

Real-Time eVTOL Powertrain Modeling for the NASA Vertical Flight Simulator

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ABSTRACT

This paper presents the development, verification, and validation results of an electrical Vertical Take-off and Landing (eVTOL) powertrain model. To better understand the potential impact of powertrain limitations on eVTOL aircraft handling qualities, a powertrain model was developed, integrated into revolutionary vertical lift technology (RVLT) reference vehicle designs, and tested in the National Aeronautics and Space Administration (NASA) Ames Vertical Motion Simulator (VMS). The high computational complexity required to capture the relevant powertrain physics may conflict with the ability to execute the simulation models in real time. In this paper, the authors present models and modeling decisions related to motors, batteries, and interconnections. Physics-based models and empirical models are used in tandem to support the modeling effort. Simulated motor-data comparisons are made to data collected from the NASA Scaled Power Electrified Drivetrain (SPEED) and Advanced Reconfigurable Electrified Aircraft Lab (AREAL) testbeds. The empirical battery model is compared to experimental 18650 battery data.

NOTATION

Symbols

A_c	Surface area of battery cell
A_{pk}	Peak Amps
α	Current angle
α_a	Current amplitude correction
α_c	Copper coefficient
α_l	Speed loss coefficient
α_{pm}	Permanent magnet coefficient
a_{rc}	Constant coefficient
α_s	Speed dependent loss power ratio
B	Viscous damping coefficient
b_{rc}	Linear coefficient with motor speed for R_{rc}
C_c	Stator core thermal capacitance
C_{fa}	Fast transient battery capacitance
C_l	Battery cell C-limit
C_r	Rotor thermal capacitance
C_{sl}	Slow transient battery capacitance
C_w	Winding thermal capacitance
c_{rc}	Quadratic coefficient with motor speed for R_{rc}
E	Back-EMF
F	Faraday constant
G_{inv}	Inverter voltage output gain
G_r	Gear ratio
g	Flux weakening gain

h	Convective heat transfer coefficient
I	Current
I_A	Phase A current
I_B	Phase B current
I_C	Phase C current
I_b	Battery block current
I_c	Battery cell current
I_d	D-axis stator current
$\dot{I}_d$	Derivative of D-axis stator current
I_l	Current limit
I_m	Motor current
I_q	Q-axis stator current
$\dot{I}_q$	Derivative of Q-axis stator current
I_{rl}	Current rate limit
I_s	Stator current
J_m	Motor inertia
J_r	Aerodynamic Rotor inertia
K	Kelvin
K_{fw}	Flux weakening integral gain
k_h	Hysteresis coefficient
L_d	D-axis rotor self-inductance
L_{dq}	DQ-axes cross inductance
L_q	Q-axis rotor self-inductance
L_{qd}	QD-axes cross inductance
M	Modulation ratio

m	Battery mass
mol	Mole
N_p	Number of battery packs in parallel
N_s	Number of cells in series in a battery pack
n_e	Number of transferred electrons
n_p	Number of pole pairs
P_f	Power factor
P_H	Heating losses
P_I	Input power
P_L	Motor load power
P_o	Motor output power
P_R	Motor power required
Q_c	Convective cooling
Q_e	Entropy heating
Q_h	Hysteresis heating power
Q_i	Resistive heating power
Q_{op}	Overpotential heating
Q_r	Radiative heating
Q_v	Dynamic viscous heating power
R	Resistance
R_c	Battery cell resistance
R_{cc}	Coolant and stator core thermal resistance
R_{fa}	Fast transient battery resistance
R_{rc}	Motor rotor and stator core thermal resistance
R_s	Stator resistance
R_{sl}	Slow transient battery resistance
R_{wc}	Winding and stator core thermal resistance
S	Entropy
S_{oc}	State of charge
t	Time
T	Temperature
T_a	Ambient temperature
T_c	Stator core temperature
T_{co}	Coolant temperature
T_r	Rotor temperature
T_s	Battery surface temperature
T_w	Winding temperature
u_f	Flux weakening control law command
V_A	Phase A voltage
V_B	Phase B voltage
V_C	Phase C voltage
V_b	Battery voltage at inverter terminal
V_c	Voltage of battery cell
V_{car}	Carrier voltage
V_d	D-axis stator voltage
V_{fa}	Fast dynamic voltage
V_{ir}	Load voltage
V_l	Voltage limit
V_m	Motor design voltage
V_{oc}	Open circuit cell voltage
V_{pk}	Peak volts
V_q	Q-axis stator voltage
V_{rl}	Voltage rate limit
V_s	Stator voltage
V_{sl}	Slow transient battery voltage
V_t	Temperature bias voltage
W	Watt

α	Current angle
α_a	Current amplitude correction to current
α_c	Copper coefficient
α_l	Speed loss coefficient
α_{pm}	Permanent magnet coefficient
α_s	Speed dependent loss power ratio
β_t	Temperature correction to current
$\Delta\tau$	Torque difference
δ	Voltage angle
ε	Battery cell surface emissivity factor
η_I	Inverter efficiency
η_m	Motor efficiency
η_o	Overall efficiency
η_t	Total overpotential
ϑ	Current control angle
λ	Motor flux linkage
μH	microhenry
σ	Stefan-Boltzmann constant
τ_{aero}	Aerodynamic shaft torque
τ_c	Coulomb friction loss
τ_e	Electromagnetic torque
τ_f	Motor torque loss
τ_I	Input torque
τ_l	Torque limit
τ_L	Load torque
τ_m	Motor torque
φ	Power factor angle
θ	Electrical motor rotor angle
ω_b	Base speed
ω_c	Current filter frequency
ω_{fw}	Flux weakening filter frequency
ω_m	Rotational mechanical motor speed
ω_r	Aerodynamic rotor speed

Superscripts

b	Motor base condition
nl	Motor no-load condition
c	Command
p	Motor peak condition
pk	Peak
r	Motor-rated condition
pr	Preliminary calculation
t	Temperature updated parameter
20	Evaluated at a rotor temperature of 20 °C
25	Evaluated at a winding temperature of 25 °C

Operators

$(\cdot)_{act}$	From actual motor
$(\cdot)_{th}$	From thermal model
Δ	Change in

Key Acronyms

A	Amperes
ABC	120-deg phase separated frame
AC	Alternating current
ADS	Aeronautical Design Standard
Ah	Ampere-hour
AREAL	Advanced Reconfigurable Electrified Aircraft Lab

BLDC	Brushless direct current
°C	Degrees centigrade
D	Direct
DC	Direct current
DQ	Direct quadrature
EMF	Electromotive force
eVTOL	electric Vertical Take-Off and Landing
fps	Feet-per-second
ft	Feet
ft-lb	Foot-pound
FW	Flux weakening
g	gram
hp	Horsepower
H	Henry
HQL	Handling Qualities Lab
IGBT	Insulated-Gate Bipolar Transistor
J	Joule
K	Kelvin
kHz	Kilohertz
kOhm	KiloOhm
lb	Pound
LiPo	Lithium-ion
mag	Magnitude
Max	Maximum (used in F7,
MCP	Maximum continuous power
MCT	Maximum continuous torque
min	Minutes
MJ	Megajoules
mol	Mole
MRP	Maximum Rated Power
MTPA	Maximum torque per amp
NASA	National Aeronautics and Space Administration
NDARC	NASA Design and Analysis of Rotorcraft
NFW	No flux weakening
Nm	Newton-Meters
PMSM	Permanent Magnet Synchronous Motor
PI	Proportional Integral
Q	Quadrature
RTPM	Real-Time Powertrain Modeling
RVLT	Revolutionary Vertical Lift Technology
s	Second
SPEED	Scaled Power ElEctrified Drivetrain
SVPWM	Space Vector Pulse Width Modulation
sin	Sine
V	Volts
VMS	Vertical Motion Simulator

INTRODUCTION

Electric and hybrid propulsion is of paramount importance for operations of electric Vertical Take-off and Landing (eVTOL) aircraft. The National Aeronautics and Space Administration (NASA) is studying how handling qualities may be impacted by rotorcraft vehicle design choices (Refs.

1 and 2). The impact of powertrain failures on pilot handling qualities has been studied by NASA Revolutionary Vertical Lift Technology (RVLT) researchers (Ref. 3).

Designers of eVTOL vehicles must carefully balance the objectives of range and payload against agility and maneuverability. Weight limitations may lead designers to size powertrains to meet performance requirements with little additional margin. Off-nominal conditions such as a low-battery state of charge or overheated components may drive powertrains to the edges of their component envelopes. This paper presents the path researchers followed to investigate the effects on powertrains operating at their capacity limits in the NASA Ames Vertical Motion Simulator (VMS) (Moffett Field, California) for piloted simulations.

Powertrain components such as electric motors, gear boxes, batteries and/or turbine generators operate to produce shaft torque to meet aerodynamic rotor torque requirements and maintain controlled maneuvering flight. From a flight controls perspective, the powertrain is a complicated actuator with state-based torque limitations which can restrict rotorcraft maneuver performance under certain conditions (Ref. 4).

A key aspect to simulating an electric powertrain is the sizing routine used to model the electric motor coefficients linking the powertrain back to the vehicle sizing program – in this case the NASA Design and Analysis of Rotorcraft (NDARC) tool (Ref. 5). The NDARC has been used to size RVLT reference aircraft designs, such as the battery-electric lift-plus-cruise variant (see Ref. 3). Additionally, the powertrain model must include enough supporting components as that of the motors to capture those aspects of the dynamic response that are relevant to the objectives of the simulation. The components include, but are not limited to, thermal models and battery models.

Models supporting an eVTOL-piloted simulation must operate in real time. Due to high power-to-weight ratios and broad industry acceptance and utilization, permanent magnet synchronous motors (PMSMs) were chosen for the modeling effort. The work, however, is general enough to apply to brushless direct current (BLDC) motors, which require a different inverter wave form. Real-time implementation of models of PMSMs (Ref. 6) presents challenges. The inverter gate switching logic is typically in the 10- to 100-kHz range. High time constants exist in PMSM motor electrical circuitry; complex interactions occur between motor speed and motor current. Given the complexities, high sample rates are required for numerical stability.

The objectives of this study are to present the components and modeling decisions made for the Real-Time Powertrain

Modeling (RTPM) tool. A centerpiece in this paper is the motor sizing process, which is used to process the NDARC vehicle powertrain information. The importance of the motor sizing process is paramount because the limits on torque as a function of motor size directly translate into torque margins and affect vehicle handling qualities. Supporting powertrain components such as batteries and thermal modeling further refine the torque limitations; therefore, the modeling efforts on these components are also covered in detail. To challenge the modeling effort, various experimental tests and simulation comparisons are set up where appropriate.

A companion to this paper has been published to Vertical Flight Society (VFS), Forum 81 (Ref. 7). Pilot testing occurred in the autumn of 2022 using RTPM in the VMS. The companion paper documents the effect of the off-nominal powertrain on pilot handling qualities. The modeling was accomplished with an initial version of RTPM, which is very similar to that which has been presented in this paper.

METHODOLOGY

When sizing an electric vehicle, NDARC reports motor specification speed, maximum rated power (MRP), maximum continuous power (MCP), peak torque, and a power limit defined from peak torque at the specification speed. This section addresses how to interpret these powertrain parameters into a computational model for simulation. The first step is to use NDARC motor specifications to derive motor coefficients, such as stator resistance and flux linkage, by way of motor physics equations. Given motor coefficients, the motor limits are then determined. Following the limit discussion, the RTPM simulation architecture is presented. Next, the component models that make up the RTPM architecture are presented.

Motor Map Development

A conceptual motor torque-speed map depicting motor limitations in both speed and torque load is provided in Figure 1. Sustained motor torque in the intermittent operations region of the conceptual motor map leads to sustained motor component temperature increases beyond motor temperature rating, which may reduce motor reliability over time; therefore, a useful practice is to size motors so that they primarily operate within the Continuous Operations region of the motor map.

Figure 1 shows that the torque margin (TM) and speed margin (SM) determine electric rotorcraft performance and are therefore crucial for control law design. One way to achieve higher SM or TM would be to use a battery with higher voltage than the motor-rated voltage. The increased voltage may allow peak torque to be achieved at higher speeds than the base speed, even up to the rated speed, assuming temperature limits are not exceeded. A second way to increase allowable TM and SM is by using flux weakening (FW) inverter control technology. The FW (Ref. 8) control laws

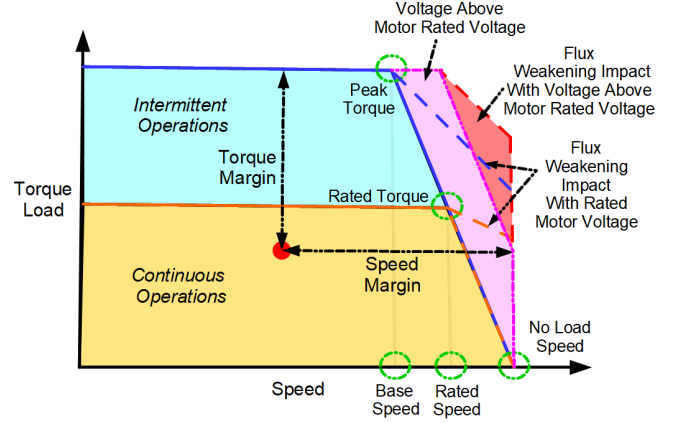


Figure 1. Torque-Speed Map Description.

increase SM and TM by reducing the effect of back-electromotive force (EMF).

The direct quadrature (DQ) frame is traditionally used to analyze operations of both PMSMs and BLDC motors (Ref. 6). For this work, the authors approximated a realistic motor map using the DQ frame. For direct current (DC) analysis of alternating current (AC) motors, rotations were performed successively from the three-phase frame to the alpha-beta frame to the DQ frame fixed to the motor rotor. The steady-state vectorial schema of a circuit equivalent plotted on the DQ axis from Ref. 9 is depicted in Figure 2.

Several assumptions were made about the vectorial schema in Figure 2 for motor coefficient sizing. Assuming that voltage due to change in current was zero, cross-inductance terms (L_{qd} and L_{dq}) were negligible, and D-axis current was zero. The D-axis current (being equal to zero) is typical of a maximum torque per amp (MTPA) strategy (see Ref. 6).

To size motor coefficients, the NDARC motor specification speed, which was a speed where efficiency peaked, was defined as the motor-rated speed. The NDARC also provided peak torque, which was assumed to be achievable at the base

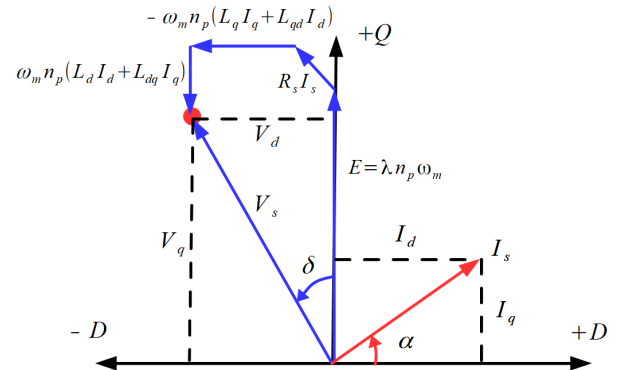


Figure 2. Steady-state Phasor diagram of the PMSM.

speed. The NDARC-reported MRP was determined to be the peak power of the motor at the base speed. The MCP of the motor was defined as the highest power for continuous operations in Figure 1 before FW control laws were required (i.e., D-axis current is non-zero), coinciding with the maximum continuous torque (MCT) operation of the motor. Manufacturers typically rate their motors at a specific speed and torque where efficiency is optimal. The authors assumed that the motor-rated power and MCP would coincide. Also assumed was that the motor-rated torque and MCT would coincide. The no-load speed of the motor was defined as the speed where the torque load was equal to zero. The motor type was assumed to be non-salient, meaning an equivalence in D- and Q-axes self-inductance.

With these assumptions in place, the steady-state Q-axis stator voltage was approximated as the summation of voltage due to torque and back-EMF as in Equation (1):

$$V_q \cong R_s I_q + \lambda n_p \omega_m \quad (1)$$

where R_s is stator resistance, I_q is Q-axis stator current, λ is motor flux linkage, n_p is number of pole pairs, and ω_m is rotational-mechanical speed of the motor. The back-EMF constant was computed by λn_p . The motor speed, ω_m , was assumed to operate at a multiple of the aerodynamic rotor shaft speed; the multiple was assumed to be a fixed gear ratio of the transmission.

The D-axis stator voltage for steady-state operations was approximated for purposes of developing the motor map as in Equation (2):

$$V_d \cong -I_q L_q n_p \omega_m \quad (2)$$

where L_q is the rotor Q-axis self-inductance. The stator voltage (see Figure 2) was computed as in Equation (3):

$$V_s = \sqrt{V_q^2 + V_d^2} \quad (3)$$

The AC motors are voltage limited by the DC-link voltage on the inverter. The voltage limitation was conveniently captured by the following constraint (see Ref. 8) as provided in Equation (4):

$$M = \frac{V_s}{G_{inv} V_b} \leq 1 \quad (4)$$

where V_b is the battery voltage at inverter terminal, and G_{inv} is the inverter voltage output gain. During motor operations, the modulation ratio, M , when equal to one, suggests the inverter is exiting the linear region of operation. Without FW control, this situation may result in open-loop behavior, therefore, rapidly limiting torque availability in the motor.

The inverter voltage output gain, G_{inv} , in Equation (4) is determined by inverter type and gate switching logic. Gate switching logic such as sinusoidal pulse width modulation gives G_{inv} to be $\frac{1}{2}$. For a Space Vector Pulse Width Modulation (SVPWM) inverter, G_{inv} must be equal to $\frac{1}{\sqrt{3}}$. For sizing purposes, it was assumed that when FW control was available for the SVPWM inverter, G_{inv} was increased to 0.664.

The sizing of the motor coefficients (see Figure 1) occurs at the intersection of rated motor torque and speed. For the following discussion, a superscript, r , was appended to variables to indicate a parameter evaluated at a motor-rated condition.

The motor power factor (Ref. 10) as a function of the power factor angle, φ , was computed as in Equation (5) using current and voltage angles defined in Figure 2:

$$P_f = \cos(\varphi) = \cos\left(\frac{\pi}{2} + \delta - \alpha\right) \quad (5)$$

where the voltage angle was solved for as in Equation (6):

$$\delta = -\tan^{-1}\left(\frac{V_d}{V_q}\right) \quad (6)$$

and the current angle was solved for as in Equation (7):

$$\alpha = \tan^{-1}\left(\frac{I_q}{I_d}\right) \quad (7)$$

The current angle, α , at the rated condition of the motor, was equal to $\frac{\pi}{2}$ due to the assumption that $I_d = 0$; therefore, at the rated condition, the motor power factor was computed as in Equation (8):

$$P_f = \cos(\varphi) = \cos\left(-\tan^{-1}\frac{V_d}{V_q}\right) \quad (8)$$

By substituting Equations (1) and (2) into Equation (8), the motor-rated inductance was solved for as in Equation (9):

$$L_d^r = L_q^r = \frac{\tan(\cos^{-1}(\varphi)) \lambda^r}{I_q^r} \quad (9)$$

The three unknown terms in Equation (9) were rated current, power factor, and flux linkage. The rated current was determined from rated input torque, τ_i , as in Equation (10):

$$I_q^r = \frac{2 \tau_L^r + \tau_f^r}{3 \lambda^r n_p} = \frac{2 \tau_f^r}{3 \lambda^r n_p} \quad (10)$$

where τ_f^r is torque losses experienced at the rated motor condition, and τ_L^r is the rated load torque. At the rated

operating condition of the motor, the viscous damping was assumed to be the most significant torque loss; therefore, the friction model was assumed to be as in Equation (11).

$$\tau_f^r \cong \tau_c + B\omega_m^r \quad (11)$$

where τ_c is Coulomb friction loss and B is the viscous damping coefficient. The damping coefficient was calculated by assuming a percentage of torque loss with respect to the rated motor load torque. It was assumed that total torque losses would amount to two percent of the rated motor load torque at the rated operating condition. It was assumed that Coulomb torque was 10 percent of the total torque losses at the rated motor operating condition. The motor load power was determined as in Equation (12):

$$P_L^r = \tau_L^r \omega_m^r \quad (12)$$

where ω_m^r is the rated motor speed. With the ratio of peak power to maximum continuous power known, the motor torque load, τ_L^r , at the rated condition was solved for. The rated input power was set equal to the motor load power with an assumed motor efficiency, η_m^r , of 96 percent as in Equation (13):

$$P_I^r = \frac{P_L^r}{\eta_m^r} \quad (13)$$

The efficiency of 96 percent used in RTPM was made to be consistent with the NDARC-assumed 95 percent efficiency for most of the RVLTL reference vehicle designs. The slight difference accounts for the inverter efficiency included in NDARC. The rated input power may be recast as motor active power which is a function of the stator voltages and currents as in Equation (14):

$$P_I^r = \frac{3}{2}V_q^r I_q^r + \frac{3}{2}V_d^r I_d^r \quad (14)$$

Casting input power as active power allows for changes to efficiency as a function of motor temperature to be directly computed.

It was assumed that load power, friction, and hysteresis losses subtracted from input power was equal to heating losses. The winding losses were determined as in Equation (15):

$$P_H^r = P_I^r - P_L^r - \tau_f^r \omega_m^r \triangleq \frac{3}{2}R_s^r (I_q^r)^2 \quad (15)$$

The rated stator resistance was computed by substituting power terms from Equations (11) to (13) into Equation (15). Solving for rated stator resistance resulted in Equation (16):

$$R_s^r = \frac{2}{3} \frac{P_H^r}{(I_q^r)^2} \quad (16)$$

The rated flux linkage was computed by substituting Equations (2) and (3) into Equation (1). The result was substituted into Equation (4) which was used to form Equation (17). Notably, Equation (17) is an inequality constraint but was converted to an equality constraint at the rated motor condition.

$$G_{inv} V_m^r = \sqrt{(R_s^r I_q^r + \lambda^r n_p \omega_m^r)^2 + (-L_q^r I_q^r n_p \omega_m^r)^2} \quad (17)$$

Equation (17) is crucial to sizing motor coefficients. The motor design voltage, V_m^r , is defined as the DC rating provided by motor manufacturers when providing a motor map. Similar to the number of pole pairs of the motor, an independent choice must be made on the rated motor voltage. The map shown previously in Figure 1 will not change regardless of V_m^r , but it will affect the resulting motor coefficients. For this research, 350 V was chosen. The pole pair count, n_p , was set equal to six in the motor coefficient sizing procedure. Other choices were acceptable depending on desired speed range of the motor; a lower number of pole pairs was ideal for high-speed motor applications. Inductance, resistance, and current computed, respectively, from Equations (9), (15), and (10) were directly substituted into Equation (17). The rated flux linkage was solved for as in Equation (18):

$$\lambda^r = \frac{V_m^r G_{inv}}{n_p \sqrt{[1 + \tan^2(\cos^{-1}(\varphi))] \omega_m^r{}^2 + 2\omega_m^r \frac{P_H^r}{\tau_I} + \left(\frac{P_H^r}{\tau_I}\right)^2}} \quad (18)$$

To solve Equation (18), the power factor was iterated until the rated speed converged to the NDARC specification speed. With power factor, flux linkage, and rated current determined, the rated inductance from Equation (9) can be computed.

Speed and Torque Limits

The motor limits are necessary for both control law design and simulation. By rearranging Equation (17) and inserting battery voltage, the peak motor speed for any load torque, up to the peak load torque, was computed for a given motor. The peak motor speed formula without FW control was computed as in Equation (19):

$$\omega_m^p = \frac{-R_s I_q \lambda + \sqrt{G_{inv}^2 V_b^2 (\lambda^2 + L_q^2 I_q^2) - L_q^2 I_q^2 R_s^2 I_q^2}}{n_p (\lambda^2 + L_q^2 I_q^2)} \quad (19)$$

where current was computed as in Equation (20):

$$I_q = \frac{2(\tau_L + \tau_c + B\omega_m^p)}{3\lambda n_p} \quad (20)$$

The rated motor constants were substituted into Equation (19) for a reasonable peak speed calculation. Since current is a function of speed, Equations (19) and (20) were easiest to solve by iteratively varying ω_m^p . Proceeding forward, the rated superscript was removed from motor coefficient variables to account for possible changes in the motor coefficients due to temperature and current draw at different motor conditions.

The authors assumed that by providing a high enough battery voltage, peak torque may be held constant between the base speed and rated speed. After the rated speed, and assuming the availability of FW control, the power was assumed to level off which required decreasing torque to maintain a constant power relationship. The peak speed performance (shown in Figure 1) beyond the rated speed, and up to the no-load speed, was defined as in Equation (21):

$$\omega_m^p \triangleq \frac{\omega_m^r \tau_L^b}{\tau_L} \leq \omega_m^{nl} \quad (21)$$

where ω_m^{nl} is the no-load speed, and τ_L^b is the peak load torque achievable at the base speed.

The base speed of the motor, ω_m^b , was achievable assuming the availability of the DC-rated voltage at the peak power condition. The base speed as determined by NDARC was computed as in Equation (22):

$$\omega_m^b = G_r \frac{P_L^p}{\tau_L^b} \quad (22)$$

where P_L^p is peak load power (or what NDARC refers to as MRP). The base speed may also be computed using powertrain physics by substituting peak current, I_q^p , defined as in Equation (23), as well as the motor-rated DC voltage, V_m^r , into Equation (19).

$$I_q^p = \frac{2}{3\lambda n_p} \left[\frac{P_L^p}{\omega_m^b} + \tau_c + B\omega_m^b \right] \quad (23)$$

The no-load speed was computed by substituting no-load current, I_q^{nl} , defined as in Equation (24), as well as the motor-rated DC voltage, V_m^r , into Equation (19) and iterating speed until convergence.

$$I_q^{nl} = \frac{2}{3\lambda n_p} [\tau_c + B\omega_m^b] \quad (24)$$

The no-load speed, ω_m^{nl} , is a crucial motor limit (see Figure 1) and was assumed to be the maximum motor speed before damage occurred. Exceeding the no-load speed with a significant torque load – while theoretically possible with a

larger voltage source and/or FW control – might lead to mechanical damage of the motor. The no-load speed may be substituted into the NDARC as a peak speed limit to adjust the motor size.

The peak load torque depends on the speed of the motor. Equation (17) was solved for peak load torque as a function of motor speed. The physical solution of the resulting quadratic equation solved for peak load torque (see Figure 1) and was derived as in Equation (25):

$$\tau_L^p = \frac{-\tau_c - B\omega_m - \sqrt{G_{inv}^2 V_b^2 (R_s^2 + L_q^2 n_p^2 \omega_m^2) - L_q^2 n_p^4 \omega_m^4 \lambda^2}}{2(R_s^2 + L_q^2 n_p^2 \omega_m^2)} \quad (25)$$

$$\tau_L^p \leq \tau_L^b$$

$$\omega_m^{nl} \geq \omega_m \geq \omega_m^b$$

The peak torque load of a motor, defined up to the base speed, is generally constant and is typically provided by a manufacturer specification sheet. In this case, the peak torque load was set to the peak torque provided by the NDARC. As motor speed is increased, and back-EMF increases, peak torque load must decrease. The peak load torque constraint, τ_L^p , provided in Equation (25) bounds the torque beyond the base speed and up to the no-load speed.

Given a high enough battery voltage and the availability of FW control there are changes to the computation of peak torque load from Equation (25). The peak load torque may be defined as in Equation (26):

$$\tau_L^p \triangleq \begin{cases} \tau_L^b & \omega_m \leq \omega_m^r \\ \frac{\omega_m^r \tau_L^b}{\omega_m} & \omega_m^r < \omega_m < \omega_m^{nl} \end{cases} \quad (26)$$

For improved handling qualities (see Ref. 4), the motor map development process should allow for motor map extension (see Figure 1) to account for both a larger battery voltage than the rated motor voltage and inverter FW control technology (Ref.8). To manipulate motor map load torque to hit the NDARC peak load torque provided at the rated speed, the DC battery voltage was solved for using Equation (27):

$$V_m^p = \frac{1}{G_{inv}} \left[n_p (\lambda^2 + (L_d I_q^p)^2) \right] \omega_m^r \quad (27)$$

Power is assumed to be held constant from the intersection of the peak torque and rated speed, rather than from the base speed when assuming voltage computed from Equation (27) is available up to the no-load speed. To extend continuous motor operations beyond the rated speed, a constant power relationship was also assumed up to the no-load speed beginning from the intersection of the MCT at the rated speed. The continuous operations of the motor are not assumed to change for a higher voltage, due to thermal constraints.

RTPM Simulation Architecture

The RTPM simulation architecture is a MATHWORKS Simulink® (The Mathworks, Inc., Natick, North Carolina) model-based implementation. The powertrain component software models were tied together to support auto-coding for VMS simulation. The component software interconnections were used for component analyses in this paper, as depicted in Figure 3. For all simulation equations that follow describing components within the simulation architecture, dependence on time for an explicit variable will not be denoted to reduce equation complexity.

Assuming that the flight control model provided a torque command to the powertrain, the torque command in the simulation architecture (see Figure 3) was limited by the peak torque allowable. The peak torque limit for the control law was defined as in Equation (28). The corresponding current limit was computed as in Equation (29), and the current command was computed similarly from the electromagnetic torque command, τ_e^c :

$$\tau_l \triangleq \tau_L^p + B\omega_m^r + \tau_c \quad (28)$$

$$I_l = \frac{\tau_l}{1.5\lambda^r n_p} \quad (29)$$

The stator voltage limit for a SVPWM inverter was computed based on the inverter link DC voltage, which was provided by the battery model. The voltage limit was computed as in Equation (30):

$$V_l = \frac{1}{\sqrt{3}} V_b(t) \quad (30)$$

The inverter-link DC-voltage gain of $\frac{1}{\sqrt{3}}$ represented the end of the linear range of operation of a SVPWM inverter, so while this “end” may be referred to as a limit in RTPM, it was also a threshold above where FW control must be engaged for the motor to achieve the requested speed and torque. It was also noted by the authors that the gain in Equation (30) was not equal to the value of G_{inv} used for sizing. The inverter gain was set to $\frac{1.15}{\sqrt{3}}$ for sizing motor coefficients assuming the availability of FW control; however, during operations, the inverter gain was set to $\frac{1}{\sqrt{3}}$ for accuracy.

Further inputs into the RTPM simulation architecture must be discussed to provide context for other component powertrain models. The aerodynamic torque, τ_{aero} , was assumed to originate from a rotor wake aerodynamic model (see Ref. 3). The ambient temperature, T_a , was provided to the battery model. The coolant temperature, T_{co} , of the liquid-cooled motor was provided to the motor temperature model. The power required, P_r , from the rotor was provided as an input to the battery model.

The powertrain component software models including the motor electrical rotor and stator model, physics-based FW model, battery model, motor temperature model, rotor shaft torque, and power model communicate their outputs to each other during simulation runtime. The motor temperature model updates the flux linkage and stator resistance based on changes in motor speed and currents. The temperature updates

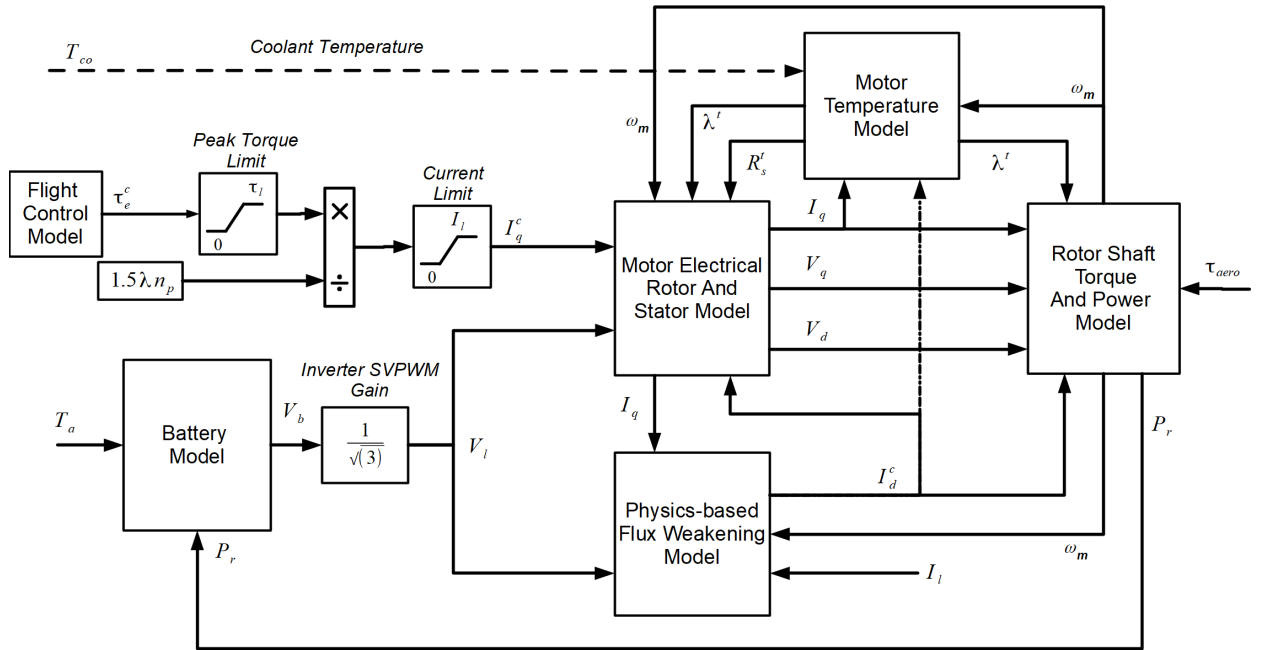


Figure 3. The RTPM Simulation Architecture.

to the rated flux linkage and resistance, λ^r and R_s^r , were denoted as λ^t and R_s^t .

The coupled QD-axis current and voltage physics of the motor were computed in a motor electrical rotor and stator model. A physics-based FW model makes modifications to the D-axis current command, I_d^c , to maintain an appropriate stator voltage corresponding to available inverter DC-link voltage. For flexibility within RTPM, the DC-link voltage may also be provided by a generator and rectifier.

Motor Electrical Rotor and Stator Model

The motor electrical rotor and stator model is simplified from a high-fidelity coupled and nonlinear model of the electrical rotor and stator interaction. Key motor dynamics were presented previously in Figure 2. A representation of the stator and rotor coupling, which achieves reasonable accuracy, is provided (see Figure 4).

A preliminary calculation of Q-voltage was computed as in Equation (31):

$$V_q^{pr} = R_s I_q^c + \omega_m L_d n_p I_d^c + \omega_m \lambda n_p \quad (31)$$

As shown previously in Figure 4, we observed that the preliminary stator voltage magnitude, V_s^{pr} , was computed from the magnitude of V_q^{pr} and V_d , where V_d was assumed to include a stator resistance component as in Equation (32):

$$V_d = R_s I_d^c - \omega_m L_q n_p I_q \quad (32)$$

The preliminary stator voltage calculation was passed through a voltage rate limit defined by V_{rl} . The voltage rate limit was required to preserve numerical stability in the simulation. The

authors believed that improvement in numerical stability from rate limits occurred because the physics of the system were slowed down by the rate limit. The authors observed that unit delay or zero-order hold blocks alone in feedback paths of the model resulted in numerical instability without rate limits. A nominal voltage rate limit of $5,000 \frac{V_{pk}}{s}$ provided numerical stability. The motor simulation results did not change significantly for higher rate limits.

The inverter was assumed to limit the preliminary stator voltage by the voltage limit computed in Equation (30). This limit was technically a linear range of operation threshold for a SVPWM inverter, but for the purposes of comparison to a MATHWORKS Simscape® motor model, this limit was required. The limited Q-axis stator voltage was then computed as in Equation (33):

$$V_q = \sqrt{V_s - V_d} \quad (33)$$

The Q-axis current was solved for using Equation (34):

$$I_q = \frac{1}{R_s} [V_q - I_d^c L_d n_p \omega_m - \lambda n_p \omega_m] \quad (34)$$

To achieve numerical stability, the resulting current must pass through a current rate limit defined by I_{rl} . A rate limit set to $5,000 \frac{A_{pk}}{s}$ was found to achieve numerical stability without negatively affecting accuracy.

Physics-based Flux Weakening Model

Using a physics-based FW control law ensures the RTPM results are not specific to a particular control law, making it easier to generalize the effects of the powertrain. A physics-based FW approach was developed from fundamental PMSM electrical stator and rotor equations. With all other information except I_d^c known or computed in

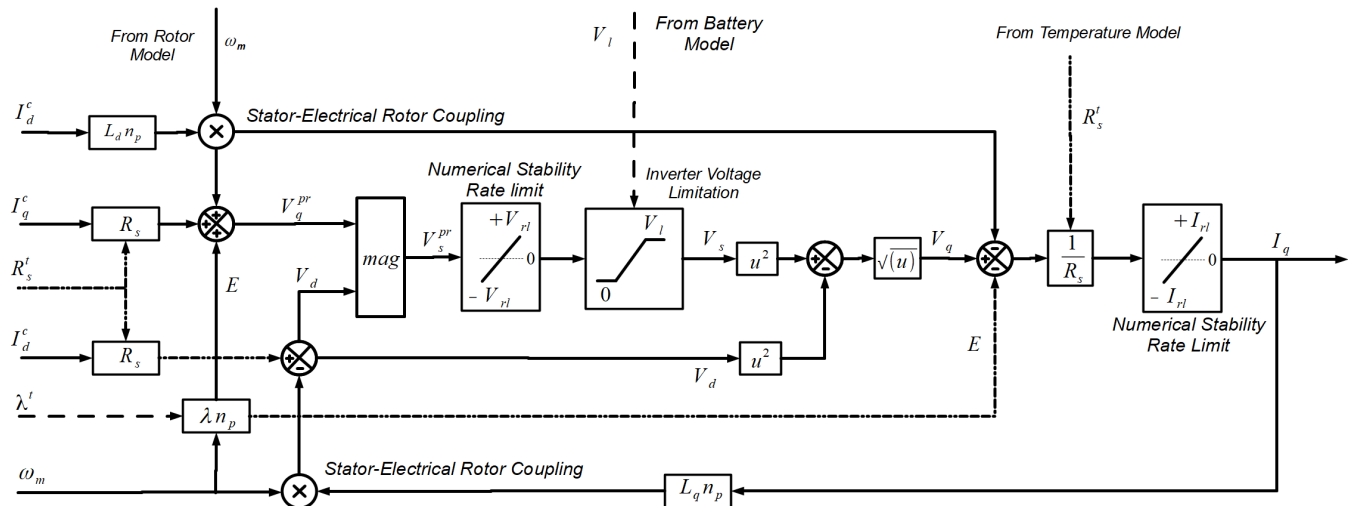


Figure 4. Motor Electrical Rotor and Stator Model.

simulation, Equation (35) has a quadratic solution with respect to I_d^c .

$$g^2 V_l^2 - (R_s I_q + \lambda n_p \omega_m)^2 - (R_s I_d^c - L_q I_q n_p \omega_m)^2 = 0 \quad (35)$$

In simulation, the positive quadratic solution represented the best solution to weaken the motor flux. The FW gain, g , in Equation (35) was chosen as 0.98. The reasoning behind choosing a gain of 0.98 on the voltage limit, as shown in Figure 5 and in Equation (35), was that FW controls must engage before stator voltage exceeds the inverter stator voltage limit defined in Equation (30).

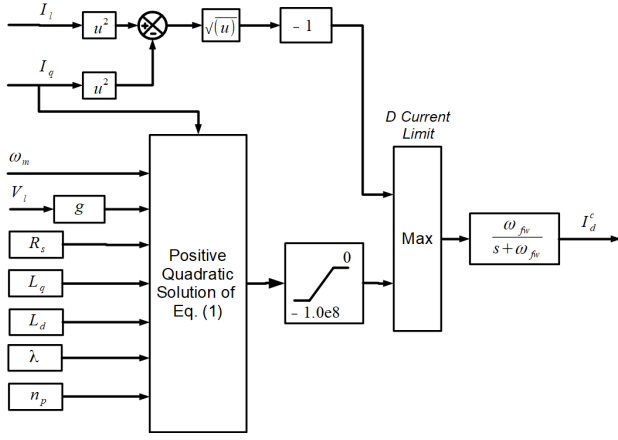


Figure 5. Physics-Based Flux Weakening Control.

The gain of 0.98 was used in a FW control law and incorporated into the Simscape[®] motor representation, then used for comparison to the RTPM in this paper.

The D-axis current command, I_d^c , was both current-limited and enforced to be 0 or negative as previously shown in Figure 5. If the current limit was exceeded by the positive quadratic solution of Equation (35), I_d^c was defined to be as in Equation (36):

$$I_d^c \triangleq -\sqrt{I_l^2 - I_q^2} \quad (36)$$

To avoid significant chatter and reduced numerical stability in the current, a low-pass filter was applied to I_d^c with a filter frequency of ω_{fw} which was set to $100 \frac{\text{rad}}{\text{s}}$. The first version of RTPM tested in the NASA Ames VMS did not include the $100 \frac{\text{rad}}{\text{s}}$ filter on the output of the FW control law, and chatter in the voltage was noted by the authors.

Rotor Shaft and Power

The rotor shaft torque, power, and efficiency relationships were shown in Figure 6. There are several ways to integrate the powertrain into the rotor equations of motion. One way, as described here, is generally applicable to more complex

physics-based aircraft simulations. The electromechanical motor torque was used to drive a rotor model with a transmission and an assumed fixed gear ratio, G_r . The gear ratio varies depending on the RVLTL reference vehicle powertrain specifications provided by NDARC. The electromechanical torque was computed as in Equation (37):

$$\tau_e = 1.5 \lambda n_p I_q + I_q I_d^c (L_d - L_q) \quad (37)$$

The motor torque loss was computed as in Equation (11) where the motor speed was defined to be equal to the multiplication of gear ratio and the rotor speed, ω_r , as in Equation (38):

$$\omega_m \triangleq G_r \omega_r \quad (38)$$

The acceleration of the rotor shaft is determined by the torque difference between electromechanical torque and aerodynamic torque. The torque difference is computed as in Equation (39):

$$\Delta \tau = G_r (\tau_e - \tau_f) + \tau_{aero} \quad (39)$$

where τ_{aero} is the rotor aerodynamic torque. The rotor aerodynamic torque may be calculated by an external rotor aerodynamic wake solver (see Ref. 3).

The rotor model computes the rotor speed using a first-order dynamic model as in Equation (40):

$$\frac{\omega_r}{\Delta \tau} = \frac{1}{J_r + J_m G_r^2} \frac{1}{s} \quad (40)$$

where J_r is the rotor inertia and J_m is the motor inertia. The power required was computed from PMSM active power as in Equation (41):

$$P_r = \frac{1}{\eta_l} \frac{3}{2} (I_q V_q + I_d^c V_d) \quad (41)$$

where η_l was an assumed inverter efficiency. While experimental data may be used to capture variations in inverter efficiency from speed and current, the powertrain during handling qualities tests operated at high motor speeds where inverter efficiency is also high; therefore, the inverter

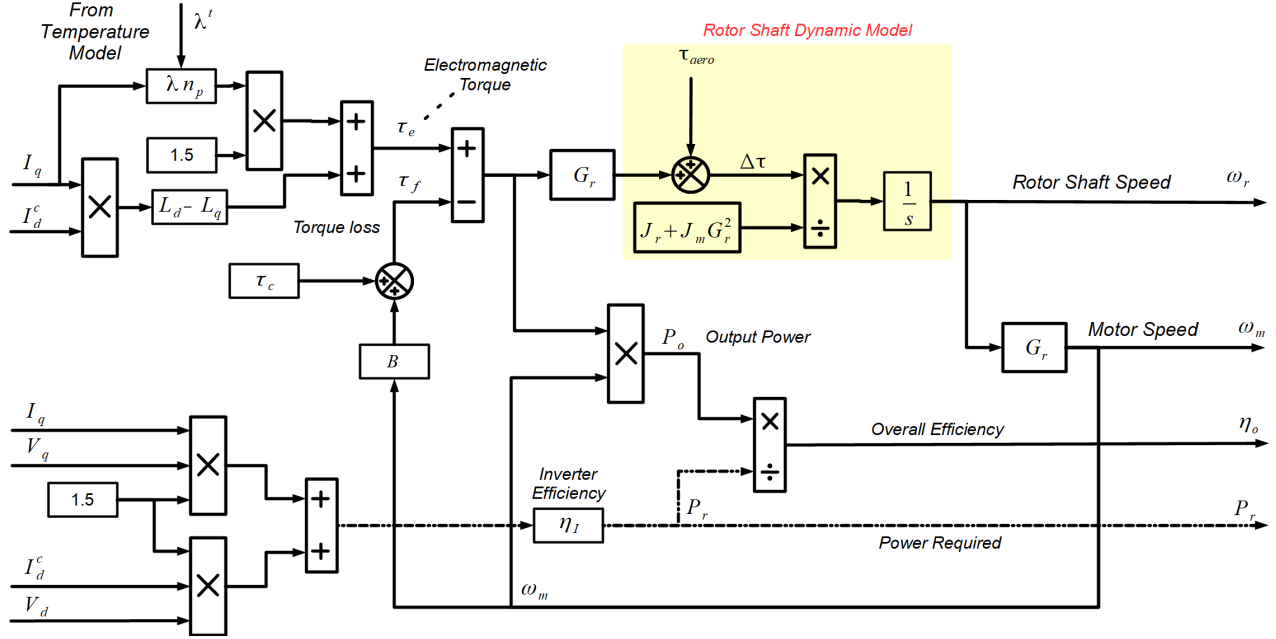


Figure 6. Rotor Torque, Power, and Efficiency.

was assumed to operate near a power conversion efficiency of 99 percent for the simulation.

The overall efficiency of the powertrain was computed as in Equation (42):

$$\eta_o = \frac{\tau_e - \tau_f}{P_r} \quad (42)$$

It was assumed that there were no direct torque losses in the transmission for RTPM simulation purposes. The NDARC allows for a user-defined transmission efficiency which for many RVLTL reference vehicles was set to 98 percent. During sizing, the RTPM captures friction losses in the motor where the moving loss coefficients are sized by assuming they make up two percent of rated torque. The transmission torque loss in RTPM was assumed to be negligible to avoid double counting the torque loss reduction to better compare with NDARC expectations.

Motor Thermal Model

An empirically developed lumped mass thermal model is a computationally efficient approach to approximate the heat loss among the motor components. The lumped masses, heating elements, and connections for a motor thermal model shown previously in Figure 3 were depicted in Figure 7. Parameters and values used to drive the thermal model are provided in Table 1.

The PMSM lumped masses were assumed to include the windings, stator core and motor rotor. The connections and equations to design the thermal model were collected from PMSM and rated at 82 hp with a peak power of 120.95 hp

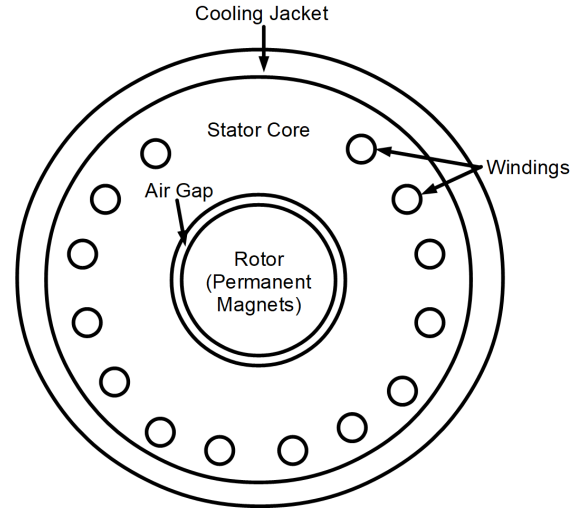


Figure 7. Assumed Motor Lumped Mass Thermal Structure.

(Ref. 11). The authors believed that 82 hp is sufficiently close to most RVLTL reference vehicle motors. To identify changes in the torque constant and back-EMF due to temperature, a modification was made to the motor thermal model to add in a motor rotor lumped mass (Ref. 12). Originally, Ref. 12 created temperature models for a 46-hp motor from the same manufacturer, but the modification worked well for the 82-hp motor.

The lumped mass thermal model for real-time simulation is presented (see Figure 8). The temperature model provided insight concerning the trend in motor winding, stator core, and motor rotor temperatures due to change in motor speed and

Table 1. Specifications for Assumed Thermal Model.

Parameter	Values
Rated Speed (ω_m^r)	4,900 rpm
Base Speed (ω_m^b)	3,432 rpm
Rated Current (I^r)	247.06 A _{pk}
Rated Torque Load (τ_L^r)	96.4 ft·lb
Peak Torque Load (τ_L^p)	185.09 ft·lb
Rated Temperature (T^r)	140 °C
Rated Stator Resistance (R_s^r)	0.0142 Ohm
Rated Inductance (L_q, L_d)	0.00037 H
Rated Flux Linkage (λ^r)	0.0588 $\frac{\text{V}\cdot\text{s}}{\text{rad}}$
Stator Resistance at 25°C (R_s^{25})	0.0093 Ohm
Flux Linkage at 20°C (λ^{20})	0.0645 $\frac{\text{V}\cdot\text{s}}{\text{rad}}$
Speed Loss Coefficient (α_l)	0.0011 $\frac{\text{s}}{\text{rad}}$
Copper Coefficient (α_c)	0.00393 $\frac{1}{^\circ\text{C}}$
Permanent Magnet Coefficient (α_{pm})	-0.001 $\frac{1}{^\circ\text{C}}$
Hysteresis Coefficient (k_h)	23.7595
Viscous Damping Coefficient (B)	0.0019 $\frac{\text{ft}\cdot\text{lb}\cdot\text{s}}{\text{rad}}$
Coolant Temperature (T_{co})	60 °C
Winding to Stator Core Thermal Resistance (R_{wc})	0.037 $\frac{^\circ\text{C}}{\text{W}}$
Coolant to Stator Core Thermal Resistance (R_{cc})	0.019 $\frac{^\circ\text{C}}{\text{W}}$
Stator Core Thermal Capacitance (C_c)	33,346 $\frac{\text{J}}{^\circ\text{C}}$
Winding Thermal Capacitance (C_w)	4,820 $\frac{\text{J}}{^\circ\text{C}}$
Motor Rotor Thermal Capacitance (C_r)	9,600 $\frac{\text{J}}{^\circ\text{C}}$

torque loads. For the following thermal model development discussion, a superscript, t , was added to motor variables λ and R_s due to changes in temperature. The thermal modeling efforts by the authors did not include a model of changes to self-inductance due to temperature. Thermal heating was assumed to be primarily due to hysteresis, dynamic viscous and resistive power losses. The hysteresis heating power was modeled as in Equation (43):

$$Q_h = k_h \omega_m \left[(L_d I_d^c + \lambda^t)^2 + (L_q I_q)^2 \right] \quad (43)$$

where k_h is the hysteresis coefficient. The dynamic viscous heating power was modeled as in Equation (44):

$$Q_v = B \omega_m^2 \quad (44)$$

The resistive heating power was modeled as in Equation (45):

$$Q_i = \frac{3}{2} R_s^t \left[(I_d^c)^2 + I_q^2 \right] \quad (45)$$

To maintain a safe operating temperature for the windings and permanent magnets, and for consistency with the empirical thermal motor equations, the motor was modeled to be liquid cooled via a cooling jacket (see Figure 7). Each lumped mass temperature calculation included an update to its present temperature governed by thermal capacitance, resistance, and connected lumped mass temperatures. The change in stator core temperature calculation per time step was computed as in Equation (46):

$$\Delta T_c = \frac{\Delta t}{C_c} \left[\frac{T_w - T_c}{R_{wc}} + \frac{T_r - T_c}{R_{rc}} + \frac{T_{co} - T_c}{R_{cc}} \right] \quad (46)$$

where R_{wc} is the thermal resistance between the winding and stator core, and R_{cc} is the thermal resistance between the coolant and stator core, and C_c is the stator core thermal capacitance. The thermal resistance between the motor rotor and stator core, R_{rc} , was also assumed to be a function of motor speed as in Equation (47):

$$R_{rc} = \frac{1}{a_{rc} + b_{rc} \omega_m + c_{rc} \omega_m^2} \quad (47)$$

where the coefficients, a_{rc} , b_{rc} , and c_{rc} were constant, linear, and quadratic coefficients that were determined by following the rotor-to-stator thermal resistance fitting procedure in Ref. 12. The values of a_{rc} , b_{rc} , and c_{rc} , vary for every motor, and for the 82-hp motor, values were determined to be 4.0438, -0.0059, and 3.5052e-6, respectively. Examination of Equation (46) suggests that a reduction in the coolant temperature produced a more negative change in stator core temperature, resulting in cooling of the stator core.

The change in winding temperature was modeled as in Equation (48):

$$\Delta T_w = \frac{\Delta t}{C_w} \left[\frac{T_c - T_w}{R_{wc}} + Q_h + Q_v + \alpha_s Q_i \right] \quad (48)$$

where α_s is the speed loss coefficient which governs the ratio of resistive heating to the rotor and winding. The speed-dependent loss power ratio was computed as in Equation (49):

$$\alpha_s = 1 - \alpha_l \omega_m \quad (49)$$

where α_l was a linear speed loss coefficient which was solved for using the speed loss coefficient sizing methodology provided in Ref. 12. The change in rotor temperature was modeled as in Equation (50):

$$\Delta T_r = \frac{\Delta t}{C_r} \left[\frac{T_c - T_r}{R_{rc}} + (1 - \alpha_s) Q_i \right] \quad (50)$$

Equations (48) and (49) show that at low-motor speeds, resistive heating increased winding temperature more so than at high speeds. At high-motor speeds, resistive heating

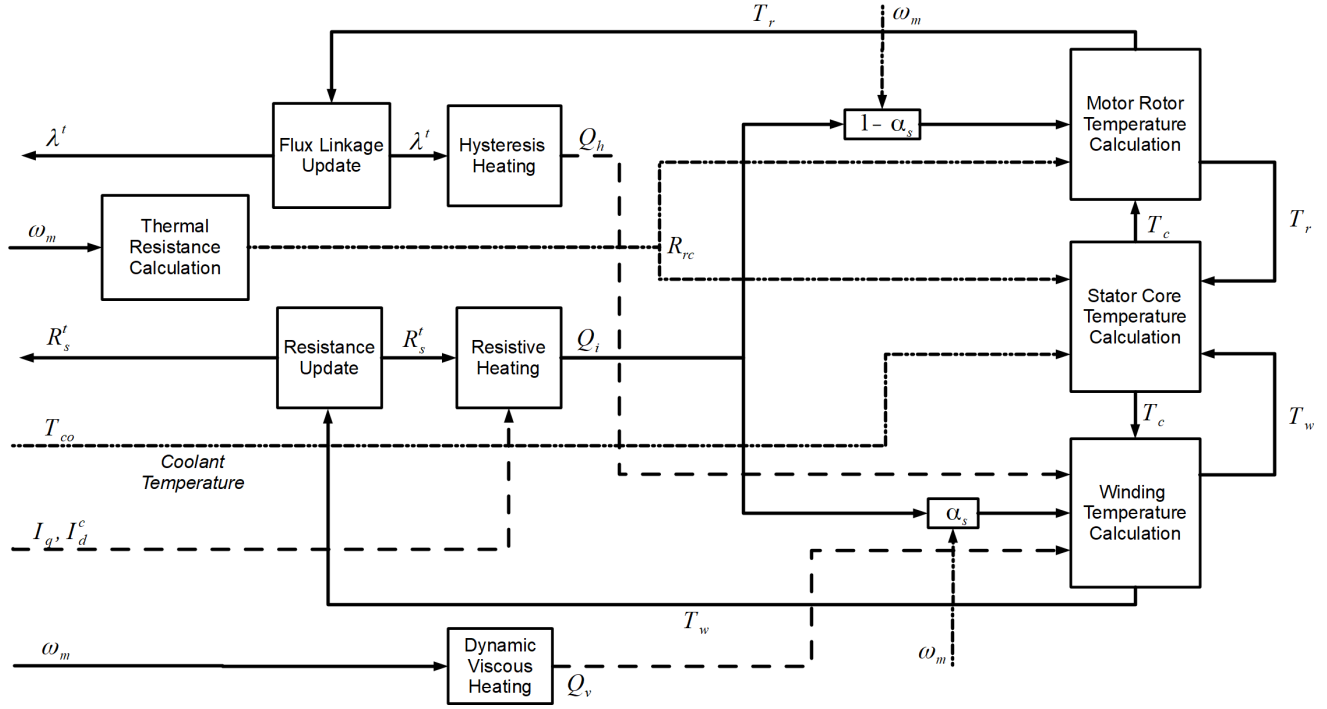


Figure 8. Motor Thermal Model.

increased the change in rotor temperature more than at low-motor speeds.

The motor flux linkage variation with temperature was modeled as in Equation (51):

$$\lambda^t = \lambda^{20} [1 + \alpha_{pm}(T_r - 20)] \quad (51)$$

where λ^{20} is the flux linkage measured at 20 °C, α_{pm} is the thermal coefficient for permanent magnet material. The rated flux linkage coefficient was substituted into Equation (51) and solved for λ^{20} when the average winding temperature was equal to 140 °C and the rotor temperature had reached a steady state.

The stator resistance was modeled as in Equation (52):

$$R_s^t = R_s^{25} [1 + \alpha_c(T_w - 25)] \quad (52)$$

where R_s^{25} is the stator resistance measured at 25° C, α_c was the copper coefficient set to $3.93e^{-3} \frac{1}{^\circ\text{C}}$. The R_s^{25} term was solved by setting the left-hand side of Equation (52) equal to the rated stator resistance when the average winding temperature was equal to 140 °C.

The change in resistance and flux linkage with temperature might have a significant impact on motor efficiency. While generating temperature-corrected motor-efficiency maps the flux linkage and stator resistance were assumed to be held

fixed once the winding temperature reached 180 °C. The limitation on the flux linkage and stator resistance was only necessary during operations in the intermittent torque range in Figure 1. The authors believed that efficiencies for higher winding temperatures should not be reported because a winding temperature of 180 °C was assumed to represent a temperature where significant motor damage could begin to occur.

Some of the coefficients provided for the thermal model require manufacturer specifications; therefore, making it difficult to generalize the thermal model to other motors. The authors decided to normalize the speed and current inputs to the thermal model by motor-rated speed and current, respectively. The outputs would be scaled to drive thermal characteristics of the actual motor. This process allowed the thermal model to be generalized to any motor used for simulation with the caveat that the thermal model may not be as accurate as if it was derived empirically for a particular motor. The scaling formula for speed that was provided to the thermal model was modeled as in Equation (53):

$$(\omega_m)_{th} = \frac{(\omega_m^r)_{th}}{(\omega_m^r)_{act}} (\omega_m)_{act} \quad (53)$$

The scaling formula for I_q current that was provided to the thermal model was used as in Equation (54):

$$(I_q)_{th} = \frac{(I_m^r)_{th}}{(I_m^r)_{act}} (I_q)_{act} \quad (54)$$

Equation (54) was also used to scale I_d current while the motor operates in the FW region. The thermal model flux linkage was used to determine the actual flux linkage as in Equation (55):

$$(\lambda^t)_{act} = \left[1 + \frac{(\lambda^t)_{th} - (\lambda^r)_{th}}{(\lambda^r)_{act}} \right] (\lambda^r)_{act} \quad (55)$$

The thermal model stator resistance was used to determine the actual stator resistance as in Equation (56):

$$(R_s^t)_{act} = \left[1 + \frac{(R_s^t)_{th} - (R_s^r)_{th}}{(R_s^r)_{th}} \right] (R_s^r)_{act} \quad (56)$$

Battery Model

The battery block was assumed to be available to power all motors. The battery block consists of battery packs strung in parallel. The battery packs were assumed to be composed of identical 18650 lithium-ion polymer (LiPo) batteries in series. The cells were assumed to be homogeneous, meaning no differences in current draw, heating, or cooling. The dynamics of each 18650 battery were modeled with empirical equations (Ref. 13) which primarily depend on the battery state of charge. A battery temperature model from Ref. 14 captured the variation of battery temperature of each battery cell. The battery connections and key supporting components are visualized (see Figure 9).

The power required from the motor was initially converted to battery cell current as in Equation (57):

$$I_c = \frac{P_r}{N_s N_p V_c} \quad (57)$$

where N_s is the number of cells in series in a battery pack, N_p is the number of packs in parallel, and V_c is the battery cell voltage.

The battery cell current was passed through a lower limit of zero A and an upper limit that was defined by the C-limit of the battery cell. The battery cell current was used to determine the current battery cell charge, voltage drops, and temperature, as shown in Figure 9. The change in the battery cell state of charge was computed as in Equation (58):

$$\Delta S_{oc} = -\frac{I_c \alpha_a \beta_t \Delta t}{3600 C_c} \quad (58)$$

where C_c is the cell capacity, α_a is a current amplitude correction to current, and β_t is a temperature correction to current. The initial cell capacity may be affected by various factors (Ref. 13), such as the number of charge and discharge cycles, age, storage time, and storage temperature. These various factors were modeled by relying on Ref. 13; however, their implementations were not recorded here for brevity, nor were they studied in the VMS.

Large current draw or high cell-surface temperature have the effect of hastening the reduction of battery cell state of charge. The battery cell state of charge and current affect the battery cell voltage. The following empirical equations from Ref. 13 were adjusted by the authors based on experimental data from a 2.5-Ah Samsung (Samsung Display Innovations (SDI), Suwon, Korea) INR18650 LiPo battery bought and tested in

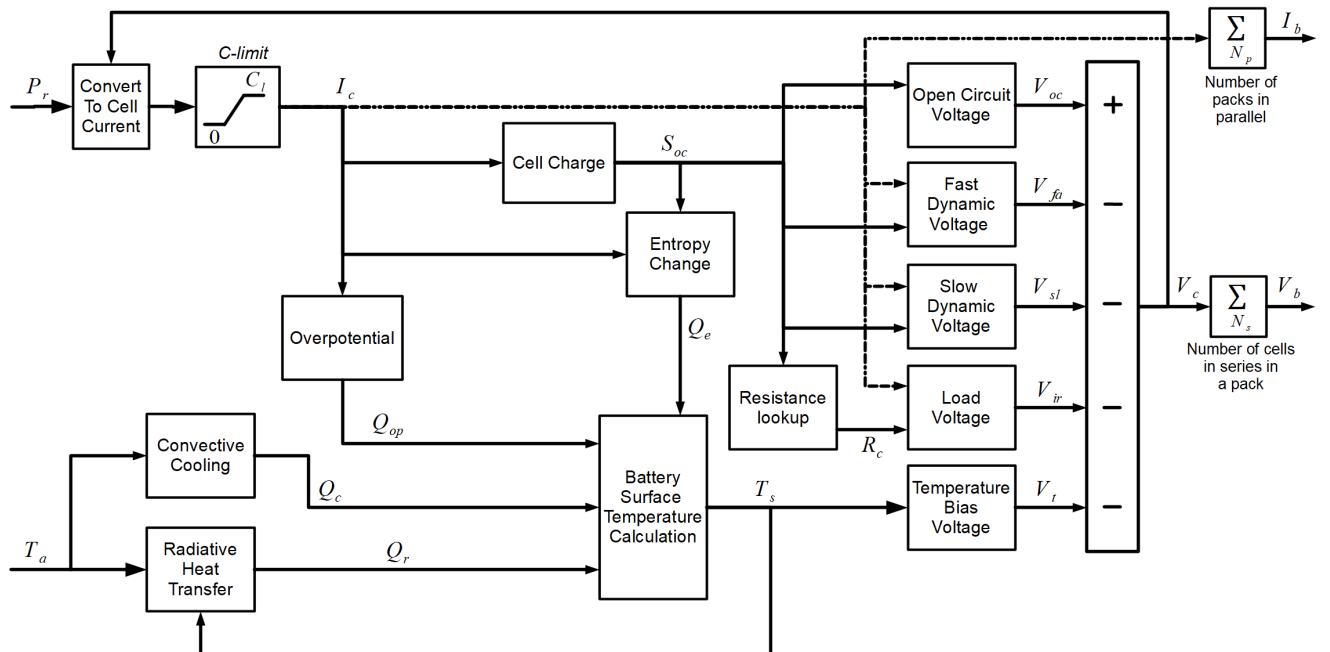


Figure 9. Battery Model.

2019. The empirical equation for the open circuit voltage was determined as in Equation (59):

$$V_{oc} = 3.427 - 2.062e^{-59.5S_{oc}} + 0.453S_{oc} - 0.271S_{oc}^2 + 0.610S_{oc}^3 \quad (59)$$

The instantaneous load voltage (see Figure 9) was computed as in Equation (60):

$$V_{ir} = I_c R_c \quad (60)$$

where the internal cell resistance was computed as in Equation (61):

$$R_c = 0.372 + 0.078e^{-24.37S_{oc}} \quad (61)$$

Fast and slow transient dynamic-battery voltage characteristics were provided in Ref. 13 and, as their name implies, model fast- and slow voltage changes due to current draw. For better comparisons to the author's experimental battery test, the sensitivity of the transient dynamic equations was reduced by 50 percent. The fast transient dynamic-battery voltage drop was modeled as in Equation (62):

$$V_{fa} = 0.5 \frac{R_{fa}}{1 + sR_{fa}C_{fa}} \quad (62)$$

where R_{fa} is the fast transient battery resistance modeled as in Equation (63):

$$R_{fa} = 0.0117 + 0.0802e^{-29.14S_{oc}} \quad (63)$$

and fast transient battery capacitance was modeled as in Equation (64):

$$C_{fa} = 703.6 - 752.9e^{-13.51S_{oc}} \quad (64)$$

The slow transient dynamic-battery voltage drop was modeled as in Equation (65):

$$V_{sl} = 0.5 \frac{R_{sl}}{1 + sR_{sl}C_{sl}} \quad (65)$$

where the slow transient battery resistance was modeled as in Equation (66):

$$R_{sl} = 0.0125 + 1.6507e^{-155.2S_{oc}} \quad (66)$$

and slow transient battery capacitance was modeled as in Equation (67):

$$C_{sl} = 4475 - 6066e^{-27.12S_{oc}} \quad (67)$$

The battery voltage is affected by temperature. The empirically determined linear relationship in Ref. 13 was provided in Table 2 to determine temperature-based voltage bias, V_t . The battery temperature model, like the motor thermal model, was subject to various power loss sources which led to battery cell surface temperature increases. The heating sources included those due to overpotential and entropy. Overpotential heating was modeled as in Equation (68):

$$Q_{op} = \eta_t I_c \quad (68)$$

where η_t is total overpotential. Entropy heat was modeled as in Equation (69):

$$Q_e = \frac{\Delta S I_c T_s}{n_e F} \quad (69)$$

where n_e is the number of electrons transferred, F is the Faraday constant, T_s is the battery cell surface temperature, and ΔS is the change in entropy modeled as in Equation (70):

$$\Delta S = -116 + 247.29S_{oc} - 179S_{oc}^2 \quad (70)$$

Cooling sources were both convective and radiative. Convective heat transfer is typically considered to be the more dominant form of cooling (Ref. 14). Convective heating was modeled as in Equation (71):

$$Q_c = hA_c(T_s - T_a) \quad (71)$$

Table 2. Variable Breakpoints for Linear Interpolation ($x_{var} \rightarrow y_{var}$).

x_{var}	y_{var}	$[x_{bp}]$	$[y_{bp}]$
I_c	α_a	[0.28, 0.7, 1.4, 2.8] A	[0.96, 1, 1.01, 1.052]
T_s	β_t	[-20, -10, 0, 10, 23, 45] °C	[1.086, 1.07, 1.065, 1, 0.975]
T_s	V_t	[-20, -10, 0, 10, 23, 34, 45] °C	[-0.48, -0.32, -0.14, 0.08, 0, 0.12, 0.24] V

where h is the convective heat transfer coefficient, and A_c is the surface area of the cell, and T_a is ambient temperature. The radiative cooling was modeled as in Equation (72):

$$Q_r = \sigma \varepsilon A_c (T_s^4 - T_a^4) \quad (72)$$

where σ is the Stefan-Boltzmann constant and ε is the battery cell surface emissivity factor.

The change in battery surface temperature was modeled as in Equation (73):

$$\Delta T_s = \frac{\Delta t}{m C_p} [Q_{op} + Q_e - Q_c - Q_r] \quad (73)$$

The temperature voltage bias, V_t , can be computed from linear interpolation as a function of battery surface temperature using values provided in Table 2. The total battery cell voltage is modeled as in Equation (74):

$$V_c = V_{oc} - V_{fa} - V_{sl} - V_{ir} - V_t \quad (74)$$

The battery voltage provided to the inverter DC link was defined as in Equation (75):

$$V_b \triangleq N_s V_c \quad (75)$$

Finally, the battery block current provided to the inverter was computed as in Equation (76):

$$I_b = N_p I_c \quad (76)$$

The battery block current was primarily computed for analysis. Battery voltage was the key to capturing motor torque limitations. Key battery constants used for the battery model were provided in Table 3.

Table 3. Specifications for Assumed Battery Model.

Parameter	Values
Total Overpotential (η_t)	0.052 V
Number of Transferred Electrons (n_e)	2
Faraday Constant (F)	96485 $\frac{A \cdot s}{mol}$
Convective Heat Transfer Coefficient (h)	13.43 $\frac{W}{m^2 K}$
Battery Cell Area (A_c)	0.0042 m^2
Battery mass (m)	43.7 g
Heat Capacity (C_p)	1 $\frac{J}{gK}$
Stefan-Boltzmann Constant (σ)	5.7e-8 $\frac{W}{m^2 K^4}$
Battery Cell Surface Emissivity Factor (ε)	0.91

MODEL DESIGN

Four motor models and one battery model were used for verification and validation of RTPM. The models were extensively verified for equational accuracy with sample results presented in this paper. An overview of the model description and objectives are provided in Table 4. The first model was developed to compare to the manufacturer-simulated data for a motor and was referred to as “Motor A” in the NASA Scaled Power ElEctrified Drivetrain (SPEED) (Ref. 15) testbed. The second motor model was developed to compare to time history data of a motor, “Motor B,” in the Advanced Reconfigurable Electrified Aircraft Lab (AREAL) (Ref. 16) testbed. The third motor model, “Motor C,” was developed to replicate lifting motor performance of the

Table 4. Model Description and Objective.

Models	Description	Objectives
Motor A	Motor in the SPEED testbed	Verify RTPM-motor map sizing can predict manufacturer-simulated data
Motor B	Motor in the AREAL testbed	Validate RTPM-Simulink® motor model against experimental time history data
Motor C	Simulink® model of the turboelectric lift-plus-cruise lifting motor developed with RTPM	Verify RTPM-Simulink® motor operations in FW region Verify RTPM-Simulink® thermal model at rated condition and various torque loads Verify RTPM-Simulink® battery model impacts motor performance as battery is discharged
Motor D	Simscape® model of the turboelectric lift-plus-cruise lifting motor developed with RTPM	Compare to Motor-C operations in FW region
Battery	Simulink® model of the turboelectric lift-plus-cruise backup battery developed with RTPM	Verify RTPM-Simulink® battery model performs as expected as battery is discharged Validate RTPM-Simulink® battery cell model against 18650 experimental data

turboelectric lift-plus-cruise reference design. Motor C was representative of a motor used for piloted testing in the NASA Ames Vertical Motion Simulator in the autumn of 2022. Motor C was compared to the fourth motor, “Motor D,” in the FW region. Motor D was a MATHWORKS Simscape® model of the turboelectric lift-plus-cruise lifting motor. The Motor D simulation model included realistic PMSM control laws, more complex physics, and FW control. A battery model was developed based on the NDARC backup battery and sized for the turboelectric lift plus cruise.

Motor A

The RTPM-motor map development procedure was used to develop a representative torque-speed map for Motor A. Moving torque loss and Coulomb torque loss were provided by the manufacturer. These losses were used to determine what percentage of rated torque was due to moving losses and Coulomb torque during the motor map development procedure. The RTPM-predicted torque-speed map for Motor A is presented in Figure 10.

Torque values to the left of the no flux weakening (NFW) line (in Figure 10) represent the torque load the motor can drive at the corresponding speed without FW control technology. To the right of NFW represents the area of possible motor operation with FW technology engaged. The MCT was extended to the right and downwards in Figure 10 using FW technology.

The RTPM-sized motor specifications and manufacturer specifications are recorded in Table 5. The line-to-line

resistance at ambient temperature from the manufacturer is 0.1014 Ohm. Given a Delta winding configuration and applying Equation (52), assuming a rated temperature of 140 °C, the manufacturer-rated resistance was predicted to be 0.0978 Ohm which was very close to the rated resistance of 0.103 Ohm predicted by the RTPM motor sizing procedure. The manufacturer phase inductance at ambient temperature is 300 μ H, similar in size to the rated inductance of 290 μ H that

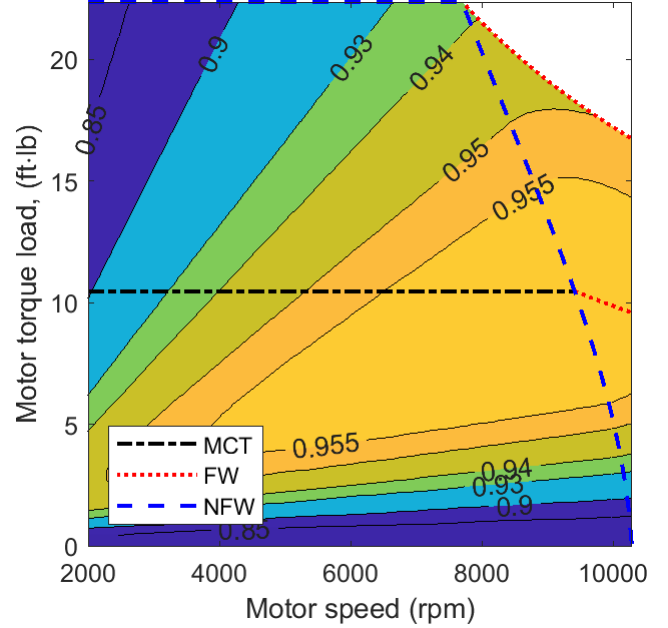


Figure 10. Motor A: Predicted Torque-Speed Map with Lines of Constant Efficiency.

Table 5. Motor A: RTPM Versus Manufacturer Specifications.

Parameter	RTPM	Manufacturer
Rated Speed (ω_m^r)	9,410 rpm	9,338 rpm
Rated Current (I_m^r)	46.03 A _{pk}	53.17 A _{pk}
Rated Torque (τ_m^r)	10 ft · lb	10.52 ft · lb
Peak Torque (τ_m^p)	22.35 ft · lb	22.82 ft · lb
Base Speed (ω_m^b)	7,699 rpm	7,516 rpm
No-load Speed (ω_m^{nl})	10,286 rpm	10,249 rpm
Rated Stator Resistance (R_s^r)	0.103 Ohm per phase	0.1014 Ohm line-to-line at ambient temperature
Rated Inductance (L_q, L_d)	290 μ H	450 μ H line-to-line at ambient temperature
Rated Flux Linkage (λ^r)	0.0349 $\frac{V \cdot s}{rad}$	0.316 $\frac{V \cdot s}{rad}$
Number of Pole Pairs (n_p)	6	6
Dynamic Viscous Coefficient (B)	0.000159 $\frac{ft \cdot lb \cdot s}{rad}$	0.000159 $\frac{ft \cdot lb \cdot s}{rad}$
Coulomb Torque (τ_c)	0.0359 ft · lb	0.0359 ft · lb
Rated Efficiency (η_m)	96%	96%
Power Factor (ϕ)	0.9328	Not listed
Rated Voltage (V_b^r)	340 V	340 V

was predicted by the RTPM motor sizing procedure. Inductance is known to change up to approximately 20 percent with temperature and current (see Ref. 6).

Motor B

Applying the RTPM-motor map development procedure resulted in a torque-speed map representative of the Motor-B torque-speed performance map. This was considerably more difficult because the manufacturer process for the Motor-B map included FW technology in the baseline motor map without a clear indication of when FW was engaged. It was also possible that the motor map provided by the manufacturer included temperature-based variations. The moving losses and Coulomb torque loss were assumed since they were not directly provided. The torque losses were used to adjust the torque-speed map so that it was representative of regions of efficiency in manufacturer-developed Motor-B torque-speed map. The RTPM-predicted torque-speed map for Motor B is presented in Figure 11. The corresponding information for the RTPM-sized motor specifications was recorded in Table 6.

It was not known what temperature Motor B was rated because this information was not provided. The manufacturer-rated speed of 5,500 rpm was approximated based on the specification sheet. The resistance was sized similar to the provided motor specification sheet at 25 °C. The efficiency at the rated condition was not precisely provided but was read from the corresponding manufacturer-provided efficiency map. As well, the authors believed that the RTPM predictions in Table 6 were not as accurate as they were for the predictions of Motor A due to some challenges in deciphering the specification sheet for Motor B and

discrepancies across the specifications provided by the manufacturer.

Motor C (Simulink®)

Motor C was developed using the RTPM-motor map development procedure. The hover condition of the turboelectric lift-plus-cruise was determined to be resting on the intersection of the rated speed and torque condition of Motor C in Figure 12. The distance above the hover condition

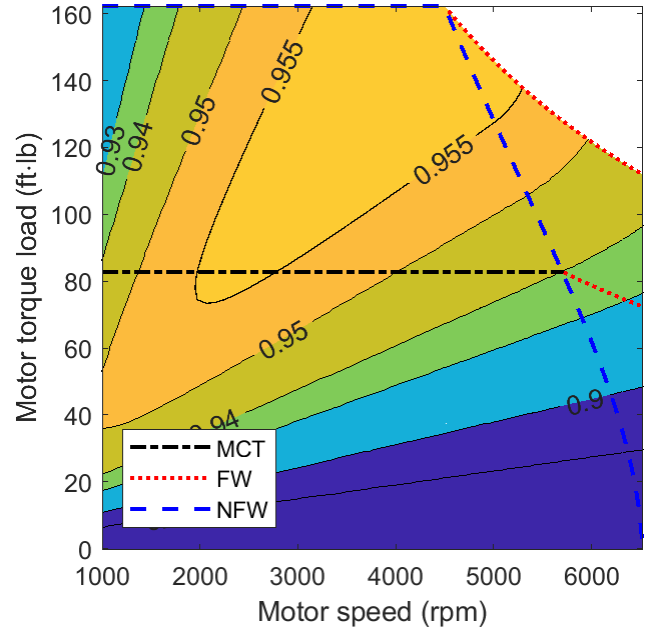


Figure 11. Motor B: Predicted Torque-Speed Map with Lines of Constant Efficiency.

Table 6. Motor B: RTPM Versus Manufacturer Specifications.

Parameter	RTPM	Manufacturer
Rated Speed (ω_m^r)	5,700 rpm	5,500 rpm
Rated Current (I_m^r)	149.39 A _{pk}	162.6 A _{pk}
Rated Torque (τ_m^r)	82.9 ft · lb	88.5 ft · lb
Peak Torque (τ_m^p)	162.3 ft · lb	169.6 ft · lb
Base Speed (ω_b)	4,499 rpm	4,500 rpm
No-load Speed (ω_m^{nl})	6,527 rpm	6,566 rpm
Rated Stator Resistance (R_s^r)	0.0187 Ohm	0.019 Ohm at 25 °C
Rated Inductance (L_q, L_d)	190 μ H	177/183 μ H
Rated Flux Linkage (λ^r)	0.0529 $\frac{V \cdot s}{rad}$	0.0542 $\frac{V \cdot s}{rad}$
Number of Pole Pairs (n_p)	10	10
Dynamic Viscous Coefficient (B)	0.069 $\frac{ft \cdot lb \cdot s}{rad}$	Not listed
Coulomb Torque (τ_c)	0.3731 ft · lb	Not listed
Motor inertia (J_m)	0.0186 slug · ft ²	0.0186 slug · ft ²
Rated Efficiency (η_m)	94%	94%
Power Factor (ϕ)	0.8785	Not listed
Rated Voltage (V_b^r)	545 V	670 V

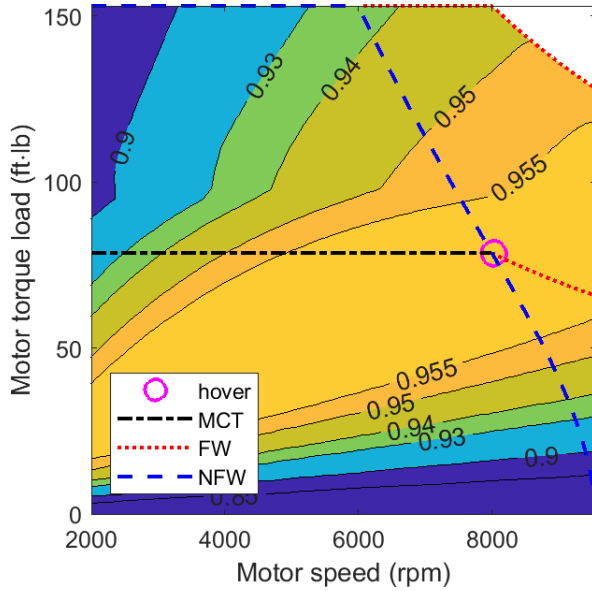


Figure 12. Motors C and D: Predicted Torque-Speed Map with Lines of Constant Efficiency.

to the peak torque line represents the torque margin available for maneuvering.

The temperature-based changes to resistance and flux linkage were incorporated into the motor map efficiency computations in Figure 12. While operating the motor in the peak torque range, the temperature climbs, potentially leading to a motor failure. Abrupt change in the efficiency lines in Figure 12 was an indication of the temperature climbing to 180 °C after being held at steady state. The motor temperature for the analysis was assumed to be fixed once the temperature climbed to 180 °C.

The RTPM-predicted motor parameters were recorded in Table 7. The motor inertia of 0.0208 slug · ft² was sized based on rotor inertia of a motor with similar torque capability in a Parker (Parker Hannifin Corporation, Cleveland, Ohio) GVM series motor catalog. A key feature of Motor C – and what makes it different from Motor D – was that Motor C was modeled completely using MATHWORKS Simulink® blocks.

Motor D (Simscape®)

Motor D shares the physical and electrical characteristics of Motor C. Motor D was modeled in MATHWORKS Simscape® with traditional PMSM model physics and a three-phase inverter. Development for Motor D was for verification of RTPM physics-based FW control. A block diagram of the Motor-D simulation architecture is provided (see Figure 13). Currents (I_A, I_B, I_C) and voltages (V_A, V_B, V_C) in the ABC frame were converted to the QD-axis currents using Park and Clarke transformations which rely on the electrical angle, θ . The control laws were developed to command-and-control QD-axis currents while not violating inverter saturation limits.

The motor control laws, stator and rotor dynamics for Motor D are presented together (see Figure 14). The Q-current equation of motion was modeled as in Equation (77):

$$\dot{I}_q L_q = V_q - \omega_m L_d n_p I_d - \lambda n_p \omega_m - I_q R_s \quad (77)$$

The D-current equation of motion was modeled as in Equation (78):

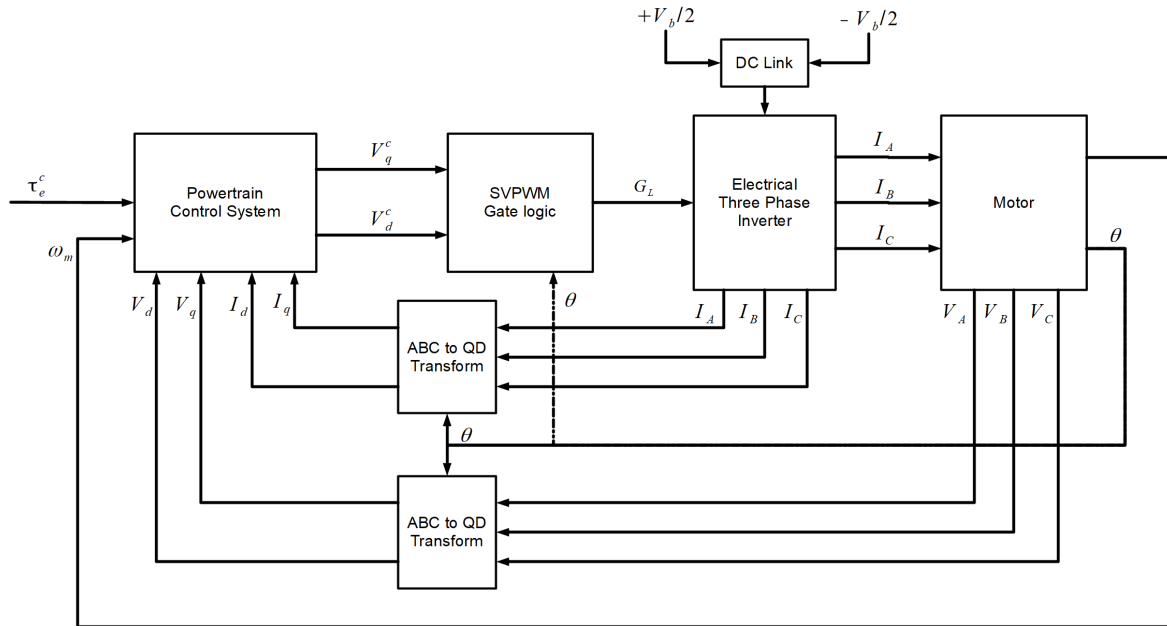


Figure 13. Motor D Simulation Architecture.

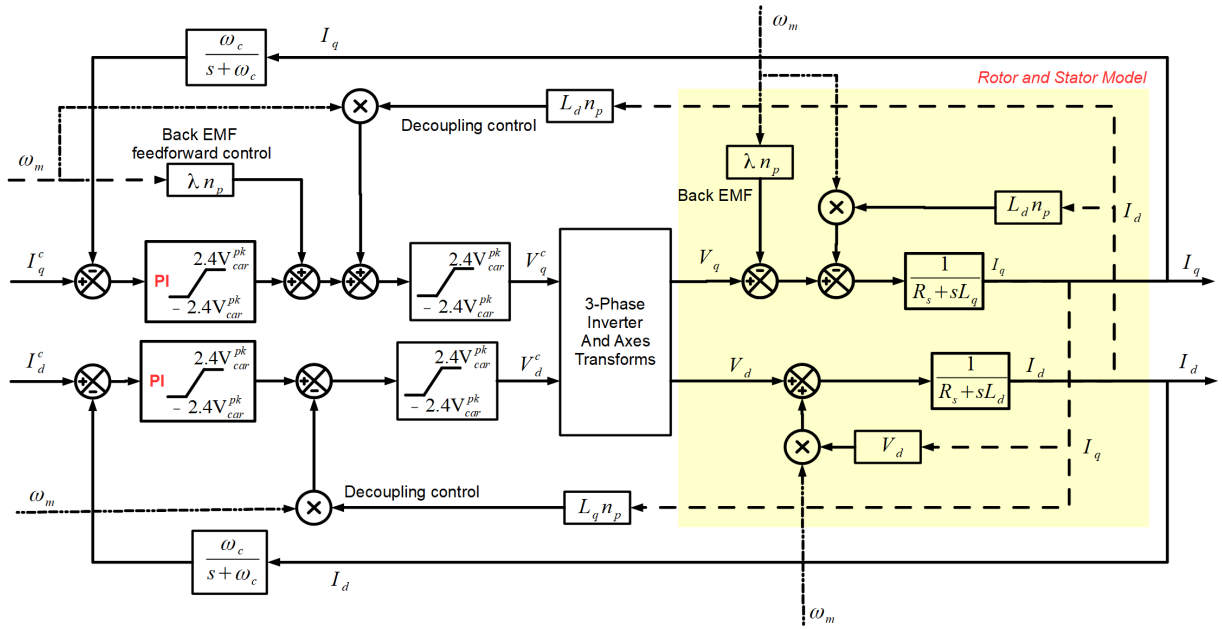


Figure 14. Motor D: Electrical Stator, Rotor, Inverter and Control Laws.

$$\dot{I}_d L_q = V_d + \omega_m L_d n_p I_q - I_d R_s \quad (78)$$

The voltages V_d and V_q result from control law commands converted to 6-pulse PWM gate logic, which were provided to the inverter control logic. The time domain solutions to Equation (77) and Equation (78) were used to update I_q and I_d . The control laws included traditional proportional integral (PI) control laws, decoupling control (Ref. 17), and back-EMF feedforward control for the QD-axis currents. Decoupling control and back-EMF feedforward control were used to better isolate the impact of the PI control laws. The sensor feedback to the PI control laws was filtered with a first-order filter of bandwidth, ω_c . The current commands to

the PI control laws, I_q^c and I_d^c , were provided by a FW control strategy (Ref. 18) that switches between weakening the flux and MTPA control depending on where the motor was operating in the torque-speed map. Stator voltage control commands were limited by approximately 2.4 times the inverter carrier peak voltage, V_{car}^{pk} . The control laws produce stator voltage commands, V_q^c and V_d^c , which were, in turn, provided as control inputs to the inverter gate logic.

The FW control logic which provides current commands to the PI control laws was shown in Figure 15. The modulation index was used to identify how close the inverter was to saturation. The modulation index was computed as in Equation (79):

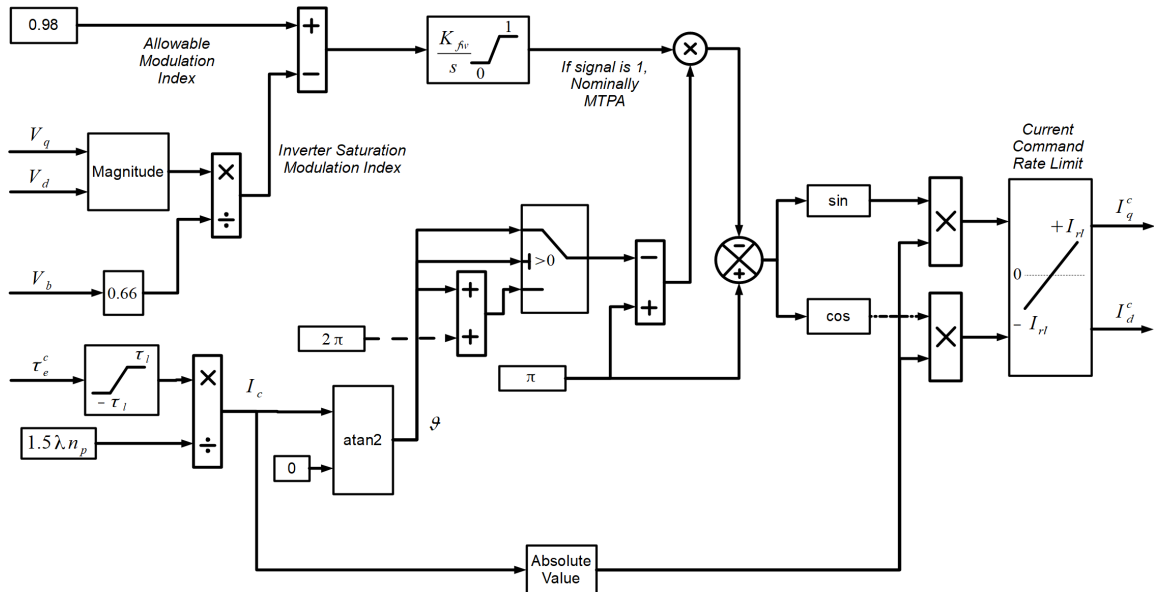


Figure 15. Flux Weakening Control Logic.

Table 7. Motor C and Motor D.

Parameter	RTPM
Rated Speed (ω_m^r)	8,000 rpm
Rated Current (I_m^r)	312.9 A _{pk}
Rated Torque (τ_m^r)	78.6 ft · lb
Peak Torque (τ_m^p)	153.0 ft · lb
Base Speed (ω_b)	5,999 rpm
No-load Speed (ω_m^{nl})	9,558 rpm
Rated Stator Resistance (R_s^r)	0.0120 Ohm
Rated Inductance (L_q, L_d)	77.2 μ H
Rated Flux Linkage (λ^r)	0.0387 $\frac{\text{V} \cdot \text{s}}{\text{rad}}$
Number of Pole Pairs (n_p)	6
Dynamic Viscous Coefficient (B)	0.0019 $\frac{\text{ft} \cdot \text{lb} \cdot \text{s}}{\text{rad}}$
Coulomb Torque (τ_c)	0.151 ft · lb
Gear Ratio (G_r)	7.616
Motor Inertia	0.0208 slug · ft ²
Rated Efficiency (η_m)	96%
Power Factor (ϕ)	0.848
Rated Voltage (V_b^r)	350 V
Voltage for Peak Torque at 8,000 RPM	458 V

$$M \triangleq \frac{V_b}{0.66 \sqrt{V_q^2 + V_d^2}} \quad (79)$$

When 98 percent of the modulation index computed in Equation (79) was exceeded, the integrator state of the FW control law decreased from one, providing a gain on the current control angle output, ϑ , from the atan2 block (see Figure 15). The current control angle was defined using the Simulink® block atan2 as in Equation (80):

$$\vartheta \triangleq \text{atan2}(I_c, 0) \quad (80)$$

where I_c is the command current extracted from an external torque command provided by a flight control law. The current control angle, as computed by Equation (80), ensured that when the FW control command was equal to one, MTPA was assumed to be operating. The MTPA control works by commanding D-current to zero. The Q-command current was defined as in Equation (81):

$$I_q^c \triangleq \begin{cases} \sin(\pi - (\pi - \vartheta)u_f) & \vartheta > 0 \\ \sin(\pi - (-\pi - \vartheta)u_f) & \vartheta \leq 0 \end{cases} \quad (81)$$

where u_f is the FW control law command. The D-command current was defined as in Equation (82):

$$I_d^c \triangleq \begin{cases} \cos(\pi - (\pi - \vartheta)u_f) & \vartheta > 0 \\ \cos(\pi - (-\pi - \vartheta)u_f) & \vartheta \leq 0 \end{cases} \quad (82)$$

The resulting commands were passed through a rate limit, which, for the comparison conducted in this paper, was set to a value large enough to avoid rate limiting. An important aspect of this FW approach was that current does not increase.

Parameters used in the Simscape® simulation for Motor D were provided in Table 8. The rotor inertia of 13.5 slug · ft² was computed from turboelectric lift-plus-cruise rotor blade parameters. Information on the Insulated-Gate Bipolar Transistor (IGBT) resistance, diode forward voltage, and snubber resistance was provided. The control gains in Table 8 were also provided.

Table 8. Motor D: Simulation Parameters.

Parameter	Value
Rotor Inertia (J_r)	13.5 slug · ft ²
Inverter Carrier Frequency	50 kHz
Carrier Peak Voltage (V_{car}^{pk})	350 V
IGBT Resistance	0.001 Ohm
IGBT Diode Forward Voltage	2 V
Snubber Resistance	100 kOhm
QD-axis Proportional Gain	10
QD-axis Integral Gain	10,000
Flux Weakening Control Gain (K_{fw})	75
Speed Control Proportional Gain	1
Speed Control Integral Gain	3
Speed Control Rate Limit	5,811 $\frac{\text{rpm}}{\text{s}}$
Simulation Time Step (Δt)	5e-7s

Battery Model

The battery model was sized using the NDARC-provided total energy of the backup battery for the turboelectric lift-plus-cruise of 262.9 megajoules (MJ). The usable battery block energy from NDARC was provided as 210.4 MJ. The battery model used for Motor-C simulations is provided in Table 9. The battery model was computed to have a total energy of 267.2 MJ with a useable energy of 227 MJ, assuming an average voltage of 3.6 V per cell. The value of cells in series and in a pack was selected to be 143. The number of packs in parallel was selected to be 41. The

Table 9. Battery Model.

Parameter	Value
Number of Cells in Series (N_s)	143
Number of Packs in Parallel (N_p)	41
Total Battery Block Energy	267.2 MJ
Useable Battery Block Energy	227.9 MJ
Cell Capacity (C_c)	3 Ah
Cell voltage (V_c)	4.22 V
C-Limit	15
Initial State of Charge (S_{oc})	40%

determination of the battery block cell configuration was conducted through iterative simulation.

The sizing objective was to find a starting battery voltage that would barely allow a single motor to turn the highest torque load of 128.08 ft-lb at an operating speed of 9,558 rpm when the battery was initialized from a 40-percent state of charge. The 40-percent state-of-charge value was selected so that the effects of a low state of charge could be readily demonstrated with a single motor. The voltage drop empirical formulas were complex, so the authors found that it was easier to determine the battery block configuration through simulation.

The backup battery of the turboelectric lift plus cruise is expected to be relied on only once if the turbogenerator fails; therefore, a C-limit equal to 15 was allowed. A known was that the empirical equations for the battery might not adequately capture the effects of current operating at such high C-limits; however, it was unknown what current would be required for simulation, and C-limiting was not intended to be used as a hard limit for the powertrain. From a practical perspective, if a 15-C-limit were to be reached, the battery block would likely experience damage and the battery block would need to be replaced after the emergency event.

RESULTS

The studies provided in the following subsections were intended to provide verification and validation for RTPM performance. The Motor-A study was used to verify that the motor map development process was reasonably accurate by comparing the RTPM-predicted torque-speed map to the manufacturer-simulated data for the motor in the SPEED testbed. The Motor-B study provided validation results that showed the simplified motor modeling process models the important electrical characteristics of an electric motor. This validation study was conducted by comparing RTPM-simulated data to Motor-B experimental data recorded in the AREAL testbed. The Motor-C and Motor-D verification study was conducted to verify performance accuracy of Motor C in the FW region.

The turboelectric lift-plus-cruise battery model was validated at the battery cell level. The validation study was conducted using two experimental test data sets with fresh 18650 batteries that were purchased in 2019. The battery model was connected to Motor C (the turboelectric lift-plus-cruise motor) to verify that the RTPM motor model interacted as expected with the battery and temperature model. The RTPM is demonstrated using two VMS study data sets. The VMS study data sets were recorded from a pilot study in the autumn of 2022. The studies demonstrate that motor temperature and battery voltage can affect motor performance.

Motor A Speed Map Comparison

The objective of the comparison of the RTPM-predicted motor map to the manufacturer motor map was to verify the

motor mapping equations were consistent with manufacturer predictions on performance. The Motor-A manufacturer-simulated data was compared to the Motor-A map data (see Table 5) that was predicted by RTPM (see Figure 16). As shown in Figure 16, the manufacturer data for motor torque, load power, efficiency, stator voltage, and stator current for both peak and continuous motor operations, compared well to

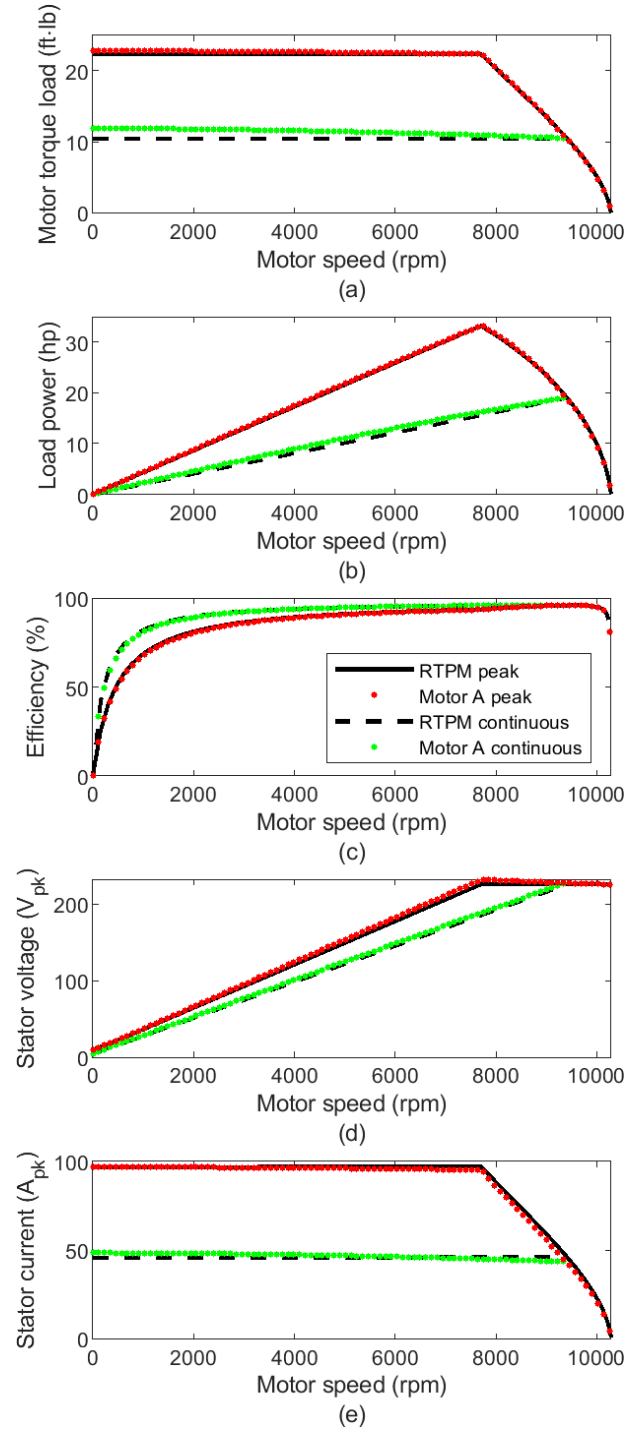


Figure 16. Motor A versus RTPM maps: (a) motor torque load; (b) load power; (c) efficiency; (d) stator voltage; and (e) stator current.

RTPM-simulated data. The close comparison confirmed to the authors that the motor map development process worked as intended.

There were notable differences, however, and we observed that at the motor stall speed (i.e., 0 rpm), the continuous torque load of the manufacturer data (see Figure 16(a)) was initialized at a higher torque load than the RTPM-simulated data. The continuous torque load of the manufacturer data descended as motor speed increased until the torque load reached the rated torque condition which intersected with the RTPM-predicted rated speed and torque load condition. The RTPM prediction of continuous torque load may therefore be conservative. Postulated by the authors was that the manufacturer-simulated data must assume a constant temperature. A constant temperature assumption might allow for a higher torque load at lower motor speeds because moving losses would also be lower.

Of note was that the peak load power (see Figure 16(b)) peaked at the base speed. This result was consistent with expectations that power should be highest at the base speed. The continuous load power (see Figure 16(b)) peaked at the rated speed as expected. Of importance for eVTOL designers is knowing the different speeds where power peaks in a continuous and intermittent sense as in Figure 1.

The efficiency (see Figure 16(c)) was shown to be lower when operating at peak condition, than that of continuous operating conditions. The lower efficiency at the peak torque power conditions occurrence was attributed to the significant increase in torque load and therefore resulted in higher resistive losses. As motor speed increases, the continuous and peak efficiency converged at the rated speed. The convergence occurred because back-EMF builds with increasing motor speed reduces motor ability to turn torque loads. At a high enough speed, known as the no-load speed, the motor cannot provide any torque to the load.

The stator voltage shown previously in Figure 16(d) climbed as speed increased, which was consistent with the discussion of increasing back-EMF with motor speed. The stator voltage must increase to overcome both internal motor resistance and back-EMF. Similarities between peak and continuous stator voltage occurred because the difference in voltage was primarily due to internal motor resistance, which is substantially smaller than back-EMF.

In the case where there was peak-current draw, we observed that the stator current (see Figure 16(e)) remained constant for RTPM and mostly constant for Motor A until the base speed was achieved. In the case where there was continuous-current draw, the stator current also remained mostly constant until the rated speed was achieved. In both cases, the current then decreased rapidly towards zero with increasing motor speed. The decrease in current was primarily due to a buildup in back-EMF with increasing speed.

Motor B AREAL Data Comparison

The objective of the experimental data comparison was to show that the RTPM predicted the time history behavior of the powertrain with response to input torque commands. To support this objective, experimental data was recorded for Motor B (see Table 6) during single-string testing in the AREAL testbed. To collect the input data to provide to the AREAL testbed driving motor, command torque and motor speed data were recorded from a fixed-base simulation study in the NASA Armstrong Flight Research Center Handling Qualities Lab (HQL) with the battery-electric lift-plus- cruise variant simulation used for VMS testing (Ref. 3) during the autumn of 2022.

An arbitrary maneuver was flown in the HQL by a test engineer. The data from the left-front motor was recorded in the HQL and subsequently scaled down to reduce the speed command to the motor in AREAL. For safety reasons, the speed was not allowed to exceed 5,000 rpm, whereas the torque command data was not allowed to exceed 100 Nm. After the scaling, only a subset of the data was used for the actual test in the AREAL testbed. The AREAL testbed includes a driving motor and load motor on the same shaft. The speed command from the HQL was provided to the driving motor operating in speed hold mode. The scaled torque load from the HQL was provided to the load motor operating in torque mode. The load motor provides a counter-torque on the shaft which must be overcome by the driving motor.

The data recorded during the AREAL single-string testing on Motor B was run through RTPM and the recorded data was compared to RTPM-simulated data (see Figure 17). When running the AREAL, Motor B recorded data through RTPM, the Motor-B speed measurement was assumed to be the speed command to the speed controller in RTPM. The Motor-B torque measurement was assumed to be the torque load in the RTPM shaft model (see Figure 6). It was shown in Figure 17 that the speed, torque, stator current, stator voltage, and bus power compared well, satisfying the objective of validation of capability for RTPM to predict powertrain response in respond-to-input torque commands.

Additionally, we observed that the speed from RTPM very closely matched the speed from Motor B (see Figure 17(a)). This close match was made possible by increasing the speed control gains in RTPM to reduce speed error. This decision was made by the authors to ensure that the shaft torque compared adequately to Motor-B torque load data (see Figure 17(b)).

The RTPM stator current (see Figure 17(c)) was observed as being noisier in comparison to the stator current from Motor B. The authors believed that the noise might have been due to recovering the current through a stator-resistance division, as shown previously in Figure 4. The stator resistance is typically very small and might amplify noise during the division. Another potential source of noise might have been

from motor operations which occur on the edge of numerical stability. The current and voltage rate limits, as shown previously in Figure 4, prevent RTPM from going unstable, but there could be residual noise.

Further observations may be made from the stator voltage (see Figure 17(d)). While observing Motor-B stator voltage data,

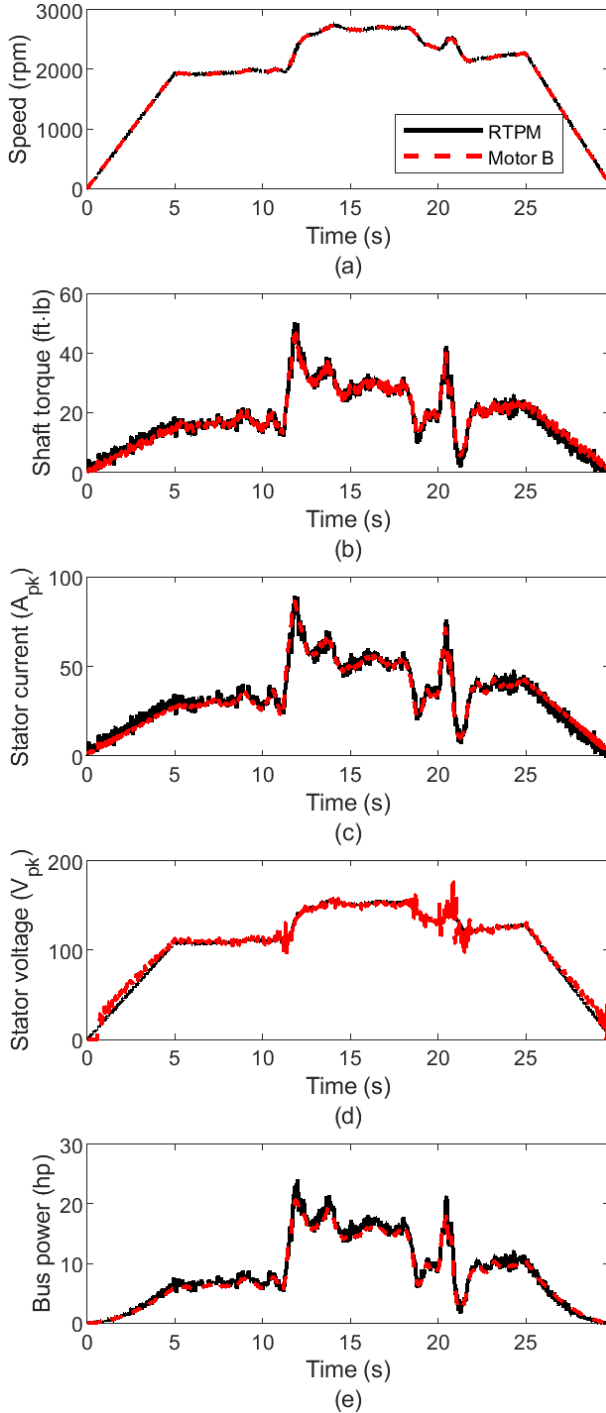


Figure 17. RTPM versus Motor B: (a) motor speed; (b) shaft torque; (c) stator current; (d) stator voltage; and (e) bus power.

the motor speed decreased below 1,000 rpm, Motor-B stator voltage slightly exceeded RTPM stator voltage, possibly indicating missing low-speed motor physics which were not captured within RTPM. These observed differences in predicted stator voltage would likely be most relevant during motor startup, as vehicle operations predicted by NDARC would occur near the motor-rated speed. Finally, significant noise presented in the Motor-B stator voltage between 20 and 23 s (see Figure 17(d)). The source of this noise and whether it was due to sensor error was not determined.

We observed that measured bus power previously shown in Figure 17(e) compared well to RTPM bus power data, but Motor-B bus power decreased slightly below RTPM bus power. The Motor-B bus power was computed by multiplying recorded battery bus voltage and current. The RTPM power for this investigation assumed bus voltage was constant. The RTPM did not account for a very slight decrease in bus voltage that was present in the Motor-B bus voltage data.

Motor C versus Motor D

The objective of comparing Motor C to Motor D was to show that the RTPM modeling approach adequately predicts the performance of a non-real-time higher fidelity motor model equipped with a three-phase inverter. The physical and electrical characteristics (see Table 7) of the motor and shaft were assumed to be identical, but additional simulation parameters were needed for Motor D (see Table 8). Motor D used a similar configuration to that of the AREAL testbed. In this configuration, the driving motor must overcome a load motor on the same shaft. For better comparison, Motor C and Motor D were equipped with the same speed control laws and driven in the manner presented in Figure 18.

The results of the comparison were provided in Figure 19. Observations from the study results are presented (see Figure 19) and show that the motor speed, QD voltages and currents compared well, satisfying the objective that RTPM can be used to predict complicated motor physics.

Furthermore, during the comparison, we observed that the speeds of Motors C and D were not on track to capture the 10,000-rpm command (see Figure 19(a)). This inability to capture the rpm command was a direct result of torque limiting in both Motors C and D. The observed sluggish response to the speed command occurred because both motors were operating with a large counter-torque of 128.08 ft-lb in the FW region (see Figure 12) where torque margin was very low.

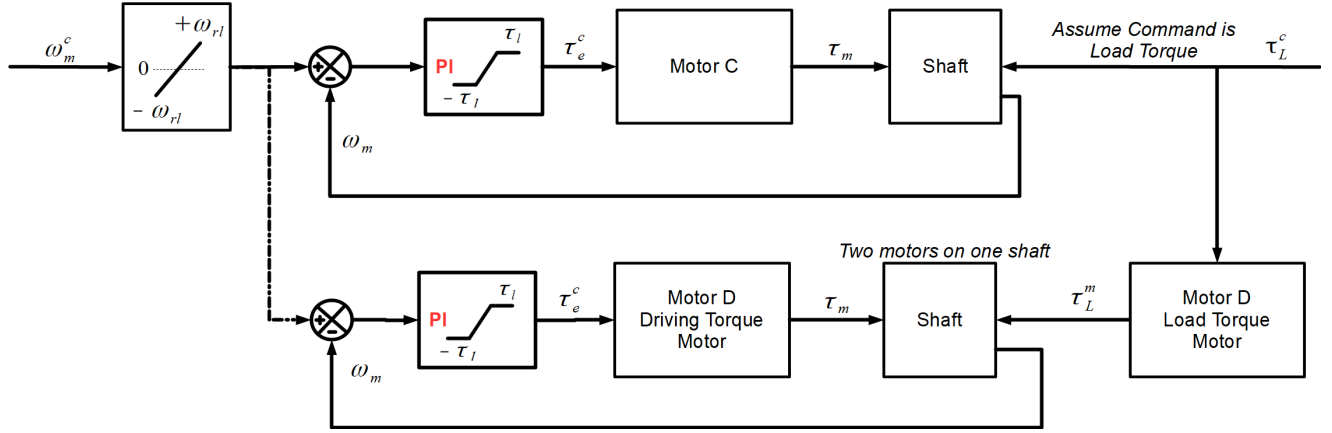


Figure 18. Motors C and D: Comparison Architecture.

When comparing the differences in Q-voltage (see Figure 19(b)); D-voltage (see Figure 19(c)); Q-current (see Figure 19(d)); and D-current (see Figure 19(e)), the largest differences were in Q-voltage and D-current. The D-current command in Motor C was derived from the physics-based FW control law (see Figure 5). Motor D produced D-current based on the logic as shown in Figure 15. Observation data (see Figure 19(e)) showed that the physics-based FW control law commanded more negative D-current. More negative D-current reduces the effect of back-EMF resulting in a lower Q-voltage requirement (see Figure 19(b)). The slight improvement of the RTPM FW control over the Motor-D FW control law may be why speed increased slightly faster for Motor C than for Motor D (see Figure 19(a)).

Timing Discussion

A runtime comparison between the two models, Motor C and Motor D, was illustrative of the benefits of RTPM. The Motor-D simulation ran 96 times slower than real time when running in Simulink® Accelerator mode. The Motor-C simulation ran 77 times faster than real time using Simulink® Accelerator mode.

The Motor-C simulation only includes one motor, while the Motor-D simulation includes two motors. When accounting for the difference in the number of motors, the RTPM modeling technique produces a motor which can run 3,696 times faster than a similarly designed Simscape® motor. Caveats include the following: (1) One-to-one time comparisons with auto-coded models of Motor C and Motor D were not conducted, and (2) The runtime efficiency of the Simscape® model was not optimized.

We found that during a timing test of the auto-coded RTPM simulation, which included eight lifting motors and one pusher motor for the turboelectric lift-plus-cruise, that the entire powertrain simulation ran 20 times faster than real time in the VMS. The base time step was set to 0.01 s. All powertrain components in Motor C were also looped 5,000 times every time step, resulting in an effective time step of

2e-6 s. The looping structure provided flexibility so that some components such as battery and temperature models may be solved at the base time step.

Battery Model Validation

The objective of the battery model studies in this subsection was to validate empirical battery equations modified from Ref. 13. A single battery cell modeled by RTPM was compared to a Samsung INR 18650 cell battery that was manufactured and tested in 2019. Two studies, including a 1-A and 2.5-A constant draw, were conducted. The data comparisons to RTPM were provided in Figure 20. The comparisons of temperature and voltage (see Figure 20) was considered relatively close by the authors, suggesting that the empirical equation adjustments met the accuracy objective of the battery model development.

In the 2.5-Ah study, however, it appeared that during the final minutes, the RTPM voltage (see Figure 20(a)) decreased more rapidly than the experimental voltage, suggesting worse performance of the RTPM battery model than that of the 18650-battery cell. Furthermore, for both the 1 Ah and 2.5 Ah cases, the experimental voltage tended to be slightly higher than RTPM voltage by 0.2 V to 0.05 V. This comparison suggested that the RTPM voltage modeling was conservative.

Of note was that the RTPM temperature led the experimental temperature data (see Figure 20(b)) for the 2.5-Ah case. The lead suggests that there may still be some adjustments that could be made to improve the battery thermal model. One potential area of improvement may be to update entropy empirical coefficients in Equation (70). The constants for the battery model provided in Table 3 were drawn from Ref. 14, and the battery characteristics may not all have been applicable to the Samsung INR 18650 battery cell. The authors observed that there was more temperature fluctuation in the experimental data recorded for the one Ah case; we believed that these temperature fluctuations were the result of small variations picked up in the digital thermometer used for recording the data.

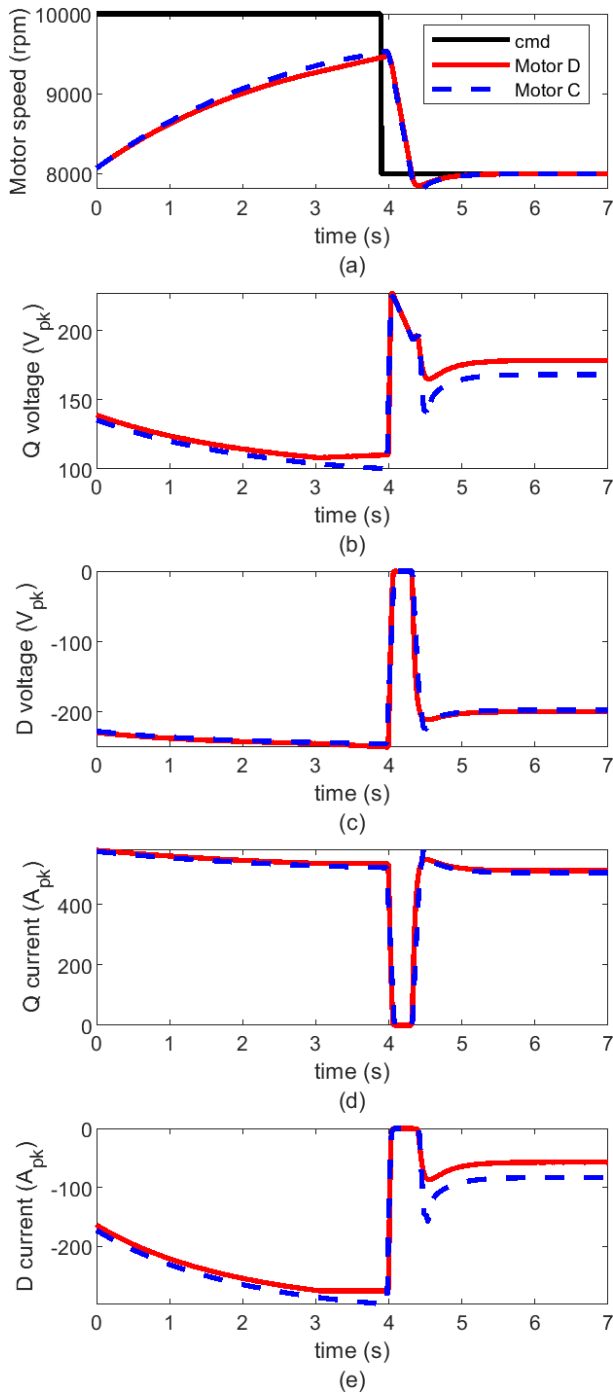


Figure 19. Motor C versus Motor D: (a) motor speed; (b) Q-voltage; (c) D-voltage; (d) Q-current; and (e) D-current.

Motor C with Battery Model

The objective of examining Motor-C performance with a battery was to verify the motor performed as expected in a low-battery-voltage situation. The backup battery of the turboelectric lift-plus-cruise (see Table 9) was initialized with a 40-percent state of charge to observe the effects of a single motor power draw. For better observation of the interplay between the battery and the motor, the motor-speed command

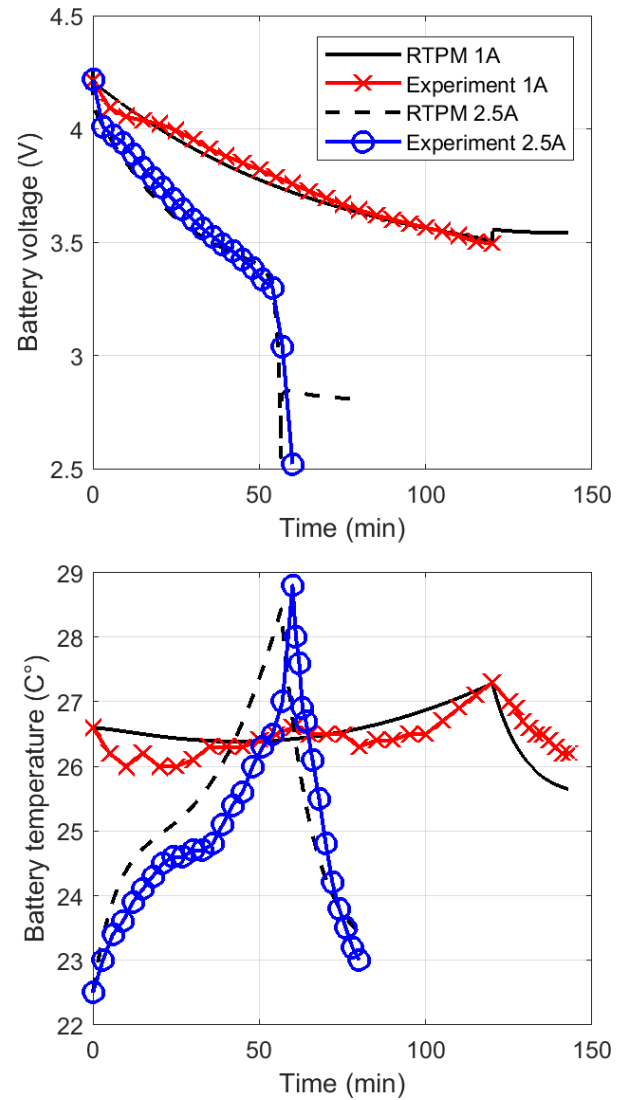


Figure 20. RTPM battery model versus experimental data: (a) battery voltage; and (b) battery temperature.

was pulsed between 4,000 rpm and 10,000 rpm, whereas Motor C was subjected to a constant counter-torque of 128.08 ft-lb. The results of pulsing Motor-C speed commands are shown in Figure 21.

The authors observed that between 1 and 2 min, shown in Figure 21, the motor speed plateaued near the no-load speed of 9,558 rpm and did not achieve the commanded 10,000 rpm (see Figure 21(a)). This result was expected due to prediction of the Motor-C peak torque limit of 128.08 ft-lb at 9,558 rpm in Figure 12. Between 3 and 4 min, the motor speed continues to degrade with respect to previous times. The degradation in motor speed captures worsens for all following speed-pulse commands. The only appreciable change in the powertrain, which occurred during the degradation in motor speed capture performance, was battery capacity drain; therefore, the authors concluded that the motor was performing as expected in the presence of a discharging battery.

We observed that the motor torque shown in Figure 21(b) continued to fluctuate from approximately 128.08 ft-lb to approximately 152 ft-lb during the pulses. As well, the torque tended not to decrease, whereas the motor speed (see Figure 21(a)) did decrease. This result suggests that the motor actuator driving an aerodynamic rotor prioritizes torque over

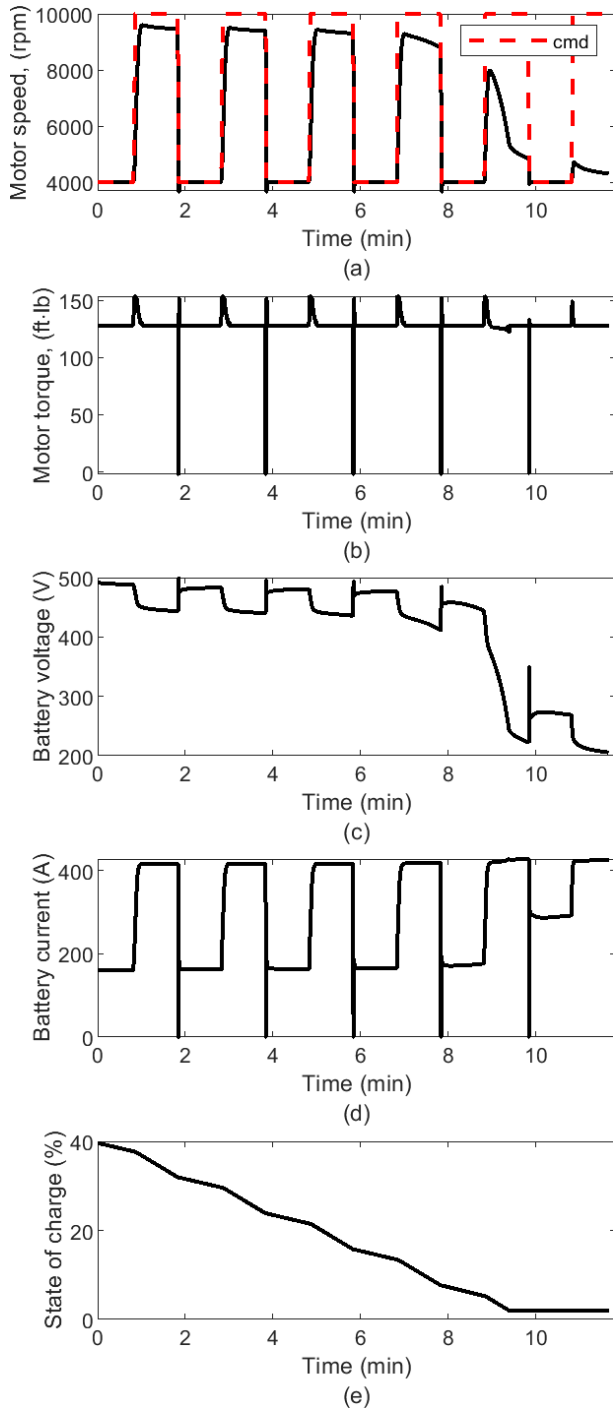


Figure 21. Turboelectric lift-plus-cruise backup battery model performance at 40 percent state of charge: (a) motor speed; (b) motor torque; (c) battery voltage; (d) battery current; and (e) state of charge.

speed. In a situation where the battery voltage decreases, and a fixed-pitch propeller and speed control law are assumed, the rotor speed would be likely to decrease, leading to loss of thrust. It may be that for a variable-pitch propeller control system the pitch of the rotor would be adjusted to account for the loss of speed; however, the increase in pitch angle of the rotor blades would lead to increased torque requirements, resulting in further decreases in rotor speed.

We observed in Figure 21(c) that there were battery voltage drops that occurred with each pulsed-speed command. During the first pulse, the battery voltage decreased from 510 V to 460 V. Following each pulse, the battery voltage decreased further than the voltage dip from the previous pulse. By the fourth pulse, the battery voltage decreases from 500 V to 370 V. Each voltage drop becomes more severe as state of charge decreases. This result confirms that the reason for reduced motor speed performance over time was the decrease in overall voltage as the battery state of charge decreased.

Also observed was that the battery current shown previously in Figure 21(d) appeared with a similar shape for the first four pulses in motor-speed command to 10,000 rpm. After the fifth speed-command pulse starting at 9 min, the current increased and remained high. This state occurred because the battery state of charge hit its minimum threshold of two percent near 8.5 min (see Figure 21(d)). Rather than burn up or shut down, the battery continued to operate; however, these effects were not explicitly modeled. The reduced battery voltage impact on rotor speed and thrust would already have been felt by a pilot in simulation.

VMS Battery Study

The objective of this subsection was to demonstrate that battery dynamics may affect vehicle response characteristics. There have been two versions of RTPM. The first version of RTPM was tested in the VMS in the autumn of 2022 (see Ref. 3). The first version required parallel processing and one motor per computational thread. The motor configuration and its characteristics were representative of Motor C but not exact. The control and modeling architecture (see Figure 14) was identical to that used by Motor D but with necessary conversions into Simulink®. The Motor-D Simscape® simulation was originally used for verification of the first version of the RTPM in Simulink®.

A pilot flew an Aeronautical Design Standard (ADS)-33 (Ref. 19) vertical maneuver using the first version of RTPM. The same motor-speed commands were presented to the battery-electric lift-plus-cruise variant simulation (see Ref. 3) with different battery states and configurations to examine the effects of the battery. The result was presented in Figure 22.

The three cases examined include the following: (1) a constant battery voltage; (2) a state of charge of 100 percent; and (3) a state of charge of 20 percent. An examination of the altitude rate (see Figure 22(a)) suggests that the altitude rate was affected by both including voltage droop in the model and

variation of voltage due to state of charge. A small delay was observed in the 100-percent state-of-charge case as compared to the constant battery voltage case. A large delay in altitude rate and a 25-percent reduction in the peak altitude rate occurred when state of charge was initialized to 20 percent.

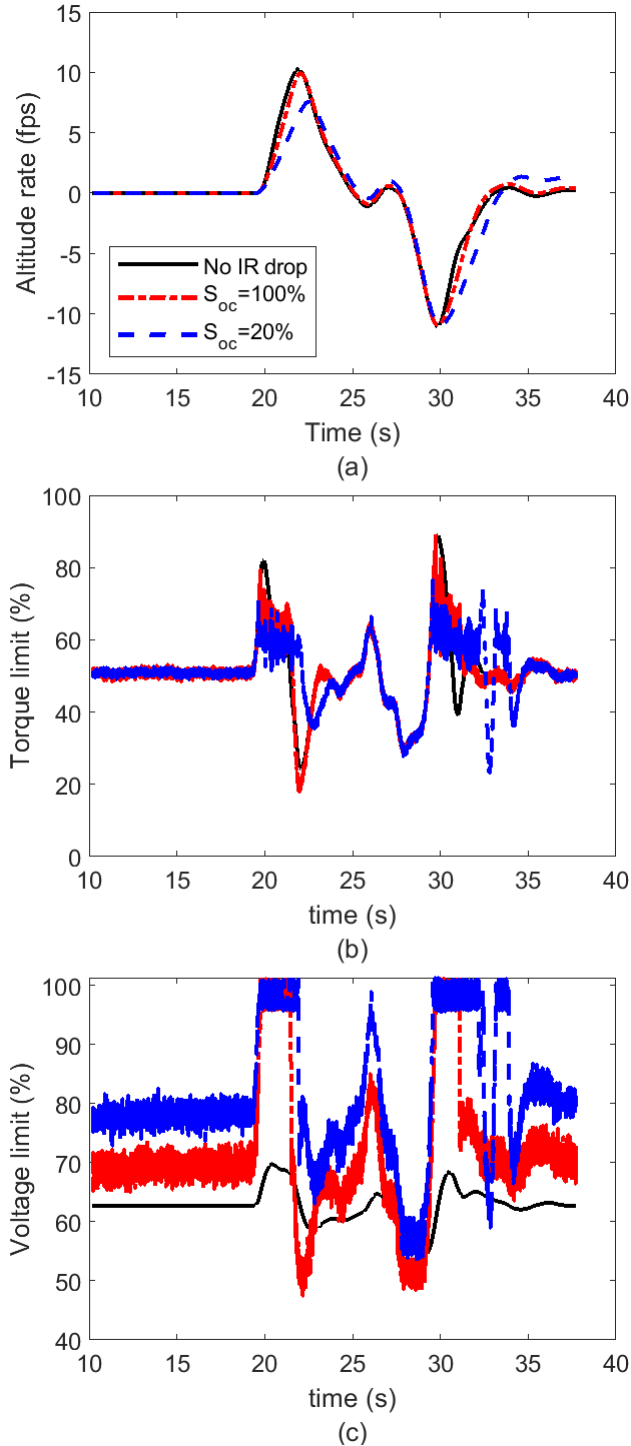


Figure 22. Piloted battery-electric lift-plus-cruise variant vertical maneuver depicting left wing-tip motor with state-of-charge variation: (a) altitude rate; (b) torque limit; and (c) voltage limit.

The motor torque seen in Figure 22(b) did not appear to be directly limited by peak torque limits; however, the torque produced by the motor changed depending on the battery configuration. The lowest torque production (see Figure 22(b)) coincided when the state of charge decreased to 20 percent. An examination of the percent of torque limit – defined as a percent of torque with respect to the peak torque of the motor – showed that the motor for all three battery configurations was not torque saturated.

While torque was not hard-limited (see Figure 22(b)), torque limiting did occur but was originating from voltage rather than some pre-defined peak torque limit. The percentage of voltage limit that was observed (see Figure 22(c)) was computed as the percent of stator voltage compared to the theoretical voltage limit: 66 percent of battery voltage. At time 0, the percent of voltage limit was 80 percent when the state of charge was equal to 20 percent. When the altitude maneuver began, voltage limiting in the 20-percent case was observed near 20 s. When the percent of voltage limit touched 100 percent, this percentage was a strong indication that the FW control law (see Figure 15) was engaged. The lift-plus-cruise flight control laws tended to maximize motor torque usage whenever FW was engaged; therefore, torque limits (derived from voltage) were met whenever FW was engaged.

Motor C with Thermal Model

The first objective of analyzing Motor C with the thermal model was to show that when counter-torque increases on the shaft, motor components heat up as predicted. The second objective of the thermal model presentation was to verify that the rated temperature of the motor was achieved when the motor operated at the rated speed and torque condition (see Figure 12).

The temperature variation analysis was conducted by increasing the counter-torque on Motor C from the rated torque by 50 percent for two 2-min pulses. The result of the analysis was provided in Figure 23. We observed (see Figure 23) that the temperature of all modeled lumped masses increased due to an increase in torque load; this result matched expectations of the authors concerning thermal model performance. Furthermore, we observed that the winding temperature initialized at 140 °C as expected.

Specifically, we observed that the winding temperature (see Figure 23(a)) climbed from 140 °C to 155 °C during the first torque load increase. The second torque load increase resulted in an increase from 150 °C to 162 °C. The temperature increase was less for the second torque load increase than the first torque load increase. This condition suggests that at some point, after enough constant peak torque pulses, the temperature will plateau. The motor speed was regulated to 8,000 rpm as shown in Figure 23(b), so the increase in temperatures were primarily due to increases in resistive heat losses from torque pulses (see Figure 23(c)).

The stator resistance (see Figure 23(d)) increased from 0.0128 Ohm to 0.0135 Ohm due to the increase in winding temperature. The motor physics where resistance changes due to temperature were identified previously in Figure 4. Motor resistance increase can have a compounding effect on temperature increase because resistive heat loss increases as a function of motor resistance. An increase in resistance

commensurately increased stator voltage, pushing the motor closer to the FW control region.

The authors observed that the flux linkage (see Figure 23(e)) decreased as rotor temperature increased. A decrease in flux linkage impacts both the back-EMF and the torque constant. A decrease in the back-EMF may appear useful, but the decrease in torque constant was not ideal and outweighed the benefit of reduced back-EMF.

VMS Temperature Study

The objective of this section was to characterize how temperature dynamics affect the battery-electric lift-plus-cruise variant vehicle response characteristics. The initial version of RTPM included a similarly designed temperature model, as documented in this paper, but without scaling (as shown in Equations (53) to (56)). A pilot flew an ADS-33 vertical maneuver using the first version of RTPM in the VMS in the autumn of 2022 (see Ref. 3). These motor-speed commands were provided to the battery-electric lift-plus-cruise variant simulation, with different temperature configurations, to understand the effects of the temperature model. The result was presented in Figure 24.

The three case studies presented (see Figure 24) include the following: (1) a constant motor temperature; (2) a nominal case with motor coolant set to a temperature of 60 °C; and (3) a high-temperature case with coolant temperature set to 120 °C. The winding temperature for cases two and three was 131.5 °C and 205 °C, respectively, with very little change in temperature during the short duration runs. Winding temperatures above 180 °C may lead to motor damage; however, this effect was not modeled.

The altitude rate (see Figure 24(a)) differed between the three cases. The difference in performance between the constant temperature case and nominal-coolant temperature case was like the difference observed between the constant voltage case and the state-of-charge of 100-percent case, as shown previously in Figure 22. So, there is a possibility that the difference was primarily due to the voltage droop due to current draw of the battery.

To determine if temperature affected performance of the vehicle, the nominal-coolant case and high-temperature case were examined more closely. The altitude rate for the high-temperature case was more sluggish than the nominal-coolant case and peaked at 25 percent lower than the nominal-coolant case altitude rate. The variation in temperature confirms that a higher temperature motor resulted in a more sluggish and limited altitude response.

Further observations were made by examining the operation of the motor. The percent of torque limit (see Figure 24(b)) for the high-temperature case was lowest when compared to the nominal-coolant case. As in the VMS battery study (see Figure 22), the torque of the motor in all three cases were not directly limited by a peak torque limit. The voltage limit

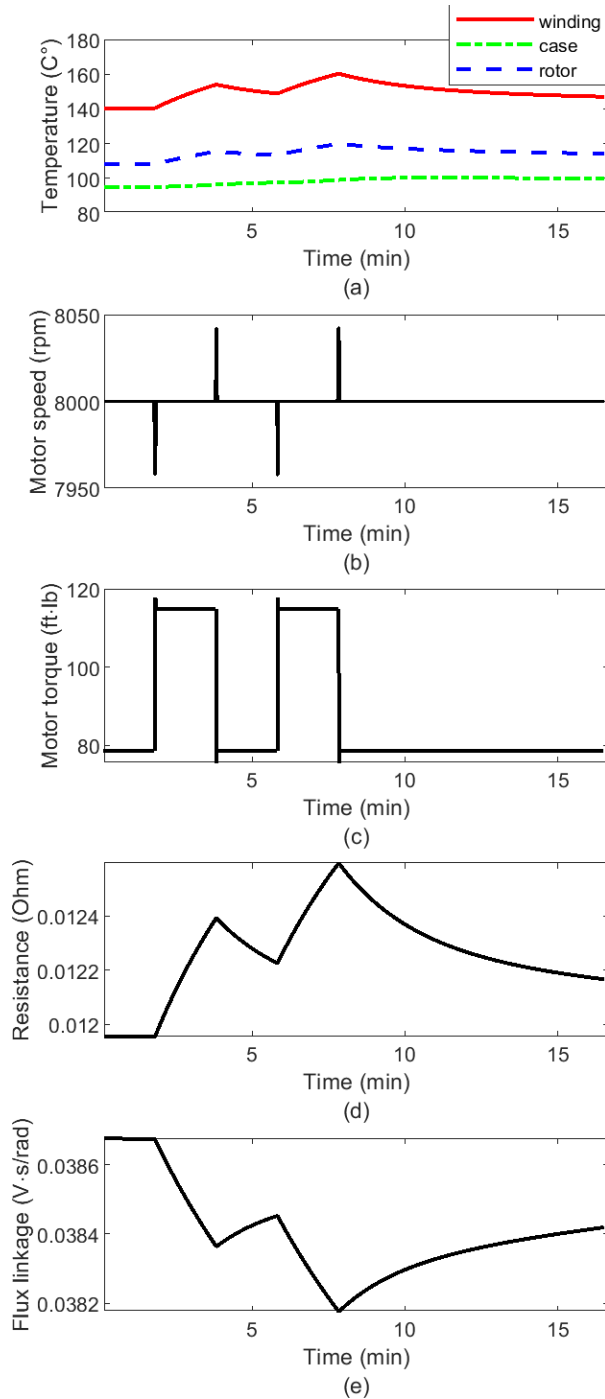


Figure 23. Motor C thermal model: (a) temperature; (b) motor speed; (c) motor torque; (d) stator resistance; and (e) flux linkage.

was met consistently for both the nominal-coolant and high-temperature cases (see Figure 24(c)). By examining the flux linkage, the high-temperature case experienced an approximate 12-percent reduction in the flux linkage relative to the nominal-coolant case. While both nominal-coolant and high-temperature cases were voltage limited, the reduction in

torque capability of the high-temperature motor resulted in the more sluggish and reduced altitude rate as shown previously in Figure 24(a).

CONCLUSIONS

The National Aeronautics and Space Administration (NASA) is studying the effect(s) of handling qualities early in the design of electric rotorcraft vehicles. The real-time powertrain played a key role in the simulation of these vehicles for handling qualities testing in the NASA Ames Vertical Motion Simulator (VMS). This paper presented the powertrain development process with a focus on model components and decision making to support VMS testing.

The motor sizing methodology was verified against manufacturer-simulated data for the motor arrangement in the NASA Scaled Power ElEctrified Drivetrain (SPEED) testbed. The computational model of the Real-Time Powertrain Modeling (RTPM) tool was validated against the motor in the Advanced Reconfigurable Electrified Aircraft Lab (AREAL) testbed. The AREAL motor data uncovered missing model physics at low-motor speed(s), which may be applicable primarily at motor start-up conditions. The RTPM was verified against a representative Simscape[®] motor arrangement in the Flux Weakening (FW) region. The physics-based FW control law was general enough to compare well with a realistic FW control law.

The motor sizing methodology was central to studying the impact of the powertrain on revolutionary vertical lift technology (RVLT) reference vehicle designs. The motor sizing methodology presented a bridge with the NASA Design and Analysis of Rotorcraft (NDARC) tool for real-time powertrain simulation.

The battery-electric lift-plus-cruise variant powertrain was studied and discussed in this paper, and motor performance was found to be affected negatively by off-nominal conditions such as low-battery state of charge and high temperature. Pilot Vertical Motion Simulator (VMS) testing with Real-Time Powertrain Modeling (RTPM) in the off-nominal conditions showed the peak altitude rate could be slowed down significantly and reduced by 25 percent during an ADS-33 vertical maneuver.

No model is perfect, and models based on empirical data become outdated as time progresses. Attempts were made to keep the powertrain models current when feasible, particularly when empirical equations were incorporated. The battery model updates were presented, and the battery model was compared to available battery data.

The thermal model was presented for a motor which is expected to be similar in size to most NASA Design and Analysis of Rotorcraft (NDARC) reference vehicle motors. The thermal temperatures of the motor trended in the expected direction in response to inputs, but no relevant experimental thermal data was available for a direct comparison.

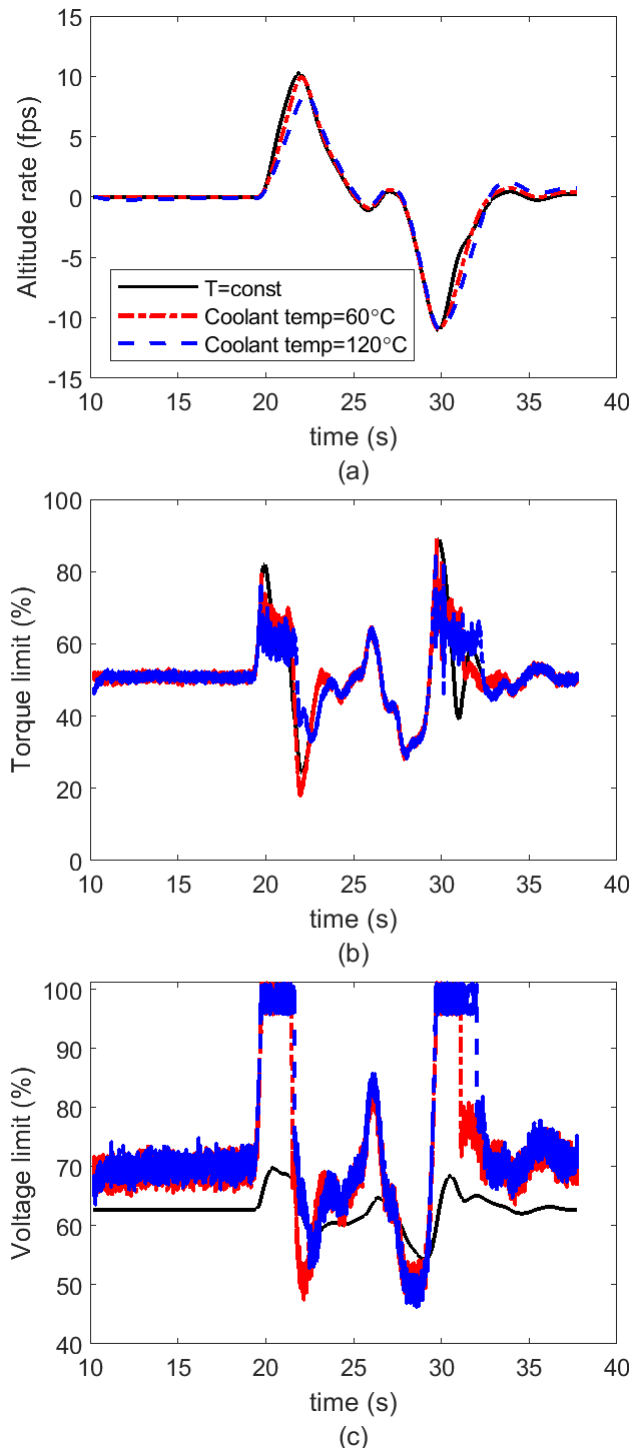


Figure 24. Piloted battery-electric lift-plus-cruise variant vertical maneuver depicting front left wingtip motor with temperature variation: (a) altitude rate, (b) torque limit, and (c) voltage limit.

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