

Control and Scaling Approach for the Emulation of Dynamic Subscale Torque Loads

Santino J. Bianco¹, Donald L. Simon²
NASA Glenn Research Center, Cleveland, Ohio, 44135, USA

Research and development of electrified aircraft propulsion powertrains are relying on the use of electromechanical systems to emulate turbomachinery/rotor loads. Replacing a physical turbomachinery/rotor with a model driving a subscale electromechanical system capable of emulating subscale torque loads and responses is a lower risk, lower cost alternative to using the full-scale turbomachinery/rotor for initial control system verification. This paper outlines a novel control and scaling approach for emulating dynamic subscale torque loads using electric machine (EM) hardware for electrified aircraft propulsion research and development purposes. The approach, known as the Sliding Mode Impedance Controller with Scaling (SMICS), drives a mechanically coupled, two-EM system to behave like a subscale hardware representation of a hybrid-electric turbomachinery shaft. One EM reflects the inertial dynamics and torque load of the subscale turbomachinery under steady-state and transient operation while the second EM represents a motor/generator connected to the shaft, which is intended to hybridize the turbomachinery. This closed loop control and scaling algorithm applies impedance and sliding mode control schemes, along with parameter scaling, to match subscale, desired dynamics in real-time and to allow this system to be driven by a full-scale hybrid-electric turbomachinery model and control. The paper elaborates on the concept of the closed loop control and scaling approach and explains the significance of using impedance and sliding mode control. It shows a derivation of the closed loop control and scaling algorithm, its implementation, and presents a comparison of theoretical and actual simulation results acquired during hardware-in-the-loop testing of a partial turboelectric propulsion concept aircraft at the NASA Electric Aircraft Testbed (NEAT). The results show that the intended dynamic responses of the hardware and the aircraft model are achieved in both the time and frequency domain. Full scale propulsion control systems can be tested using this hardware and software approach.

I. Nomenclature

α	= speed scalar
β	= torque scalar
B	= viscous damping coefficient
F_{int}	= interaction force
J	= shaft angular inertia
K	= robotic manipulator spring constant
K_D	= derivative gain
K_I	= integral gain
K_P	= proportional gain
η	= linear reaching gain
ϕ	= switching gain

¹ Control Systems Engineer, Intelligent Control and Autonomy Branch, and AIAA member.

² Control Systems Engineer, Intelligent Control and Autonomy Branch.

s	= sliding function
$\dot{s}$	= reaching law and time derivative of sliding function
$sat(\cdot)$	= saturation function
T	= torque
$V(s)$	= Lyapunov function
$\dot{V}(s)$	= derivative of Lyapunov function
ω	= shaft angular speed
$\dot{\omega}$	= shaft angular acceleration

Subscripts

$*_E$	= electrified turbomachinery
$*_G$	= research electric machine
$*_M$	= emulation electric machine
$*_{net}$	= sum of individual variables
$*_P$	= coupled electric machine system
$*_S$	= subscale

II. Introduction

At the present time, electrification of existing turbomachinery systems shows significant promise at improving the performance, efficiency, and operability of aeronautic propulsion systems. The National Administration and Space Administration (NASA) Aeronautics Research Mission Directorate (ARMD) is prioritizing research in this area by investing in the design and implementation of aeronautic hybrid-electric propulsion testbeds [1, 2]. These testbeds are intended to test electromechanical components and control algorithms for hybrid electric propulsion systems in an environment without turbomachinery. The goal of these testbeds is to raise the technology readiness level (TRL) of many enabling technologies in the subject area of electrified aircraft propulsion (EAP). Among these testbeds is the NASA Electric Aircraft Testbed (NEAT), designed to test mega-watt scale EAP electrical and/or electromechanical components to verify their operation before use on a flight demonstrator. Among the many technologies likely to be integrated with the next generation of electrified aircraft is advanced control algorithms that take advantage of an electromechanical system to move power between electrified propulsion subsystems to benefit the performance, efficiency, and operability of an aircraft. An electrified propulsion subsystem could refer to a gas turbine engine, an electrically driven propeller, or an energy storage system such as a battery. These electromechanical systems for EAP applications come in a plethora of configurations, but in all cases, it is common to have individual electric machines (EMs) mechanically coupled to each shaft. The shafts are often coupled to turbomachinery or a propeller/fan. The EMs are electrically coupled to a power management/distribution system that transfers power between the mechanical and electrical systems. For example, power may be extracted from the low-pressure shaft of a turbofan using an EM and transferred to electric propulsors on an aircraft wing using an electrical power system.

Conventionally, the verification and validation of these aircraft engine control algorithms begins with simulation, continues with control rig tests, and culminates with full-scale ground testing on turbomachinery. Turbomachinery is expensive, costly to operate, and requires that a significant number of safety precautions. The initial testing of advanced EAP control algorithms on full-scale turbomachinery is compounded by the requirement for electromechanical system hardware. This presents significant operational and safety risks to the turbomachinery and electromechanical system if the algorithm does not operate as intended. For these reasons removing turbomachinery and scaling down the electromechanical system during the initial testing of these algorithms would eliminate any risk associated with the turbomachinery, greatly reduce the financial risk of damaging full-scale flight-capable electromechanical hardware and reduce the required in-place safety measures normally required to conduct the testing of these algorithms. To achieve these goals, the turbomachinery must be replaced with a controllable, torque-producing device that closely approximates the behavior of the turbomachinery. At NEAT, in other labs at NASA, and in the aerospace industry, the device used to accomplish this is an electric machine (EM), often referred to as the “emulation EM”. The emulation EM is mechanically coupled to a subscale “research” EM and subscale electrical power system. The emulation EM provides an approximate torque load that is indicative of the subscale turbomachinery torque load. Together, the combination of the emulation EM, the subscale research EM, and the subscale electrical power system represent the subscale electrified shaft of a gas turbine engine. The goal of this representative system is to become an equivalent replacement for the full-scale turbomachinery and its full-scale electromechanical system, which can then be commanded by using a software model of the turbomachinery and its controller. Essentially, this partially software, partially hardware system constitutes a subscale, hardware-in-the-loop

(HIL) testbed, devoid of actual turbomachinery, yet capable of verifying advanced hybrid-electric propulsion control algorithms in a low-risk environment on a realistic, subscale hardware system. Fig. 1 illustrates this testbed concept. The grey shaded regions indicate equivalent systems, where the gas turbine engine is replaced by closed loop control, scaling, the emulation EM, and a full-scale turbomachinery model. Figures 1a and 1b are the conceptual control diagram implementations with and without turbomachinery. Of course, the implementation of the emulated turbomachinery system must take into consideration the mechanical characteristic differences (e.g., angular inertia, viscous friction) for the system to become a subscale dynamic representation of an electrified gas turbine engine shaft(s).

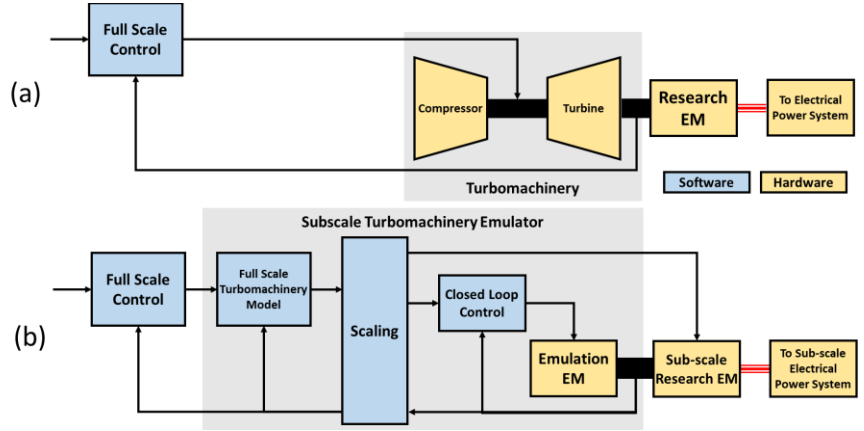


Fig. 1. Illustration depicting the difference between implementing control on a propulsion system versus a subscale emulated propulsion system. Figures (1a) and (1b) depict control strategies with and without physical turbomachinery, respectively.

When comparing EMs to turbomachinery, they differ in three main categories: power, angular inertia, and angular viscous friction. For the types of EMs used in subscale testbeds to emulate turbomachinery, the power level and angular inertia are much smaller than that of full-scale turbomachinery. Also, the relative effect of viscous friction on a subscale EM compared to a full-scale turbomachinery shaft is much more pronounced. The solution to the power discrepancy is to scale any full-scale commands or subscale feedback signals passed between the full-scale model and the subscale system. Scaling commands to and from the systems ensures that the subscale system operates within its operating range when being controlled by a full-scale model. The result of the angular inertia and viscous friction discrepancies is that the hardware shaft speed response to identical torque inputs on the EMs differs from the shaft software model. Dynamically speaking, they are not equivalent, and the solution is to use closed loop control to modify the dynamic response characteristics of the subscale hardware system so that its behavior is a subscale representation of the full-scale software model.

Using closed loop control and scaling to emulate dynamic subscale mechanical loads is not a novel control goal. Many documented applications in the literature have successfully used closed loop control and scaling to emulate the mechanical loads of turbomachinery components, windmill propellers, and various powertrain loads in automobiles to obtain a dynamic system response of desirable mechanical characteristics [3, 4, 5, 6, 7]. For mechanical systems, the desirable characteristics would be inertia, viscous friction, and spring constants. The novelty of the work presented in this paper is based on the control theory and scaling algorithm used, its application toward the emulation of turbomachinery loads, and the control robustness and performance it provides when applied to a non-linear system. This paper presents and derives a closed loop control and scaling algorithm, named the Sliding Mode Impedance Controller with Scaling (SMICS), that effectively reconciles the differences in power level, angular inertia, and viscous friction between a full-scale electrified turbomachinery shaft software model and a subscale hardware emulation system. This algorithm permits the creation of a subscale dynamic representation of an electrified turbomachinery shaft without turbomachinery that is controllable by a full-scale, hybrid-electric propulsion system model and its prospective control, which is shown in Fig. 1. The purpose of this paper is to prove that the closed loop control and scaling approach enables the testing of full scale electrified aircraft propulsion control systems in a HIL environment with subscale electromechanical power system hardware present and without turbomachinery or rotors present.

In this study, the algorithm is implemented on a megawatt-scale electromechanical power system at NEAT. The subscale, closed loop emulation system is driven by the propulsion system model of the Single-aisle Turboelectric Aircraft with Aft Boundary-Layer Propulsion (STARC-ABL) [8] and its advanced control algorithm [9, 10]. Section III explains the concept of impedance control toward the emulation of dynamic torque loads by comparing the current application to a common application in robotic arm interaction regulation. Section IV derives the closed loop control and scaling algorithm and comments on its drawbacks relating to stability. Section V explains the implementation of

the closed loop control and scaling algorithm towards emulating the dynamic subscale torque loads a single STARC-ABL turbofan on its and the tailfan rotor load. Section VI presents results comparing the performance of the controller and propulsion system model when the controller is on versus off. Section VII restates the scope of the paper and the major conclusions.

III. Concept

Impedance control is the fundamental control concept upon which the SMICS algorithm is based [11, 12, 13]. At the core of impedance control is an algorithm that establishes a relationship between the control input and the measured output of a system. Most commonly used in mechanical systems for regulating the interaction of a robotic arm with its environment [14, 15, 16, 17], impedance control develops a relationship between torque/force and angular/translational position and/or velocity. The relationship is defined in terms of mechanical characteristics (e.g., inertia, viscous friction, spring constant). When implementing impedance control for the regulation of robot arm interaction, the control designer can dictate how the robotic arm will react to an interaction with its environment by allocating mechanical characteristics to the controller that create a desired reaction. For example, the designer can decide that the robotic arm will behave like a spring by adding a desired spring constant to the controller. If the robotic arm were to be manipulated by a human hand and that interaction force was denoted as F_{int} , the robotic arm would behave like a spring with spring constant K , applying a linear force on the hand proportional to the displacement of the robotic arm. The human would feel as if they were pushing on a spring-instead of pushing on the robotic arm. Fig. 2 illustrates this concept and how it relates to the application in this paper. In this work, the same concept applies. For the application being described in this paper, the robotic arm is an EM representing the turbomachinery, the human hand or environment is the subscale electromechanical system to be attached to turbomachinery, and the spring constant is the subscale angular inertia, J_{ES} , of the subscale gas turbine engine shaft. The electromechanical system meant to be attached to turbomachinery, but instead, it is torquing an EM attached to a shaft with angular inertia, J_P .

The secondary aspect of control methodology in this algorithm is sliding mode control [18, 19, 20, 21]. Sliding mode control is a non-linear, discontinuous, variable structure control algorithm known for its robustness qualities. Sliding mode control uses a discontinuous switching algorithm, formally referred to as a ‘reaching law’, to force a sliding function to reach for and remain on a defined sliding surface. In sliding mode control theory, the sliding function is a cost function, and the sliding surface is the ideal value of that cost function. In a simple mechanical application, the sliding function could be a position or velocity error and the sliding surface is when the sliding function (i.e., error) is equal to zero. Sliding mode control is particularly useful in the algorithm outlined in this paper because it is inherently robust to plant parameter uncertainty. Impedance control requires that it use a parametric model of the plant under control in its algorithm. This leaves the controller susceptible to performance degradations introduced by plant parameter uncertainty. By integrating sliding mode control into the impedance control algorithm, the control designer can add controller robustness to plant parameter uncertainty, meaning the performance of the controller becomes less dependent on the knowledge of the plant parameters.

IV. Derivation

This section derives the closed loop control and scaling algorithm. Deriving the model-based controller begins with a mathematical description of the plant to be controlled. The dynamic mechanical equation of a coupled EM shaft is shown in Eq. (1). The torque of the EM, tasked with providing a subscale emulation of the turbomachinery, is denoted as T_M . This EM will be referred to as the emulation EM. The torque of the subscale EM, in the electromechanical system to be attached to turbomachinery, is denoted as T_{GS} . This EM will be referred to as the research EM. The

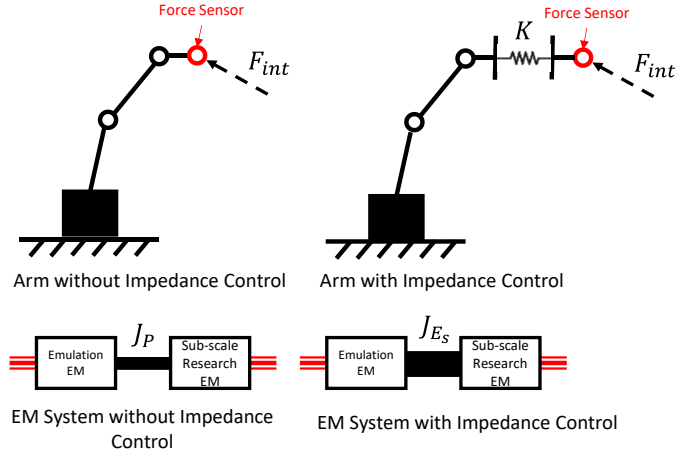


Fig. 2. Illustration of robotic arm and electric machine system with and without impedance control.

angular inertia, angular acceleration, angular viscous damping coefficient, and angular speed of the plant are denoted as J_P , $\dot{\omega}_P$, B_P , and ω_P , respectively, where $J_P > 0$, $B_P > 0$, and $\omega_P \geq 0$.

$$T_M + T_{G_S} - J_P \dot{\omega}_P - B_P \omega_P = 0 \quad (1)$$

The conceptual goal is to create a relationship between angular speed and torque that drives the emulation EM to respond to the research EM with a torque that forces the shaft to behave with the subscale, inertial characteristics of the gas turbine engine shaft. The mathematical description of the subscale electrified turbomachinery shaft to be emulated is represented by Eq. (2). This is the mathematical representation of the conceptual goal of the controller. The subscale net turbomachinery torque is denoted as T_{E_S} . The subscale angular inertia and angular acceleration of the electrified turbomachinery shaft are denoted as J_{E_S} and $\dot{\omega}_{E_S}$, respectively, where $J_{E_S} > 0$.

$$T_{E_S} + T_{G_S} - J_{E_S} \dot{\omega}_{E_S} = 0 \quad (2)$$

The simplest impedance controller that theoretically fulfills the control goal is obtained by assuming that $\dot{\omega}_P = \dot{\omega}_{E_S}$. Based on this assumption, Eqs. (1) and (2) are combined. The resulting equation is solved for T_M to yield the simple impedance controller shown in Eq. (3).

$$T_M = \frac{J_P}{J_{E_S}} [T_{E_S} + T_{G_S}] + B_P \omega_P - T_{G_S} \quad (3)$$

Equation (3) is a model-based controller relating position to force and has a tunable inertial term, J_{E_S} . The resulting closed loop equation is identical to the desired electrified turbomachinery shaft dynamics given in Eq. (2).

The previous derivation has shown that a closed loop system identical to the subscale electrified turbomachinery shaft can be derived. In order for the shaft software model to be replaceable by this closed loop hardware system, the closed loop system must be scaled to mathematically represent a full-scale electrified turbomachinery shaft. The addition of the following scalars in Eqs. (4), (5), (6), and (7) accomplishes this goal. The full-scale net turbomachinery torque, full-scale research EM torque, full-scale electrified turbomachinery shaft angular speed, speed scalar, and torque scalar are denoted as T_E , T_G , ω_E , α , and β , respectively, where $\omega_E \geq 0$, $\alpha > 0$, and $\beta > 0$. The scalars are defined by the control designer per the application and provide a means to ensure that the EM hardware operates within its allowable speed and torque range. The sum of the compressor and turbine torques applied to a common shaft is denoted as T_E , where the compressor torque is generally negative, and the turbine torque is generally positive.

$$T_{E_S} = \beta T_E \quad (4)$$

$$T_{G_S} = \beta T_G \quad (5)$$

$$\omega_{E_S} = \omega_P = \alpha \omega_E \quad (6)$$

$$\dot{\omega}_{E_S} = \dot{\omega}_P = \alpha \dot{\omega}_E \quad (7)$$

The corresponding scaling of J_{E_S} , must be found and is a function of α and β . To determine the scaling function for J_{E_S} , Eq. (2) was non-dimensionalized and scalars were introduced to determine the relationship between the scaling parameters. The result is Eq. (8). The full-scale angular inertia of the electrified turbomachinery shaft is denoted as J_E , is greater than zero, and is taken from the gas turbine engine shaft model to be emulated.

$$J_{E_S} = \frac{\beta}{\alpha} J_E \quad (8)$$

Substituting Eqs. (4), (5), and (8) into Eq. (3) yields the following simple impedance controller with scaling.

$$T_M = \frac{J_P}{\frac{\beta}{\alpha} J_E} [\beta T_E + \beta T_G] + B_P \omega_P - \beta T_G \quad (9)$$

Substituting Eqs. (9), (5), and (7) into Eq. (1) yields the full-scale closed loop system shown in Eq. (10).

$$\beta T_E + \beta T_G - \beta J_E \dot{\omega}_E = 0 \quad (10)$$

When under the simple impedance controller with scaling, the closed loop hardware system is now a potentially suitable replacement for the full-scale software model.

Model-based controller performance is directly related to how well the designer knows the true plant parameters. In practice, error exists between the plant parameters assumed by the control designer and the true plant parameters.

The result of this discrepancy is reduced controller performance. Sliding mode control is integrated into the impedance controller to compensate for the uncertainty that exists between the estimated plant parameters used in the controller and the true parameters of the plant. The sliding function, s , which is the goal of the sliding mode controller, is the simplified integral of Eq. (2) and is shown in Eq. (11).

$$s = \int [T_{Es} + T_{Gs}] dt - J_{Es} \omega_p = 0 \quad (11)$$

Using Eq. (11) as the sliding function has the following benefits:

1. Taking the derivative of the inertial portion of Eq. (11) results in an easily measurable state, ω_p , with a low amount of noise as the feedback variable.
2. The control designer can set the initial condition of the sliding function. The sliding function starts from the sliding surface if $\dot{\omega}_p = 0$, the result is the elimination of the reaching phase.
3. Robustness is promised [22].

Using an integral sliding function as shown in Eq. (11) is known as integral sliding mode control [23]. A drawback of the simplification of the integral is the steady state error in the sliding function that results from the inertial portion lying outside of the integral. The result is a sliding function that will only reach for the sliding surface, $s = 0$, when $T_{Es} + T_{Gs}$ is nonzero (the implemented propulsion system model is accelerating or decelerating).

A reaching law, $\dot{s}$, drives the sliding function to the sliding surface. When on the sliding surface, the sliding model controller is completely compensating for the plant parameter uncertainty; the desired subscale electrified turbomachinery shaft closed loop equation shown in Eq. (10) is achieved. The reaching law chosen for this application is shown in Eq. (12). It has two driving functions, $-\eta \text{sat}\left(\frac{s}{\phi}\right)$, which is a general discontinuous reaching function, and a proportional-integral-derivative (PID) action. The linear reaching gain is denoted as η , where $\eta > 0$. The saturation function with upper and lower bounds of 1 and -1, respectively, is denoted as $\text{sat}(\cdot)$. The switching gain is denoted as ϕ , where $\phi > 0$. This gain scales the sliding function to reduce high frequency switching between the saturation bounds. The proportional, integral, and derivative gains are denoted as K_p , K_i , and K_d , respectively.

$$\dot{s} = -\eta \text{sat}\left(\frac{s}{\phi}\right) - K_p s - K_i \int [s] dt - K_d \dot{s} \quad (12)$$

The reaching function retains the non-linear, variable structure of the controller inherent to sliding mode control theory. $-\eta \text{sat}\left(\frac{s}{\phi}\right)$ forces the sliding function to linearly reach toward the sliding surface. The proportional action forces the sliding function to exponentially reach for the sliding surface, resulting in a quicker convergence to the sliding surface during an excursion away from it. The integral term eliminates steady state error that exists due to ω_p existing outside of the integral in Eq. (11). The derivative action dampens oscillations that exist if the plant is naturally underdamped. Taking the derivative of Eq. (11), setting Eq. (11) equal to Eq. (12) via $\dot{s}$ terms, and solving for $\dot{\omega}_p$ yields Eq. (13).

$$\dot{\omega}_p = \frac{1}{J_{Es}} \left[T_{Es} + T_{Gs} + \eta \text{sat}\left(\frac{s}{\phi}\right) + K_p s + K_i \int [s] dt + K_d \dot{s} \right] \quad (13)$$

Substituting Eq. (13) into Eq. (1) and solving for T_M yields Eq. (14), the sliding mode impedance controller (SMIC).

$$T_M = \frac{J_p}{J_{Es}} \left[T_{Es} + T_{Gs} + \eta \text{sat}\left(\frac{s}{\phi}\right) + K_p s + K_i \int [s] dt + K_d \dot{s} \right] + B_p \omega_p - T_{Gs} \quad (14)$$

Substituting Eqs. (4), (5), and (8) into Eq. (14) yields the sliding mode impedance controller with scaling (SMICS).

$$T_M = \frac{J_p}{\beta J_E} \left[\beta T_E + \beta T_G + \eta \text{sat}\left(\frac{s}{\phi}\right) + K_p s + K_i \int [s] dt + K_d \dot{s} \right] + B_p \omega_p - \beta T_G \quad (15)$$

Substituting Eqs. (15) and (7) into Eq. (1) yields the closed loop equation shown in Eq. (16).

$$\beta T_E + \beta T_G - \beta J_E \dot{\omega}_E + \eta \text{sat}\left(\frac{s}{\phi}\right) + K_p s + K_i \int [s] dt + K_d \dot{s} = 0 \quad (16)$$

If the reaching law and its gains are tuned to sufficiently compensate for the plant parameter uncertainty, s and $\dot{s}$ will remain at or near zero and the equation will reduce to Eq. (10).

With all control designs, proving stability is critical to the controller design process. Unfortunately, asymptotic stability is not provable in this work. The authors are unaware of a function that proves asymptotic stability. A general

Lyapunov function used in sliding mode control theory is used in this work to show this drawback. The function is shown in Eq. (17).

$$V(s) = \frac{1}{2}s^2 \quad (17)$$

It can be shown that by taking the derivative of Eq. (17) to obtain the Lyapunov derivative in Eq. (18) and then substituting Eq. (12) into Eq. (18) to yield Eq. (19), that the Lyapunov derivative function is not always negative. $\dot{V}(s) < 0$ when $s \neq 0$ is a requirement for proving asymptotic stability.

$$\dot{V}(s) = s\dot{s} \quad (18)$$

$$\dot{V}(s) = s \left[-\eta \text{sat} \left(\frac{s}{\phi} \right) - K_p s - K_I \int [s] dt - K_D \dot{s} \right] \quad (19)$$

If $K_I \int [s] dt$ and/or $K_D \dot{s}$ has the opposite sign of s , then Eq. (19) has the possibility to be positive. Unguaranteed asymptotic stability is one of the drawbacks of this controller.

V. Implementation

The SMICS was implemented on an electromechanical system at the NASA Electric Aircraft Testbed (NEAT) at the NASA Neil A. Armstrong Test Facility in Sandusky, Ohio. NEAT is a reconfigurable, megawatt-scale electromechanical system and vacuum chamber designed to test EAP electromechanical components and systems at altitude [2]. For this work, the NEAT electromechanical system was configured to represent the STARC-ABL electromechanical system [24]. The STARC-ABL, shown in Fig. 3, is a partially turboelectric aircraft concept developed to optimize vehicle performance using new aerodynamic designs and hybrid-electric propulsion technologies. It consists of two wing-mounted gas turbine engines (GTEs) and a boundary layer ingesting, EM-driven, ducted fan mounted at the tail. This electric propulsor subsystem is denoted as the tailfan (TFAN). EMs are mechanically coupled to the GTE low pressure spools (LPSs) and continuously extract power from them. That power is then supplied to the EM of the

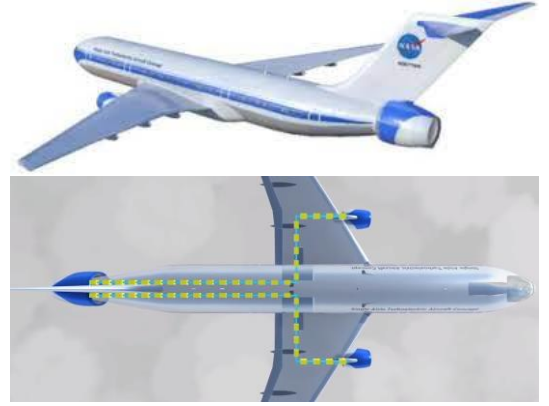


Fig. 3. STARC-ABL concept vehicle.

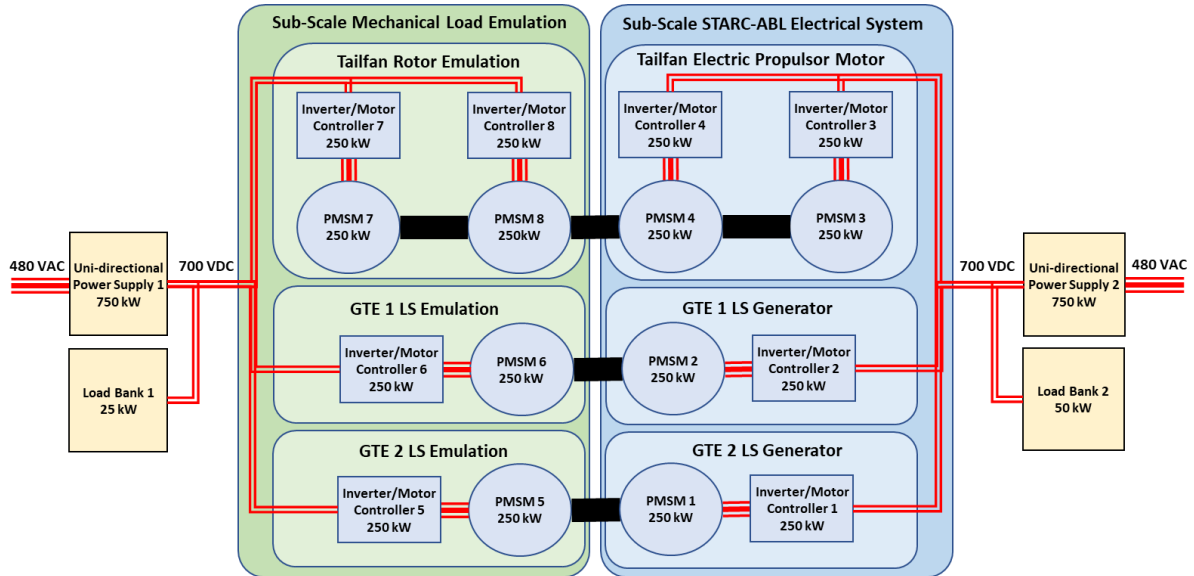


Fig. 4. NEAT STARC-ABL electromechanical system.

TFAN via two independent, identical power system electrical buses. This architecture is represented using existing NEAT electrical system components and is shown in Fig. 4. In the concept, the buses are independent for redundancy. However, for this test, the electrical buses are electrically coupled. Redundant buses are not necessary to test the STARC-ABL control algorithm. The electromechanical system is divided into two main groups: torque load emulation and STARC-ABL electrical system. In this application the EMs are permanent magnet synchronous machines (PMSMs). The EMs shown in the green section of Fig. 4, use closed loop and scaling to emulate the subscale inertial load of each GTE LPS and TFAN shaft. The EMs provide a subscale dynamic mechanical resistance that is representative of the resistance the EMs, in the light blue-shaded area of Fig. 4, would experience if they were attached to a subscale version of the GTE LPSs and the TFAN rotor. Each grouping has a common bus connected to a unidirectional power supply and a load bank. The purpose of this unidirectional power supply and load bank system is to sink or source power if necessary. The TFAN uses two pairs of EMs to emulate the subscale rotor torque load and represent the electric propulsor motor system. The use of more than one EM for a single purpose is to increase the power level of that system. An ideal electromechanical system would consist of components sized so that scaling is not necessary.

The STARC-ABL model and SMICS are implemented in the MATLAB®/Simulink® environment and run in real-time at a time step of 15 ms. The STARC-ABL model uses the Toolbox for the Modeling and Analysis of Thermodynamic Systems (T-MATS) [25] to model the turbomachinery of each GTE and the boundary layer ingesting ducted rotor of the TFAN. The NEAT electromechanical system components, shown in the dark blue shaded area of Fig. 4, are the subscale hardware representation of the STARC-ABL electrical power system. The components in the dark green shaded area replace the subscale turbomachinery. The EMs in this section are driven by the SMICS algorithm to emulate the subscale inertial load of the respective system. These closed loop systems, as well as the EMs in the large, blue, shaded area are commanded by the STARC-ABL propulsion system model. A dSPACE® SCALEXIO® real-time computing system is used to compile and run the model and control in real-time. The real-time computer commands and receives feedback from each inverter/motor controller via a controller area network (CAN) bus. A graphical user interface (GUI) developed in the dSPACE® ControlDesk® environment is the interface between the compiled model and control running on the real-time computer and the user. The GUI allows a user to command the STARC-ABL model, which through the SMICS, commands the NEAT electromechanical system. Data from the model, control, and physical electromechanical components are recorded at 15 ms intervals.

Prior to testing at NEAT, the STARC-ABL model and the closed loop control and scaling algorithm were verified using a model of the NEAT facility to ensure controller stability, model convergence, and system operation within system limits. The facility model was constructed using the Electrical Modeling and Thermal Analysis Toolbox (EMTAT) power flow library in the MATLAB®/Simulink® environment [26]. The results of this simulation verified the operation of the propulsion system model, closed loop control, and scaling algorithm and provided predictions of the model, control, and NEAT electromechanical system operation and performance.

This application uses the scalars listed in Table 1. The EMs operational capabilities constrain the achievable power level and scaled

Table 1. Scalars and inertias.

	GTEs 1 & 2	TFAN
Torque Scalar, β	0.2	0.05625
Speed Scalar, α	0.45	1.6
Power Scalar, $\alpha\beta$	0.09	0.09
Full-Scale Inertia, J_E, lbf-ft-s²	6.1875	23.9805
Inertia Scalar, β/α	0.4444	0.0352
Subscale Inertia, J_{Es}, lbf-ft-s²	2.7497	0.8441

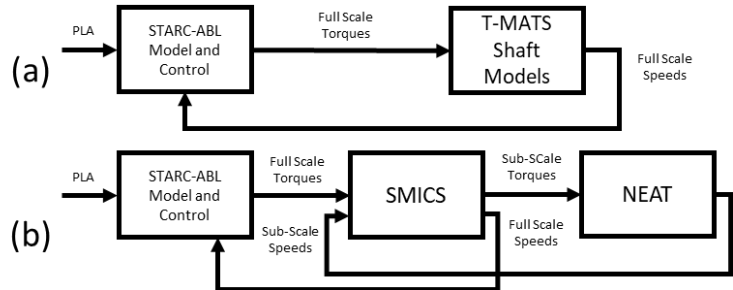


Fig. 5. Visual representation of compared control loops. (a) SIL – Theoretical. (b) HIL – Actual.

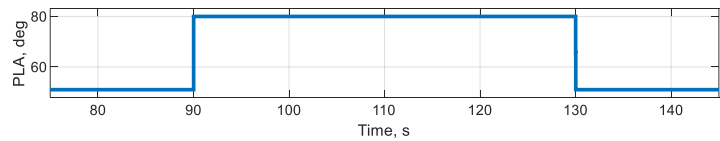


Fig. 6. Power lever angle (PLA) profile.

(i.e., emulated) inertia of each EM system. Each EM system operates at 9% of their respective system's full power level, 44.44% of the GTE 1 & 2 LPS full scale inertias, and 3.52% of the TFAN inertia. The estimated inertias of the coupled EMs representing GTEs 1 & 2 and the TFAN are 0.0963 lbf-ft-s² and 0.2139 lbf-ft-s², respectively. The scaled (emulated) inertias of GTEs 1 & 2 and TFAN are ~28.55 and ~3.94 times larger than the inertias of the representative EM systems.

VI. Results

Three sets of results present the operation of the STARC-ABL model at sea level static conditions under different simulation/test setups. The control loops describing the circumstances are shown in Fig. 5. Control loop 5(a) shows the STARC-ABL propulsion system driving a full-scale model of the GTE LPS shafts and TFAN shaft. The results from the propulsion system are considered the theoretical results. In all subsequent plots, the results are denoted "SIL", or "software-in-the-loop". Control loop 5(b) shows the STARC-ABL model driving the SMICS, which is driving the physical NEAT electromechanical system. These results are the test results to be compared against the theoretical results produced by the SIL. In all subsequent plots, the results are denoted "HIL", or "hardware-in-the-loop". For control loop 5(b), tests were run with the SMIC both off and on while retaining the scaling algorithm. For all tests, the STARC-ABL model uses the power lever angle (PLA) burst and chop profile shown in Fig. 6. A PLA of 51 represents idle and 80 represents full power. In the plots to be presented, only the GTE1 and TFAN results are shown. GTE2 results are omitted as the operation of GTE1 and GTE2 are considered to be identical for this study. The controller gains for the GTEs and the TFAN are shown in Table 3.

Fig. 7 shows, from top to bottom, the propulsion system model GTE1 fan, LPS, and high-pressure spool (HPS) shaft speeds and the TFAN shaft speed during the burst and chop segments of the test. These engine states are of particular interest when determining the control accuracy. If the STARC-ABL model shaft responses are similar during both SIL and HIL – SMIC On test, it can be concluded that a coupled EM system under control of the SMICS is a sufficient replacement, dynamically speaking, for testing the performance of full-scale hybrid-electric propulsion control systems. Qualitatively, it is evident from Fig. 7 that there is a differing response with and without the SMIC. When the SMIC is on, the shaft speed responses

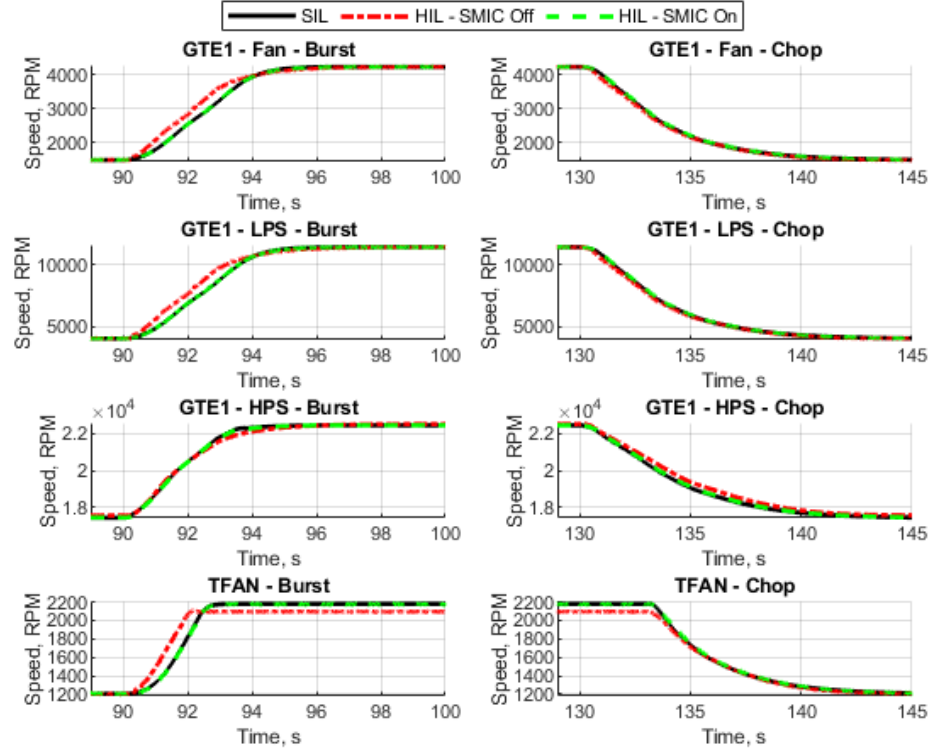


Fig. 7. STARC-ABL model shaft speeds.

Table 3. Controller gains.

	GTEs 1 & 2	TFAN
η	20	20
ϕ	0.1	0.1
K_P	20	20
K_I	20	20
K_D	0	0

Table 2. Propulsion system model speed RMSRE percentages.

	SMIC Off		SMIC On	
	Burst	Chop	Burst	Chop
GTE1 - Fan	3.961	0.878	0.177	0.506
GTE1 - LPS	3.961	0.878	0.177	0.506
GTE1 - HPS	0.527	0.973	0.114	0.194
TFAN	4.905	2.120	0.329	0.500

Table 4. Propulsion system model torque RMSRE percentages.

	SMIC Off		SMIC On	
	Burst	Chop	Burst	Chop
GTE1 - LPS Engine	21.857	65.603	1.095	3.006
GTE1 - LPS - EM	7.012	7.777	0.970	1.190
TFAN - EM	7.088	7.769	0.886	1.151
TFAN - Rotor	8.997	4.255	0.847	1.031

accurately trace the theoretical response from the SIL case. Table 2 quantitatively assesses the responses by showing the root-mean-squared relative error (RMSRE) of the HIL – SMIC Off and HIL – SMIC On cases to the SIL case. The error is relative to the maximum value of the SIL signal. For all speed states, the RMSRE is less than or nearly equal to 0.5% with SMIC On and up to 4.9% with the SMIC off.

Fig. 8 shows, from top to bottom, the propulsion system model net LPS GTE1 turbomachinery torque, the LPS GTE1 EM torque, the TFAN electric propulsor motor torque, and the TFAN rotor torque. The net LPS GTE1 turbomachinery torque is a summation of the fan, low-pressure compressor, and turbine torques. These signals are shown to illustrate the differences in model operation and hybrid-electric propulsion system control actuation with and without the SMIC. The significant variation in torque application is evident qualitatively in Fig. 8 and quantitatively in Table 4. For all torque signals, the RMSRE was less than or nearly equal to 3% with SMIC On and as high as 65.6% with the SMIC off.

Fig. 9 and Fig. 10 show key performance variables for the GTE1 and TFAN subsystems of the propulsion system model, respectively. Fig. 9, from top to bottom, shows the low- and high-pressure compressor stall margins, the total temperature aft of the burner (Station 4), and the static pressure aft of the high-pressure compressor (Station 36). Fig. 10, from top to bottom, shows the fan (rotor) stall margin and the total pressure aft of the fan (rotor). Table 5 quantifies these results. These variables are of interest because they not only show propulsion subsystem performance and operability but are also often used in the controller design process to inform the design of subsystem limits. Much of the same variation exists when the SMIC is on versus off, with a significant increase in accuracy when on.

Fig. 11 shows the sliding function, s , during an entire test of the SMICS controller regulating the EM systems that represent the

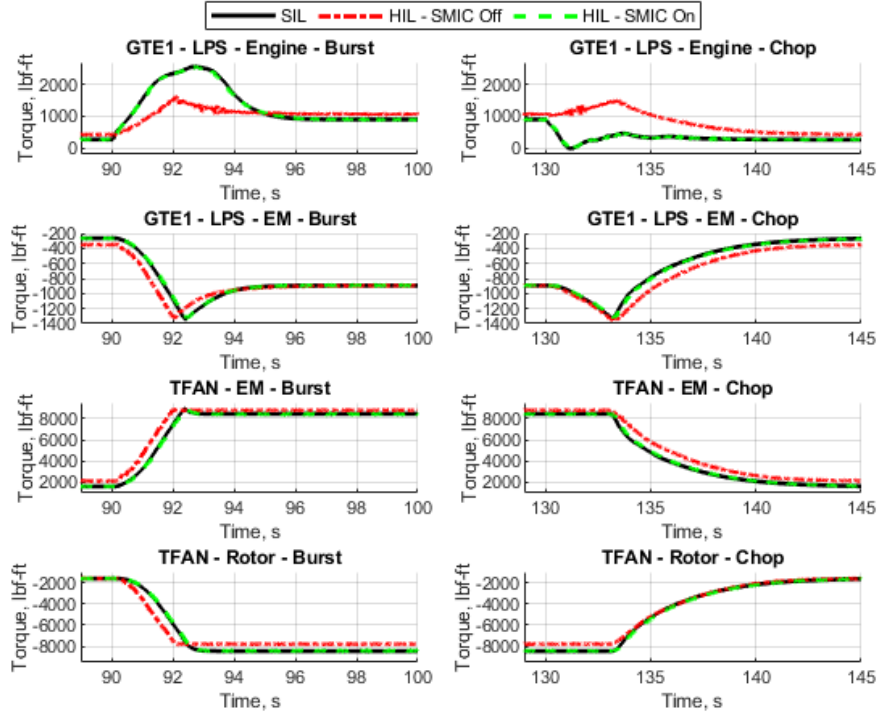


Fig. 8. STARC-ABL model shaft torques.

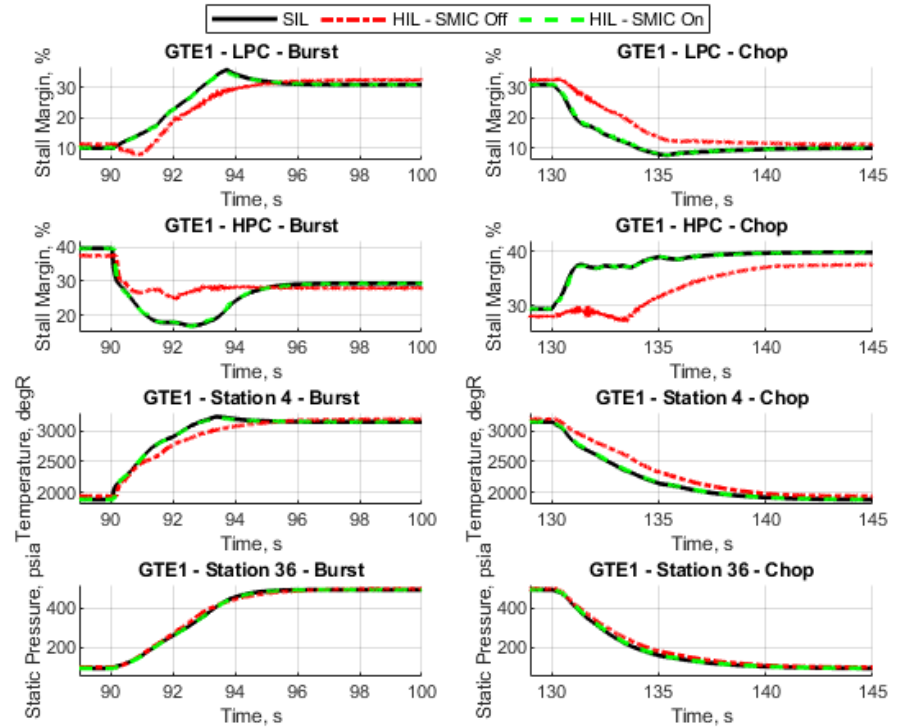


Fig. 9. GTE1 model performance variables.

GTE1 LPS and TFAN. When $s = 0$, the response prescribed by the user-defined, subscale angular inertia is being realized. On excursions away from zero, the closed loop system response differs from the intended response. The sliding function remains at zero during steady state conditions, which means that any inherent EM system viscous friction is eliminated. During transients, the GTE1 LPS sliding function moves away from $s = 0$, but it is not enough to produce a dynamical system

that significantly degrades the propulsion system model operation. Plant parameter uncertainty as well as gain tuning also play a role in the magnitude of the sliding function excursions from $s = 0$. Since the SMICS is a model-based controller, the larger the differences are between the true and estimated plant parameters, the more the sliding mode controller will have to work to compensate for that uncertainty. Increasing the compensation increases the magnitude of sliding function excursions and decreases performance. This is hypothesized to be the problem that explains the larger excursion of the GTE1 LPS sliding function. Future updates to the algorithm will be explored to address this shortcoming.

Up until this point, the controller performance has been compared in the time domain. A further verification that the desired dynamics are being achieved is to compare controller performance in the frequency domain. To accomplish this, a transfer function of the form shown in Eq. (20) must be found for both GTE1 and TFAN in the SIL, HIL – SMIC Off, and HIL – SMIC On cases. Equation (20) is the transfer function form of the desired closed loop equation shown in Eq. (10). The net shaft torque, shaft speed, frequency domain variable, angular inertia, and angular viscous damping coefficient are denoted as T_{net} , ω , s , J , and B , where $T_{net} = T_E + T_G$ and $\omega = \omega_E$.

$$\frac{T_{net}(s)}{\omega(s)} = \frac{1}{Js + B} \quad (20)$$

In the SIL case, $J = J_E$ and $B = 0$. In the HIL – SMIC Off and HIL – SMIC On cases, J and B , are empirically determined by performing an optimization using the MATLAB[®] *fmincon* function that fits the speed response of Eq. (20) to the test data speed response by solving for J and B . For GTE1 with SMIC Off, $J = 0.3128$ lbf-ft-s² and $B = 0.1532$ lbf-ft-s, with a RMSRE of 4.8501% between the transfer function and test data speed responses. For the TFAN with SMIC Off, $J = 8.5689$ lbf-ft-s² and $B = 4.3267$ lbf-ft-s, with a RMSRE of 4.5596%. For GTE1 with SMIC On case, $J = 6.2145$ lbf-ft-s² and $B = 1.2543 \cdot 10^{-7}$ lbf-ft-s, with a RMSRE of 5.6765%. For the TFAN with SMIC On, $J = 24.0233$ lbf-ft-s² and $B = 1.4492 \cdot 10^{-7}$ lbf-ft-s, with a RMSRE of 5.9634%. By comparing the J

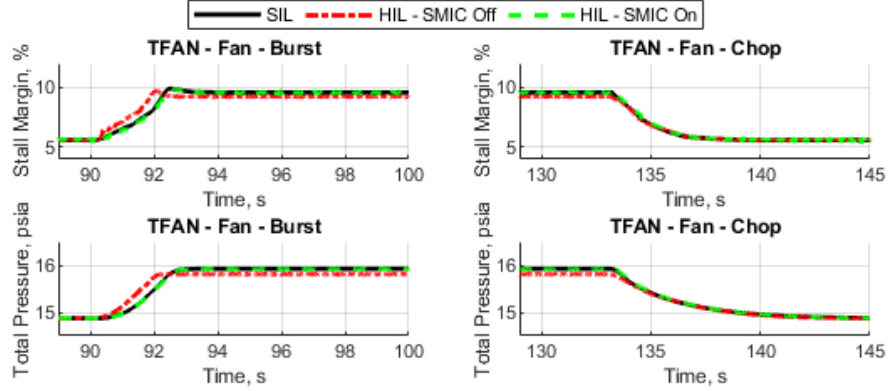


Fig. 10. Tailfan propulsor model performance variables.

Table 5. Propulsion system model performance variable RMSRE percentages.

Signals	SMIC Off		SMIC On	
	Burst	Chop	Burst	Chop
GTE1 - LPC – Stall Margin	9.267	15.298	0.574	1.134
GTE1 - HPC - Stall Margin	12.028	13.384	1.359	0.484
GTE1 - Station 4 – Total Temp.	3.108	4.306	0.454	0.382
GTE1 - Station 36 – Static Pres.	1.733	3.079	0.313	0.786
TFAN - Fan - Stall Margin	4.847	2.070	1.003	1.116
TFAN - Fan - Total Pres.	0.774	0.382	0.067	0.084

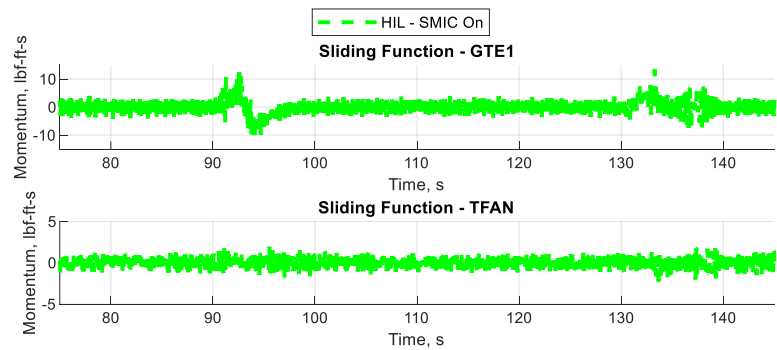


Fig. 11. SMICS sliding function, s .

and B terms shown in Table 1 (B term is zero) to the empirically determined values for the HIL – SMIC On case, it is evident that the values are very similar to the SIL case. For HIL – SMIC Off, the J and B terms differ greatly and are likely the characteristics of the open loop EM system. These results indicate the controller is realizing the desired closed loop equation shown in Eq. (10). To show this graphically, the Bode plots for each transfer function are shown in Fig. 12. The HIL – SMIC On case lies nearly on top of the SIL case, and the HIL – SMIC Off case varies significantly throughout the frequency range.

VII. Conclusion

The SMICS, a closed loop control and scaling algorithm designed to drive an EM to emulate dynamic subscale torque loads, was conceptualized, derived, implemented, and validated. This closed loop control and scaling algorithm, along with a turbomachinery model, can replace turbomachinery hardware in an electrified propulsion system during the initial HIL testing of advanced hybrid-electric propulsion system controllers. The closed loop control and scaling algorithm successfully replicated to a high degree of accuracy the subscale mechanical dynamics of the STARC-ABL GTE and TFAN shafts. This algorithm allows for full-scale hybrid-electric propulsion system controllers to be tested in a low cost, low risk, subscale electrified aircraft testbed without the need for turbomachinery. There are a few shortcomings in this controller that prevented guaranteed asymptotic stability and contributed to performance degradations due to inexact knowledge of the parameters of the system under control (plant parameter uncertainty). It is hypothesized that these shortcomings can be addressed by incorporating adaptive control to automatically tune plant parametric parameters that exist in the controller. The addition of adaptive control has the potential to reduce the effects of plant parameter uncertainty, guarantee asymptotic stability, and improve controller performance.

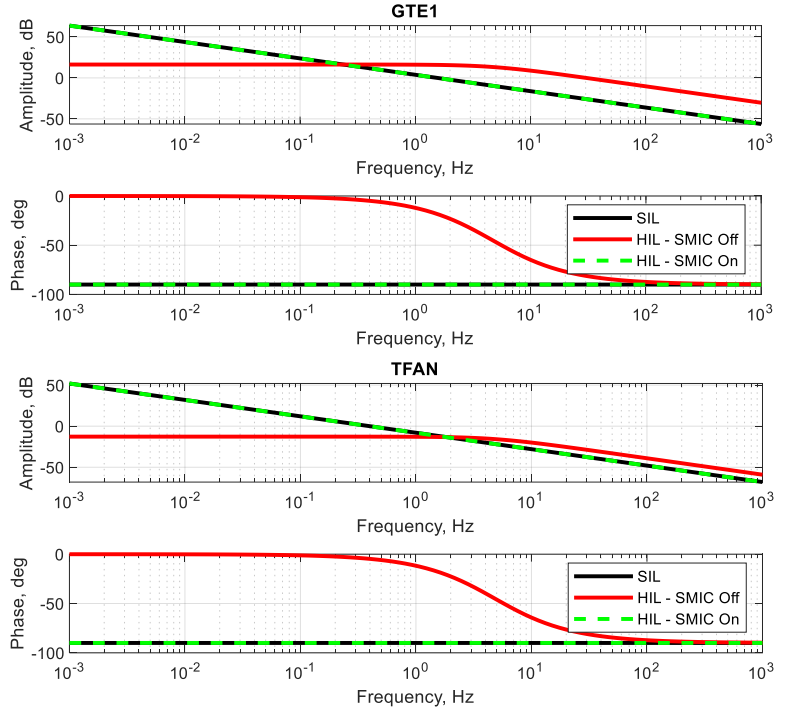


Fig. 12. GTE1 and TFAN Bode plots.

Acknowledgments

The authors acknowledge the NASA Advanced Air Transport Technology (AATT), Hybrid Thermally Efficient Core (HyTEC), and the Transformational Tools and Technologies (TTT) projects for funding this work. The authors also acknowledge Marcus A. Horning, Jonah J. Sachs-Wetstone, Joseph R. Saus, Joseph W. Connolly, Amy L. Jankovsky, Jonathan S. Litt, Joseph M. Haglage, Michael D. Becks, Kevin M. Ryley, William A. Cummings, Mark L. Worley, Ying C. Cha, Joshua Archibald, Michael E. Smith, Richard, Plastow and Andrew Taylor for their contributions to the testing of this controller.

References

- [1] Buescher, H. E. et al, “Hybrid-electric Gas Turbine Engine Mechanical Load Emulation Testbed with Energy Storage: Overview and Capability,” *AIAA Science and Technology Forum*, San Diego, CA, 2023.
- [2] Haglage, J. M., Brown, T. W., “NASA Electric Aircraft Testbed (NEAT) Reconfiguration to Enable Altitude Testing of Megawatt-Scale Electric Machines,” *AIAA Propulsion and Energy Forum*, New Orleans, LA, 2020.
- [3] Kyslan, K., Durovsky, F., “Dynamic Emulation of Mechanical Loads—An Approach Based on Industrial Drives’ Features,” *Automatika Journal for Control, Measurement, Electronics, Computing, and Communications*, Vol. 54, No. 3, 2013, pp. 356-363.

-
- [4] Fajri, P., Ahmadi, R., Ferdowsi, M., "Control Approach Based on Equivalent Vehicle Angular Inertia Suitable for Motor-Dynamometer Test Bench Emulation of Electric Vehicles," *IEEE International Electric Machines & Drives Conference*, Chicago, IL, 2013.
 - [5] Oliveira, C. M. R., Aguiar, M. L., Castro, A. G., Guazzelli, P. R. U., Pereira, W. C. A., Monteiro J. R. B. A., "High-Accuracy Dynamic Load Emulation Method for Electrical Drives," *IEEE Transactions on Industrial Electronics*, Vol. 67, No. 9, 2020.
 - [6] Lee, K., Lee, J., Back, J., Lee, Y. I., "A Robust Emulation of Mechanical Loads Using a Disturbance-Observer," *Energies*, Vol. 12, No. 12, 2019.
 - [7] Correa, B.A., "Emulation of An Aeroderivative Twin-Shaft Gas Turbine Engine Using an AC Electric Motor Drive," Ph.D. Dissertation, College of Engineering and Computing, Univ. of South Carolina, Columbia, SC, 2013, Scholar Commons.
 - [8] Jansen, R. H., Bowman, C., Jankovsky, A., Dyson, R., Felder, J., Overview of NASA Electrified Aircraft Propulsion Research for Large Subsonic Transports," *AIAA Propulsion and Energy Forum*, Atlanta, GA, 2017.
 - [9] Kratz, J. L., Thomas, G. L., "Dynamic Analysis of the STARC-ABL Propulsion System," *AIAA Propulsion and Energy Forum*, Indianapolis, IN, 2019.
 - [10] Simon, D. L., Bianco, S. J., Horning, M. A., "Integrated Control Design for a Partially Turboelectric Aircraft Propulsion System," *ASME Turbomachinery Technical Conference and Exposition*, Boston, MA, 2023, submitted for publication.
 - [11] Hogan, N., "Impedance Control: An approach to Manipulation: Part I–Theory," *ASME Journal of Dynamic Systems, Measurement, and Control*," Vol. 107, No. 1, 1985, pp. 1-7.
 - [12] Hogan, N., "Impedance Control: An approach to Manipulation: Part II–Implementation," *ASME Journal of Dynamic Systems, Measurement, and Control*," Vol. 107, No. 1, 1985, pp. 8-16.
 - [13] Hogan, N., "Impedance Control: An approach to Manipulation: Part III–Applications," *ASME Journal of Dynamic Systems, Measurement, and Control*," Vol. 107, No. 1, 1985, pp. 17-24.
 - [14] Hogan, N., "Stable Execution of Contact Tasks using Impedance Control," *IEEE International Conference on Robotics and Automation*, Raleigh, NC, 1987, pp. 1047-1054.
 - [15] Hogan, N., "Impedance Control of Industrial Robots," *Robotic and Computer-Integrated Manufacturing*, Vol. 1, No. 1, 1984, pp. 97-113.
 - [16] Song, P., Yu, Y., Zhang, X., "A Tutorial Survey and Comparison of Impedance Control on Robotic Manipulation," *Robotica*, Vol. 37, No. 5, 2019, pp. 801-836.
 - [17] Ikeura, R., Moriguchi, T., Mizutani, K., "Optimal Variable Impedance Control for a Robot and its Application to Lifting an Object with a Human," *IEEE International Workshop on Robot and Human Interactive Communication*, Berlin, Germany, 2022.
 - [18] Itkis, U., "Control Systems of Variable Structure," Wiley, New York, 1976.
 - [19] Utkin, V., "Variable Structure Systems with Sliding Modes," *IEEE Transactions on Automatic Control*, Vol. 22, No. 2, 1977, pp. 212-222.
 - [20] Utkin, V., "Sliding Modes in Control and Optimization," *Springer-Berlin*, Berlin, 1992.
 - [21] Edwards, C., Spurgeon, S., "Sliding Mode Control: Theory and Applications," 1st ed, *Taylor and Francis*, London, 1998.
 - [22] Niu, Y., Ho, D. W. C. Lam, J., "Robust integral sliding mode control for uncertain stochastic systems with time-varying delay," *Automatica*. Vol. 41, No. 5, 2005, pp. 873–880.
 - [23] Utkin V., Shi, J., "Integral Sliding Mode in Systems Operating under Uncertainty Conditions," *Proceedings of 35th IEEE Conference on Decision and Control*, Kobe, Japan, 1996.
 - [24] Welstead, J. R., Felder, J. L., "Conceptual Design of a Single-Aisle Turboelectric Commercial Transport with Fuselage Boundary Layer Ingestion," *AIAA Science and Technology Forum*, San Diego, CA, 2016.
 - [25] Chapman, J. W., Lavelle, T. M., May, R. D., Litt, J. S., Guo, T. H., "Toolbox for the Modeling and Analysis of Thermodynamic Systems (T-MATS) User's Guide," *NASA Technical Reports Server*, NASA/TM-2014216638, 2014.
 - [26] Bell, M. E., Litt, J. S, "Electrical Modeling and Thermal Analysis Toolbox (EMTAT) User's Guide," *NASA Technical Reports Server*, NASA/TM-20205008125, 2020.