable devices

Traditional prosthetic ankles are passive devices made of carbon fiber. They are designed to replace some of the function of the absent limb by supporting the wearer, and by storing and returning energy during the stance phase of the gait cycle, when the limb contacts the ground. As passive devices, however, they are not capable of delivering the net positive work that a biological limb does during walking and running. It is thought that this deficiency can result in an asymmetrical gait, as well as a higher metabolic cost of walking and a lower
self-selected walking speed [1, 2, 3]. Over the last two decades, researchers have therefore developed, and in some cases commercialized, autonomous prostheses in an attempt to normalize the gait of lower-extremity amputees [4, 5, 6, 7, 8, 9, 10, 11, 12].

Lower extremities have high-torque, low-speed requirements. To date, autonomous lower-extremity prostheses have tended to meet these requirements either with pneumatic actuators, or with high-speed, low-torque motors with a high transmission ratio, as shown in Table 1.1.

Like all wearable devices, lower-extremity prostheses are subject to constraints on mass, size, and energy use. Pneumatic actuators are costly in terms of mass and volume, and tend to have low torque bandwidth. While electric motors can be smaller and lighter than pneumatic actuators, small-diameter motors are generally rated for low torques, and a transmission with a high gear ratio sacrifices some efficiency, and may add significant mass. Audible noise is also a concern, and high-speed motors can be loud.

Small electric motors rated for high torques and low speeds, while less common than small low-torque, high-speed motors, have the potential to reduce losses, noise, and mass in the transmission of an autonomous lower-extremity prosthesis. For this application, the Biomechatronics Research Group is interested in evaluating the suitability of a novel design in this category, an ‘axial-transverse flux’ permanent magnet synchronous motor designed by Planet Rider, LLC [13].

An actuator that is appropriate for lower-extremity prosthetic devices will likewise be useful in other autonomous wearable robotic devices, such as exoskeletons and orthoses, which have similar constraints on mass, volume, noise, and energy usage. An electric motor well-suited to prosthesis design will therefore have high versatility over a range of applications.
<table>
<thead>
<tr>
<th>Citation</th>
<th>Joint type</th>
<th>Actuator type</th>
<th>Motor type</th>
<th>Rated torque</th>
<th>Rated speed</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sup et al, 2008 [8]</td>
<td>Pneumatic</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cherelle et al, 2012 [12]</td>
<td>Ankle</td>
<td>Motor</td>
<td>Brushed (Maxon RE-30, 51.7 mNm cont., 60W)</td>
<td>150 mNm peak 7600 rpm</td>
<td></td>
</tr>
<tr>
<td>Au and Herr, 2008 [10]</td>
<td>Ankle</td>
<td>Motor</td>
<td>BLDC (Maxon EC-Powermax 30, 94.6 mNm cont., 200W)</td>
<td>3220 mNm stall 16100 rpm</td>
<td></td>
</tr>
</tbody>
</table>

Table 1.1: Recent powered lower-extremity prostheses with actuator type used
1.2 Suitable electric motors

High-torque, low-speed motors have been developed for many industrial applications, including drilling, extruding and injection molding, freight elevators, electric vehicles, and lithography. They tend to use permanent magnets and have high pole counts and large diameters, features which afford higher torque density.

1.2.1 Primary permanent magnet motor topologies

Permanent magnet motor topologies are named for the axis along which flux from the conductors interacts with permanent magnets to produce torque.

In radial flux machines, windings are typically wrapped around stator poles oriented radially, creating a magnetic field that points inward across the air gap (or outward, in the case of outrunners) towards the rotor shaft. This flux interacts with permanent magnets that also have fields oriented radially. In axial flux machines, windings are typically wrapped around stator poles oriented parallel to the rotor shaft, and the flux path runs axially as it passes through magnets with the same orientation. In transverse flux machines, the stator poles can be thought of as being wrapped around the windings, rather than the reverse. The stator field passes through the magnets parallel to the circumference of the motor.

1.2.2 Transverse flux motors: Advantages and disadvantages

Transverse flux motors have two primary advantages over others in high-torque applications. The first is that there are no end-turns in the windings; current passing through any section of the conductors in a transverse flux motor generates useful flux which is concentrated and directed to the permanent magnets. With no wasted conductor length, conduction losses from non-torque-generating sections of the windings are theoretically eliminated, allowing higher torques with no increase in conduction losses relative to other topologies. The second advantage is that the number of poles is decoupled from the space available for the windings,
allowing the designer to increase the pole count for a higher torque constant without winding space as a constraint.

However, transverse flux motors often exhibit high cogging due to large variations in the reluctance of the poles as a function of rotor angle. They can also be difficult to model due to complex three-dimensional flux paths, whereas most finite-element modeling techniques for electric motors assume two-dimensional paths and can disregard the third dimension.
1.3 The ‘Axial-Transverse Flux’ Motor

1.3.1 Design

The design and fabrication of the machine discussed here (Figure 1-1) was not the work of this author. This machine was created by Planet Rider, LLC [13] based on a novel topology they refer to as an ‘axial-transverse flux’ machine (ATFM) [14]. The prototype characterized in this thesis is a 165-mm, 30 pole-pair, three-phase permanent magnet synchronous machine designed for a nominal continuous torque of 180 Nm and a nominal speed of 240 rpm.

The prototype itself is much too large and heavy for use in a wearable device. It was created at this scale for ease of construction; manufacturing a smaller ATFM poses fabrication challenges that Planet Rider and collaborators at MIT have begun to address separately. The large prototype was made in order to demonstrate some of the fundamental characteristics of this topology. How performance will scale with size (particularly as some design elements are forced to change to meet tighter tolerances) remains an important yet unanswered question.

The ATFM rotor consists of a 50-mm aluminum shaft surrounded by four rings of pole pieces. Each pole piece (Figure 1-2(6)) is made of 0.014” M15 silicon steel ribbon (35A210), which is wound into a coil and then flattened and annealed, so that the laminations are
Figure 1-3: JMAG simulation of ATFM flux path. This figure shows 1/30th of a single phase, with two magnets (1), three spacers (2), three poles (3), the flux return ring (4), and conductors (5). Flux encircles the conductors, passing mainly radially through the pole pieces, axially into the spacers between the magnets, and transversely into and through the magnets. Image credit: Cameron Taylor.
Figure 1-4: ATFM components. (1), (9) Case endplates (3) Electromagnetics (4) Case shell (5) Rotor shaft (6), (8), (10) Bearing retainers (7) Bearing. CAD model credit: Planet Rider.
oriented radially, except at the ends of each piece. Between adjacent pole rings is a ‘flux return ring’ of the same laminated steel (Figure 1-2(5)), which encircles the rotor shaft and completes the magnetic circuit. (Figure 1-3) Each ring is slit between one pair of pole pieces in order to limit eddy currents, which were found to be high in initial testing by the designer.

The stator is comprised of three rings of 60 permanent magnets each (Figure 1-2(3)). The magnets are 45sh neodymium-iron-boron, which are EDMed, ground, and copper- and nickel-plated to prevent corrosion. Their fields point around the circumference of the ring with alternating polarity. Between the magnets are spacers made of a soft magnetic composite (Somaloy, SG Technologies [15]), and the ring is held together with epoxy. The windings for each phase (Figure 1-2(4)) are a single coil of 18-gauge copper wire with Nomex insulation rated for 220°C. Each coil sits inside a magnet ring with some of the turns filling a slot in the center of each magnet to maintain alignment. The three magnet-and-coil rings are stacked between four aluminum spacers (Figure 1-2(7)), each of which surrounds a rotor pole ring, leaving a nominal 0.0098” air gap between each rotor and stator ring.

The rotor turns on a single custom four-point contact bearing from NSK ([16]), held in place with a steel bearing retainer. The motor is enclosed in an aluminum case with steel endplates on the shaft end, and is fastened with steel bolts (Figure 1-4).

1.3.2 Asymmetries

A number of asymmetries were introduced by the designer. Most exist to reduce cogging, and to create more sinusoidal and balanced back-EMFs. The motor’s many asymmetries make simulations computation-intensive, limiting what can be modeled easily.

Pole piece spacing All poles on the four rotor rings are spaced 12.10 degrees apart, except for one pair on each ring, which are 9.08 degrees apart (Figure 1-5). This feature was added to reduce cogging.
Figure 1-5: All adjacent poles are 12.10 degrees apart except for one pair on each ring, which are 9.08 degrees apart.
Figure 1-6: The two outer magnet rings are rotated by different amounts relative to the inner ring.
Figure 1-7: Each flux return ring has a slit through the laminations to eliminate some unanticipated eddy currents.
**Magnet ring spacing** One of the two outer magnet rings is rotated 4.00 degrees from the center magnet ring, while the other is rotated 2.45 degrees from the center magnet ring (Figure 1-6). This feature was added to reduce cogging.

**Pole ring spacing** Adjacent stator rings are oriented 6.05 degrees from each other. This feature was added to reduce cogging.

**Flux return slit** Each flux return ring (the rings of steel laminations on the stator in between the pole rings) has a slit on one side (Figure 1-7). This asymmetry was added to reduce significant eddy currents discovered in the flux return ring in early prototypes.

**Flux sharing** The two center rings of pole pieces each share flux from two phases (one from phases A and B, the other from phases B and C), while the outer rings each carry flux from only one phase (one from phase A, one from phase C).

### 1.4 Torque ripple reduction

Tests on the ATFM prototype showed significant torque ripple (Chapter 4) when the motor is driven using balanced three-phase commutation. Torque ripple is most problematic at high torques and at low speeds, when rotor inertia does less to smooth out variations in torque. Since these are the conditions under which we would like to use this motor, it is important to be able to reduce ripple without otherwise sacrificing performance. Ripple reduction in AC motors has been of interest in research and industry for decades. The best solution is generally good machine design (fractional slot-pitch windings and skewing); however, improvements can be made with appropriate control strategies as well [17]. The strategy implemented and tested here uses a standard approach to ripple mitigation, in which phase current waveforms are pre-computed based on observed ripple as a function of current and rotor position, under constraints such as conduction loss minimization [18, 19].
1.5 Thesis summary

Chapter 2 describes the dynamometer designed to characterize and test the ATFM prototype. Chapter 3 presents the results of experiments done to characterize some of the ATFM's behavior. In Chapter 4, two commutation strategies for torque ripple reduction in the ATFM are described and compared in simulation and experiment. Chapter 5 summarizes the results from prior chapters and offers some directions for relevant future work.
Chapter 2

Testbed

A custom testbed was built for characterizing and testing the ATFM prototype, as described in this chapter. Section 2.1 presents requirements and early design choices for the testbed. Section 2.2 describes the final design, including the drives (2.2.2), power sources (2.2.3), sensors (2.2.4), and grounding scheme (2.2.5). Section 2.3 concerns controller hardware and operation. Section 2.4 details some safety considerations involved.

2.1 Design decisions

Initial requirements for this testbed included: capacity to measure torque up to $\pm 250$ Nm peak (the ATFM’s predicted peak torque) and $\pm 180$ Nm continuous; capacity to run fixed-velocity experiments at speeds from zero up to at least $\pm 240$ rpm (the ATFM’s nominal maximum speed); flexibility to implement arbitrary control strategies, including driving the three motor phases independently; and capacity to measure internal motor temperature (in order to prevent overheating). Cost and lead-time were also considerations. Several design configurations for this testbed were considered.

One design choice for the testbed concerned whether to connect the ATFM to an active, controllable load, such as a second motor, or to a passive inertial load (flywheel). The inertial
Figure 2-1: Testbed system block diagram. Each phase of the ATFM can be controlled independently with its own current amplifier, which is enabled by and receives PWM commands from a custom PSoC-based controller. A commercial motor coupled to the ATFM is controlled with proprietary hardware and software. Each motor is instrumented with an encoder and shaft-to-shaft torque feedback is measured by the ATFM controller.
Figure 2-2: Testbed components: (1) encoder disk mount, (2) encoder disk, (3) encoder reader, (4) encoder reader mount, (5) ATFM, (6) motor mounts, (7) bellows body coupling segments, (8) tapered coupling segments, (9) torque sensor, (10) baseplate, (11) opposing motor, (12) opposing motor encoder.
load could be used as a velocity source for constant-velocity measurements. The inertial-load design had the advantage of requiring less additional hardware—no second motor, drive, or controller would be needed. The dynamics of the load would also be easy to characterize, while a separate motor with a proprietary control system might be more opaque, and might have its own non-ideal characteristics such as cogging torque and ripple.

However, we chose to use an active load in order to be able to run other types of tests, such as simulating operation conditions in a prothetic device. A commercial motor and drive well-suited to the application also happened to be available in the laboratory, which reduced the cost and lead-time of this option.

A second design choice was whether to measure reaction torque between the ATFM case and motor ground using a load cell, or shaft-to-shaft torque between the ATFM and the load using a rotary torque sensor. The reaction torque configuration would have the drawback of measuring inertial torque from the ATFM rotor along with the electromagnetic torque of interest; meanwhile, the shaft-to-shaft torque configuration would have the drawback of also measuring bearing friction torque [20]. The shaft-torque configuration would have the
additional disadvantage of decreasing the stiffness of the coupling between the two shafts, due to both the length and compliance of the torque sensor itself. More compliance between the shafts is generally undesirable as it means lower-frequency resonances in the testbed.

The shaft-to-shaft torque measurement configuration was ultimately chosen in spite of these shortcomings, primarily because it was faster to build. Mounting the ATFM and the load cell properly would have involved ordering a number of parts machined at tolerances that were not achievable within the desired timeframe.

2.2 Testbed design

In the testbed that was built, the ATFM is mounted horizontally to a base plate and coupled to an opposing 2.9 kW commercial motor through a rotary torque sensor. The testbed is shown in Figure 2-3 and represented as a block diagram in Figure 2-1.

Opposite the ATFM is a 2.9 kW AC motor (model: SGMSV-30A3A61, Yaskawa America [21]) with 9.8 Nm rated torque. This motor was chosen because it had a power rating similar to the ATFM and sufficient torque to backdrive the ATFM at low currents without a gearhead, and because it was available. Also available was a 10:1 planetary gearhead (model: 042PLX, CGI [22]), raising the maximum continuous torque to 98 Nm when installed.

Both motors were bolted to 1/2" aluminum mounts, which were bolted to a 1/2" aluminum baseplate. Locating tabs and holes on the mounts and base ensured rough concentricity of the two motor shafts, and fine adjustments to vertical position were made with shims inserted between the mounts and the base. The baseplate was bolted to a wooden testbench below.

A rotary shaft-to-shaft torque sensor was coupled to the two shafts with bellows-style flexible couplings (models: BK5/300/114/25.4/BB & BK5/300/25/TS (Yaskawa side) and BK5/300/114/50/BB & BK5/300/25/TS (ATFM side), R+W America [23]). These couplings are zero-backlash and together compensate for up to 0.55 mm of lateral shaft misalignment. The couplings are split so that either bore can be replaced, and the two halves
are press-fit together under compression when installed. In an alternate configuration using the 10:1 gearhead, which has a slightly smaller shaft than the motor itself, half of the Yaskawa-side coupling can be replaced by another with the appropriate bore. In this configuration a 250-Nm rotary torque sensor (model: 64000-250Nm-0.1%, Sensy [24]) can be installed in place of the 20-Nm sensor for higher-torque experiments.

2.2.1 Limitations

The testbed does not meet the initial speed range requirement due to the limited range of available power supplies and current amplifiers. All of the experiments described in later chapters were performed at low velocity, but future testing at higher speeds would require higher-voltage supplies and drives. The initial 180 Nm continuous torque specification is also not met, even with the 250 Nm torque sensor installed, due to the 98 Nm limit on the Yaskawa gearhead. Higher-torque tests would require a higher-ratio gearhead or a motor rated for higher torque.

2.2.2 Drives

The Yaskawa motor is driven by a 200V AC ‘Servopack’ drive (model: SGDV-200A01, Yaskawa [21]). The drive is configured in and receives commands from Yaskawa’s SigmaWin+ utility.

Each phase of the ATFM is driven independently by a 90V, 36A Copley Accelus switched-supply amplifier (model: ASP-090-36, Copley Controls [25]). In this independent-phase-control configuration, each amplifier is configured as a brushless DC motor drive, so that it operates as a current amplifier. A software interface (model: CME2, Copley Controls) facilitates monitoring current and other signals with an oscilloscope utility, configuring current limits, and tuning the current loop.
2.2.3 Power

The Yaskawa drives run off wall power. Each ATFM amplifier is powered by a 30V, 2A linear supply (model: 1760A, BK Precision [26]) though they could be supplied higher voltages for higher-power experiments.

2.2.4 Sensing

The testbed is instrumented with sensors for measuring torque between the two motors, ATFM and Yaskawa rotor position, and internal ATFM temperature.

Torque

Shaft-to-shaft torque is measured with a 20-Nm rotary torque sensor (model: TRS300, p/n FSH01988 09-0323-99-06, Futek [27]). The sensor has +/-0.2% nonlinearity, +/-0.1% hysteresis, and +/-0.2% non-repeatability relative to the rated output.

Output is boosted to a +/-10V signal with a strain gauge amplifier (model: CSG110, p/n FSH01449, Futek) with 1 kHz bandwidth.

Strain gauge sensor output is antialias-filtered at 723 Hz with a first-order RC filter before being read by the controller ADC.

Position

A 20-bit absolute encoder mounted on the back of the Yaskawa motor provides position feedback to the Servopack drive. Position feedback from the ATFM comes from a 10000-line (40000-count) incremental optical encoder with an index pulse (disk model: DISK-2-10000-093-IE, reader model: US Digital EM2-2-10000-I, US Digital [28]). The disk is press-fit and glued onto a 3D-printed step-up shaft adapter, which is press-fit onto the short rear rotor shaft. The reader is fastened to the motor case with a two-part 3D-printed mount, so that
the reader can slide radially for easier installation and position adjustment (Figure 2-4). This encoder is read by the custom controller.

Temperature

Two K-type yellow thermocouples provide temperature readings inside the stator, which are read visually with digital display thermocouple amplifiers (model: RPK-PYRMTR, Geo Knight [29]).

2.2.5 Grounding

To avoid ground loops, component grounds are connected at a star point on the ATFM controller. The motor housing is connected to earth ground through the power supply for safety in case of a short between a winding and the case.

Digital and analog grounds ($V_{ssd}$ and $V_{ssa}$) are separated on the PSoC development board, meeting at the ground plane, and high-frequency PWM and encoder signals are
connected only to digital ground so as not to inject noise into analog readings.

2.3 Controller

A custom ATFM controller was made, with the requirements that it be able to take torque and position feedback and communicate them to a data-logging PC; that it be able to control the three phase amplifiers independently; and that there be no constraints on the commutation or feedback control strategy. This section describes the hardware (2.3.1), software (2.3.2), and signal conditioning (2.3.3) involved.

2.3.1 Hardware

Sending current commands to the Copley drives is a custom controller developed on a PSoC (Printed System-On-Chip) 5LP microcontroller (Cypress Semiconductor [30]) with a Cortex-M3 core. PSoC's are microprocessors with reconfigurable hardware modules for common functions such as communication (e.g. UART, SPI, USB), motor control (e.g. PWM, look-up tables), sensing (e.g. ADC, quadrature decoding, capacitive sensing), op-amps, etc. These modules are comprised of abstract ‘Universal Digital Blocks’ which are allocated at compile time to whichever modules are enabled. The PSoC Creator IDE includes a GUI for configuring hardware modules alongside firmware development. Firmware is written in C augmented by the PSoC API, and compiled with GCC 4.8.4 (Free Software Foundation [31]).

This project made use of the CY8CKIT-050 development kit, since there were no space or power constraints involved.

2.3.2 Controller operation overview

At runtime the controller updates position and torque measurements, computes and sends a current command to each of the amplifiers, and periodically sends data to a PC to be logged in MATLAB (MathWorks [32]). Upon reset it operates as follows:
Figure 2-5: Controller platform: CY8CKIT-050 development kit with PSoC 5LP
Figure 2-6: Controller block diagram. The controller consists of a quadrature decoder module for reading encoder feedback, an ADC module for torque feedback (with some signal conditioning), three PWM modules for sending current commands to the amplifiers, and a UART block for data transfer to a PC.
At startup:

- Initialize system components (ADC, op-amps, PWMs, UART, quadrature decoder) and variables. Enable ISRs
- Wait for user to press 'Master Enable' button allowing operation to begin
- If index pulse on quadrature encoder has not yet been seen, wait for it to pass so that absolute position is known (this requires the rotor shaft to be turned manually or by the opposing motor at each startup, which is acceptable for testing purposes)
- Enable all three Copley amplifiers by setting hardware-enable input pins low

Main loop:

- At 10 kHz:
  - 100-us PWM period ends, triggering start of ADC conversion
  - ADC conversion ends, triggering high-priority interrupt ADC.EOC.ISR ('Analog-to-digital converter end-of-conversion interrupt service routine'). In this ISR:
    * Read ADC value and convert to torque
    * Read encoder value
    * Calculate desired current based on some control strategy, using some reference value, current and previous measurements and errors, and some set of gains
    * Saturate desired current to maximum/minimum allowable value
    * Convert current command to a PWM compare value for each phase using commutation look-up tables
    * Update PWMs with new compare values
    * Update variables that keep track of previous values, for use in the control calculation
  - Once every 10 times (at 1 kHz):
* Update global variables with latest values to be logged

* Set a 'comm sample ready' semaphore, indicating to the main loop that there is new data to be logged

** Background tasks: **

- When 'comm sample ready' semaphore is set (at 1 kHz), create a packet containing the latest data to be logged, and add it to a circular comm buffer where it will wait to be sent. Set the 'comm sample ready' semaphore back to zero

- Whenever there is room in the UART buffer and data in the comm buffer, send a packet out to data-collection computer

All calculations in the high priority ADC.EOC.ISR are executed in integer arithmetic for speed.

### 2.3.3 Torque sensor signal conditioning

The analog torque sensor output is converted to a digital value at 10 kHz with a 12-bit SAR (successive approximation register) ADC. The reading is made relative to half supply \( \left( \frac{V_{dd}}{2} \right) \), so that the voltage range \( (V_{ssa}, V_{dda}) \) maps to (-1024, 1024) counts.

The strain gauge amplifier on the torque sensor has an output voltage range of +/- 10 V with respect to \( V_{ssa} \), but ADC input must be between \( V_{ssa} \) and \( V_{dda} \) (\( = V_{ssa} + 5 \)), so the signal is conditioned as shown in Figure 2-6.

With the VDAC offset voltage set to 1.808V, +/- 10V sensor output voltage is inverted, shifted, and scaled to a 0.001 - 4.411V ADC input voltage.

The precise formula for input voltage \( V_{in} \) as a function of ADC counts \( c \) was found through calibration to be

\[
V_{in} = -0.005437c + 9.890148
\]
Using factory calibration data for the torque sensor to obtain a linear fit of the relationship between torque and amplifier voltage, the formula for torque $\tau$ as a function of ADC counts $c$ was derived:

$$\tau = -0.010861c + 19.767958$$

### 2.4 Safety

As a research tool, this testbed lacks many of the safety features that should be present in a commercial device. However, the system still needs to avoid damaging the motor by sending too high a current through the windings. The amplifiers are therefore configured to limit maximum instantaneous and continuous current to safe levels (ones that will neither damage the windings nor produce more instantaneous torque than the torque sensor can handle). Additionally, it is desirable to prevent the amplifiers from erroneously commanding maximum current. This could occur if, for instance, the PWM signal or ground line between the amplifier and the controller became physically disconnected, causing the amplifier to read a constant 100% or 0% duty cycle, corresponding to full positive or negative current. This could also occur if an amplifier were turned on while the controller were off, so that the PWM signals were left floating. To prevent this, the amplifiers have hardware-enable inputs which are configured to be off by default (active-low inputs with internal pull-ups), and which can only be enabled if the controller is connected, powered on, and actively driving them low. A software reset in the controller automatically disables the amplifiers, and the controller waits for a manual ‘amp enable’ button-press before enabling them.
Chapter 3

Motor characterization

This chapter describes important characteristics of the ATFM and the procedures followed to measure them, including winding impedances (Section 3.1), ‘torque constant’ functions (Section 3.2), and zero-current torque (Section 3.3).

3.1 Winding impedances

Motor windings, as coils of wire wrapped around a low-reluctance material, are typically characterized by their resistance and inductance.

**Resistance** The resistance of each phase winding was measured by applying a constant voltage and measuring current through and voltage across the winding, using two voltmeters. Three measurements at different current levels were taken and averaged for each phase. Winding resistance was determined to be 0.59 Ohms on all three phases.

**Inductance** Winding inductance for each of the phases were measured independently by applying a step in voltage (0 to 2.5V) across the winding and measuring the time constant of the current rise through a sense resistor. Rotor position was held constant
by the opposing motor during these measurements. Under these conditions winding
inductance was measured to be 9.9 mH on phase A, 10.0 mH on phase B, and 11.3
mH on phase C.

3.2 Torque constant

The torque constant $K_t$, representing torque (Nm) generated per unit current (A) as a
function of angle, was measured for each phase independently. First, with the ATFM rotor
being driven by the opposing motor at a low constant speed (1 rpm, forward) and the
ATFM leads open, torque was measured as described in Section 3.3. Then, a low constant
current (1 Amp) was applied to one phase at a time with the opposing motor still driving
the rotor at 1 rpm, and output torque was measured and averaged as a function of position
over 10 revolutions for each phase. $K_t$ for each phase was then estimated as the difference
between the average torque waveform with and without current, divided by the magnitude
of the phase current. These waveforms are shown in Figure 3-1.

The torque constant $K_t$ typically refers to the motor as a whole, rather than to a single
phase. In an ideal three-phase sinusoidally-wound motor, $K_t$ describes the total output
torque produced by a total input current of 1 A, applied as balanced three-phase sinusoidal
currents. To the extent that the per-phase $K_t$ functions in a real motor are approximately
sinusoidal, spaced 120° apart, and the same amplitude, $K_t$ for the motor as a whole is equal
to 1.5 times the peak value of the $K_t$ function for a single phase. Fitting a sinusoid to the
$K_t$ function for each phase, the average amplitude is 4.84 Nm/A (A: 4.70 Nm/A, B: 4.74
Nm/A, C: 5.08 Nm/A), so the torque constant for the whole motor is approximately 7.26
Nm/A.

3.3 Zero-current torque

Cogging torque refers to torque that develops when the rotor deviates from a local minimum
reluctance position. Cogging torque can be modeled ideally as a function of position alone,
Figure 3-1: Measured $K_t$ function for each phase as a function of rotor position over a full revolution. Under the ideal motor model, these waveforms represent torque produced by each phase per unit current through that phase at a given angle. They are theoretically equivalent to back-EMF divided by rotor velocity (mechanical).
independent of velocity or phase current, and should add linearly with electromagnetic torque (torque produced by current through the windings).

High cogging torque is typically expected in transverse-flux machines due to large variations in the reluctance of the pole ring. While it was not possible to measure cogging torque in isolation without disassembling the motor, it was possible to measure total zero-current torque as a function of rotor angle, using a low constant velocity in order to minimize contributions from frictional and hysteresis torques.

Zero-current torque in the ATFM was measured with the opposing motor maintaining a rotation velocity of 1 rpm, and with no current on any of the ATFM phases (motor leads open). Torque was measured as a function of position over 10 revolutions, filtered at 50 Hz with a second-order Butterworth filter, and averaged across all revolutions. The experiment was performed in both rotation directions.

Whereas cogging torque theoretically has a DC value of zero and is independent of rotation direction, the data shows a non-zero DC value as well as direction-dependence, indicating that other types of torque are present (Figure 3-2). Figure 3-3 shows the AC components of each torque profile in detail. As shown, the waveforms for each direction are consistent across revolutions, but differ both in magnitude and in shape depending on the direction of rotation. The DC components of the waveforms also differ: 3.82 Nm for forward rotation vs -2.86 Nm in reverse. The zero-current torque measured in this experiment therefore represents more than cogging and bearing friction (Coulombic and viscous; there should be no stiction during this experiment since the rotor is in continuous motion.) It is possible that hysteresis in the core contributes additional torque with both AC and DC components.
Figure 3-2: Zero-current torque as a function of rotor angle, measured with the ATFM rotating at a constant 1 rpm over a full revolution. The torque waveforms differ in shape, amplitude, and DC offset depending on the direction of rotation.
Figure 3-3: Zero-current torque detail, showing AC components only over one tenth of a revolution in both rotation directions at 1 rpm. The unexpected differences in shape between the two directions are evident.
Chapter 4

Motor control

Autonomous wearable lower-extremity devices require actuators capable of smooth, accurate torque control. Torque ripple, the amount of variation in open-loop output torque due to imperfect winding shapes, makes this more difficult. In this chapter, we describe how commutation currents may be optimized to minimize ripple in the ATFM compared to standard permanent magnet synchronous machine commutation (balanced three-phase commutation, also referred to here more briefly as 'sinusoidal' commutation). The trade-off for this ripple reduction is that a more complex drive is required in order to control the three motor phases independently.

Section 4.1 shows performance limits for torque maximization and ripple minimization in the ATFM using balanced three-phase commutation. Section 4.2 presents simulation and experimental results showing that, while mean torque cannot be increased by commutating with independent phase control, it is possible to decrease torque ripple using this strategy without sacrificing average torque output or increasing conduction losses.
4.1 Baseline: Balanced three-phase sinusoidal commutation

4.1.1 Theory

Three-phase permanent magnet synchronous machines are typically commutated with a sinusoidal current through each winding. An ideal three-phase AC motor has perfectly sinusoidal windings, meaning that the torque produced by each phase $\varphi$ is proportional to current through that phase and sinusoidal in rotor angle, with amplitude $K_{t\varphi}$. Ideally the three sinusoids $K_{t\varphi}(\theta)$ are equally-spaced, that is, offset 120° from each other. If this is the case, then driving each phase with unit-amplitude sinusoidal currents in phase with the $K_{t\varphi}(\theta)$ function for each phase will generate three sine-squared torque waveforms, which sum to a constant torque of $1.5K_t$ Nm.

It is convenient that three equally-spaced sinusoids of equal magnitude and frequency, such as the phase currents just described, sum to zero for all $\theta$. This result means that it is possible to drive only two of the three windings independently, as long as the third winding is connected so that the current through it is the sum of the currents through the other two windings. One way to impose that constraint is to connect the three negative motor leads together in a so-called ‘wye’ (Y) configuration. Having only two windings to drive independently instead of three makes it possible to use drives that are smaller, lighter, and cheaper.

If the three $K_{t\varphi}(\theta)$ functions are ideal – perfectly sinusoidal, of exactly equal amplitude, and precisely spaced at 120° from each other – then driving the motor with wye-connected leads is equivalent to controlling the windings independently, so in theory there is no cost to using the simpler drive. But the non-idealities in real motors appear as torque ‘ripple,’ which is the amount of variation in open-loop output torque due to imperfect winding shapes, and a diminished average output torque. If there are significant non-idealities present in a certain motor, it might be preferable to drive the three phases independently, so that the current waveforms can have arbitrary shape. This type of commutation will be referred to here as ‘independent phase’ or ‘arbitrary-waveform’ commutation.
4.1.2 Torque maximization

In order to commutate properly as described in the last section, the relationship between rotor position as measured with the encoder and electrical angle as defined by the torque constant functions $K_{tp}$ must be known. The $K_{tp}$ waveforms for all three phases as a function of rotor angle were measured as described in Section 3.3. In an ideal motor with perfectly balanced sinusoidal windings, the current through each winding would be in phase with the $K_{tp}$ function for that phase; this would maximize mean torque and result in zero ripple.

This solution for maximizing mean torque over a full revolution for a given peak current – that currents be 'in phase' with the torque constant functions – is no longer well-defined once we relax the assumption that the $K_{tp}(\theta)$ functions are ideal. With balanced three-phase commutation, this optimization now has one degree of freedom, since the current waveforms must be 120 electrical degrees apart and have equal frequency and amplitude, but can have an arbitrary phase offset. For the ATFM, the current phasing $\phi_0$ that maximized mean torque over one revolution was found numerically, to the accuracy of the encoder, under the assumption that total torque is a linear combination of phase currents:

$$
\tau(\theta) = K_{ta}(\theta)\sin(N\theta + \phi_0) \\
+ K_{tb}(\theta)\sin(N\theta - 2\pi/3 + \phi_0) \\
+ K_{tc}(\theta)\sin(N\theta + 2\pi/3 + \phi_0)
$$

(4.1)

where $N = 30$ is the number of pole pairs. The optimized result is shown in Figure 4-1, with mean torque 7.240 Nm.

This optimization ignores torque ripple. It is generally desirable to reduce ripple, but under the assumption that phase torques add linearly, this is not possible using balanced three-phase commutation. Varying the phase offset of the winding currents only affects the DC torque value and does not change its AC components. Absolute torque ripple, defined as the difference between maximum and minimum torque over a revolution, is therefore constant with respect to current phase offset; and relative ripple, defined as absolute torque
Figure 4-1: Simulated results of torque-maximizing optimization using balanced three-phase commutation. This figure spans only one tenth of a revolution in order to show detail.

ripple as a percentage of mean torque, is necessarily minimized when mean torque is at its maximum.

4.2 Commutation with independent phase control

If we assume an ideal drive with infinite bandwidth and range, we can consider what current waveforms would result in the best performance for the ATFM based on the measured torque constant functions for the windings. Since balanced three-phase commutation is an element of the set of all possible current waveforms as a function of position, performance can only be improved by commutating with properly-chosen arbitrary waveforms.
4.2.1 Torque maximization

One improvement we might seek to make is to increase mean torque without increasing losses. While total losses are a nonlinear function of many variables, we can partially represent it by conduction losses, which are often significant relative to others.

Formulation

The optimization was formulated as a linear optimization with a quadratic constraint, with mean output torque as the objective function to be minimized (equivalent to maximizing negative torque), and with average instantaneous conduction losses constrained to be less than or equal to those in the sinusoidal commutation case. With $K_{tA}$, $K_{tB}$, and $K_{tC}$ as measured in Section 3.3 and expressed as discrete functions of rotor angle using encoder counts as the index, the objective function is

$$\text{min}(-\bar{T})$$  \hspace{1cm} (4.2)

such that

$$\bar{T} = \frac{1}{nP} \sum_{p \in \{A,B,C\}} \sum_{k=1}^{N} K_{\theta_k} i_{\theta_k}$$  \hspace{1cm} (4.3)

$$= \frac{1}{n} K_t^T \cdot i$$

where

$$K_t = \begin{bmatrix} K_{tA} \\ K_{tB} \\ K_{tC} \end{bmatrix}$$  \hspace{1cm} (4.4)

and
\[
    i = \begin{bmatrix}
    i_A \\
    i_B \\
    i_C 
\end{bmatrix}
\]  

(4.5)

where \(K_{tA}, K_{tB}, \) and \(K_{tC}\) are vectors of \(K_t\) values for each phase, indexed by discretized rotor position, and \(n\) is the number of encoder counts per revolution. \(i_A, i_B, \) and \(i_C\) are likewise vectors of current values for each phase.

The conduction loss constraint is:

\[
\frac{i^T i R}{n} \leq P_{\text{sin}} 
\]

(4.6)

where \(R\) is winding resistance and \(P_{\text{sin}}\) (W) is average power dissipation in the windings over one revolution for the sinusoidal commutation case.

This optimization was solved using Matlab’s \textit{fmincon}, with Hessians pre-computed for efficiency. The full mechanical revolution had to be split into five parts for computation speed. Results were consistent across three sets of initial conditions ((1) constant zero Amps on all phases, (2) constant two Amps on all phases, and (3) optimized sinusoidal currents) to add confidence that the solution was not a local minimum.

Simulation results

Optimized current waveforms and the resulting output torque over one revolution are shown in Figure 4-2 and Table 4.1. We see that only negligibly (1%) better average open-loop torque can be achieved with this strategy than with optimized balanced three-phase commutation for the same conduction losses. (Incidentally, torque ripple also increases with the optimized commutation waveforms, though no improvement was expected since ripple was not included in this optimization.)
Figure 4-2: Results of torque-maximizing optimization, showing both arbitrary and sinusoidal current waveforms and the respective torque outputs. This figure spans only one tenth of a revolution in order to show detail.
### Table 4.1: Simulated results using balanced three-phase (sinusoidal) and independent phase (arbitrary-waveform) commutation optimized for maximum output torque. The optimized arbitrary waveforms fail to provide greater mean torque than balanced three-phase commutation under the constraint that conduction losses do not increase.

<table>
<thead>
<tr>
<th></th>
<th>Balanced three-phase commutation</th>
<th>Arbitrary current commutation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mean output torque, Nm</td>
<td>7.24</td>
<td>7.27</td>
</tr>
<tr>
<td>Ripple (max-min), Nm</td>
<td>0.78</td>
<td>1.36</td>
</tr>
<tr>
<td>Ripple (% mean torque)</td>
<td>10.8</td>
<td>18.6</td>
</tr>
<tr>
<td>Ripple (torque variance)</td>
<td>0.0293</td>
<td>0.0988</td>
</tr>
<tr>
<td>Mean $I^2R$ losses (W)</td>
<td>0.90</td>
<td>0.90</td>
</tr>
</tbody>
</table>

4.2.2 Ripple minimization

With arbitrary waveform commutation, it is nonetheless possible to reduce torque ripple without increasing conduction losses relative to balanced three-phase commutation, and while maintaining the same mean output torque over a revolution.

**Formulation**

Although ripple minimization was the goal, this optimization was simplest to formulate as a maximization of mean torque, subject to the constraint that torque ripple $r$ be no greater than some value, iterating over increasing values starting at zero; and with the constraint that conduction losses be no greater than in the optimized sinusoidal commutation case. The objective function was thus the same as in the previous optimization (Eq. 4.2). The conduction loss constraint also remained the same (Eq. 4.6).

For the purpose of this optimization, ripple was defined as the variance of the torque output over a full revolution:

$$r \equiv \frac{1}{n} \sum_{i=1}^{n} (\tau_{i} - \bar{\tau})^2$$  \hspace{1cm} (4.7)

A more common definition of ripple is the difference between minimum and maximum torque, but that definition is perhaps less informative here, since it depends only on outliers.
Using the expression for $\tau$ from Eq. 4.3, Eq. 4.7 becomes

$$r = \frac{1}{n} \sum_{i=1}^{n} \left( \tau[\theta_i] - \frac{1}{40000} K^T_i \cdot i \right)^2$$

$$= \frac{1}{n} i^T G^T G i$$

where

$$G = K_1 - \frac{1}{n} K_2$$

for

$$K_{1(nx3n)} = \begin{bmatrix} K_{tA_1} & 0 & K_{tB_1} & 0 & K_{tC_1} & 0 \\ \vdots & \ddots & \ddots & \ddots & \ddots & \vdots \\ 0 & K_{tA_n} & 0 & K_{tB_n} & 0 & K_{tC_n} \end{bmatrix}$$

and

$$K_{2(nx3n)} = \begin{bmatrix} K_i^T \\ \vdots \\ K_i^T \end{bmatrix}$$

A solution was found using the same procedure as in the previous optimization (Section 4.2.1).

**Simulation results**

With commutation optimized for low ripple, it is thus theoretically possible to eliminate torque ripple – whether defined as peak-to-peak torque or as torque variance – without
Figure 4-3: Simulated results of ripple-minimizing optimization, showing both arbitrary and sinusoidal current waveforms and the respective torque outputs. This figure spans only one tenth of a revolution in order to show detail.

<table>
<thead>
<tr>
<th></th>
<th>Balanced commutation</th>
<th>three-phase Arbitrary commutation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mean output torque, Nm</td>
<td>7.24</td>
<td>7.24</td>
</tr>
<tr>
<td>Ripple (max-min), Nm</td>
<td>0.78</td>
<td>0.00</td>
</tr>
<tr>
<td>Ripple (% mean torque)</td>
<td>10.7</td>
<td>0.0</td>
</tr>
<tr>
<td>Ripple (torque variance)</td>
<td>0.0293</td>
<td>0.0000</td>
</tr>
<tr>
<td>Mean $I^2R$ losses (W)</td>
<td>0.90</td>
<td>0.89</td>
</tr>
</tbody>
</table>

Table 4.2: Simulated results using balanced three-phase (sinusoidal) and independent phase (arbitrary-waveform) commutation optimized for minimum torque ripple. In simulation, the arbitrary waveforms reduce ripple without diminishing mean output torque or increasing conduction losses relative to sinusoidal commutation.
sacrificing mean output torque and without increasing conduction losses relative to balanced three-phase sinusoidal commutation.

<table>
<thead>
<tr>
<th></th>
<th>Balanced three-phase commutation</th>
<th>Arbitrary current commutation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Simulation</td>
<td>Experiment</td>
</tr>
<tr>
<td>Mean output torque, Nm</td>
<td>7.24</td>
<td>7.21[0.00]</td>
</tr>
<tr>
<td>Ripple (max-min), Nm</td>
<td>0.78</td>
<td>0.93[0.04]</td>
</tr>
<tr>
<td>Ripple (% mean torque)</td>
<td>10.8</td>
<td>12.9[0.6]</td>
</tr>
<tr>
<td>Ripple (torque variance)</td>
<td>0.0293</td>
<td>0.0304[0.0002]</td>
</tr>
</tbody>
</table>

Table 4.3: Simulated and experimental torque output using balanced three-phase (sinusoidal) and independent phase (arbitrary-waveform) commutation optimized for minimum torque ripple. The optimized arbitrary current commutation strategy reduces ripple in both simulation and experiment, though ripple is larger in the experimental data than in simulation for both types of commutation. Experimental results are the average of ten revolutions at 1 rpm and are reported as mean[standard deviation].

**Experimental results**

Experimentally-measured torque ripple using arbitrary currents optimized for minimum ripple is greater than in simulation (see Figure 4-4, Table 4.3) but still represents a reduction relative to balanced three-phase commutation, however we define ripple. Torque ripple decreases from 0.93 Nm to 0.56 Nm as the difference between maximum and minimum torque \((p < 0.0001)\), from 12.9% to 7.8% as a percentage of mean output torque \((p < 0.0001)\), and from 0.0304 to 0.0062 as measured by torque variance \((p < 0.0001)\). Mean output torque agrees well with the simulation value (7.21 Nm measured, 7.24 Nm predicted).

This data was obtained by commanding the optimized currents with the ATFM back-driven at a constant 1 rpm by the opposing motor over ten revolutions. The output was filtered at 50 Hz with a second-order Butterworth filter to remove switching noise, and the average measured zero-current torque waveform was then subtracted from this result respect to position to obtain torque due to phase currents.

The discrepancy between the simulated and measured torques is likely due in part to limited current amplifier bandwidth, which the optimization did not account for.
Figure 4-4: Experimental and simulation results for both commutation strategies shown over a full revolution.
Another reason for this discrepancy may be the result of interactions between the motor phases. Phase B shares poles with phases A and C, and the linear superposition model in which total torque is the sum of $K_t \phi_i \phi$ over all phases $\phi = A, B, C$ has limited validity for this motor.

Quantization error in the current commands and in position measurement also accounts for some this error, though only a small part. Including quantization error in the simulation for arbitrary-waveform commutation yields 1.3% ripple (though still a torque variance of 0.0000) and mean torque of 7.22 Nm over one revolution.

4.2.3 Limitations

A primary limitation of this optimization is that it does not account for drive bandwidth or for supply voltage limits, which limit the maximum allowable rate of current change. It also does not account for core losses, current limits, or phase interactions.

This optimization also requires the $K_t \phi$ function for each phase as inputs, so that it is necessary to characterize the motor prior to running the optimization. Depending on how much of the non-ideality in these functions comes from low manufacturing tolerances, as opposed to the unusual motor geometry, currents optimized for one ATFM may not reduce ripple in another.
Chapter 5

Conclusion and Future Work

In this thesis we developed a testbed for, characterized, and tested an optimized commutation strategy for a prototype 'axial transverse-flux' permanent magnet synchronous motor. The prototype, while too large for prosthetics applications itself, was of potential interest in designing autonomous lower-extremity prostheses, due to its topology's nominally high torque density and low winding resistance. We built a dynamometer capable of driving the ATFM at fixed velocity or torque, instrumented with torque and position feedback, a custom PSoC-based controller, and a drive capable of controlling the three phases separately. Using this testbed we measured zero-current torque, which included a large AC component. This result was expected due to the motor's transverse-flux topology, which is often characterized by significant cogging torque. We also identified torque constant waveforms for each phase independently. Using this information we numerically optimized current waveforms for mean torque maximization using both wye-connected leads and independent phase control, and determined that no significant increase in torque could be achieved by driving the phases independently, except at the cost of higher conduction losses. However, we determined and confirmed experimentally that torque ripple could be reduced (and with an ideal drive eliminated) using independent phase control, without sacrificing mean torque or increasing conduction losses.

Future work to characterize the ATFM prototype includes investigating phase interac-
tions and the effect of flux-sharing in the rotor rings; characterizing the thermal path and measuring the effect of temperature rise on torque characteristics; measuring and identifying losses at higher torques and speeds; measuring efficiency; testing a wider range of strategies for reducing torque ripple, including strategies that account for other important factors such as supply voltage; investigating strategies for mitigating the significant cogging torque; and examining ways to model the behavior of the motor in spite of its many asymmetries.
Bibliography


