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Article

Power and Thermal Management of a Series Hybrid-Electric Aircraft Propulsion System

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Abstract

With the rise of aviation standards and the need to increase reliability and improve aircraft performance, various strategies have been considered to reduce fuel consumption, lower the emission, and more integrated control to improve the system response, especially in emergency situations. One of the most important of these has been the electrification of the aircraft propulsion system. The most important challenges in the development of these systems include: the need to use equipment with high levels of power, voltage and frequency while simultaneously lowering the total weight of the propulsion module, and the associated thermal challenges. Accordingly, the present study has attempted to examine the performance of these systems by simultaneously considering power and heat flow in a series hybrid propulsion system. After presenting a mathematical model of the system components, low-level and high-level control systems are presented. Finally, flight data from an aircraft in the general aviation class is used to examine the performance of the closed-loop model. It is shown that the proposed model can satisfy the power and thermal requirements of a hybrid propulsion system and results in a 17% fuel saving.

Keywords: hybrid-electric aircraft; series configuration; power and thermal management; low-level control; supervisory control

1. Introduction

Conventional aviation engines face low thermal efficiency. In addition, their speed of response to changes, especially in emergency conditions, is not as desirable. By using batteries and electric motors, these drawbacks can be hoped to be solved. The presence of additional resources for power generation (generator and battery) increases the flexibility of the propulsion system. By adjusting the operating point of the system and dividing the work between the engine and sources of electric power generation, the overall efficiency of the system can be increased.

What distinguishes a hybrid-electric aircraft propulsion system (HEAPS) is: 1) equipment with high power density, frequency and voltage, 2) weight restrictions and 3) greater thermal challenges.

High-power and high-frequency equipment. It is usually necessary to use multi-level converters for providing high electric power. These converters also provide the possibility of achieving higher frequencies and voltages but the high switching frequency often corresponds to a high stray inductance in the power loop which can cause voltage spikes during switching. To reduce this high loop inductance, the switching frequency must be reduced below the nominal value which will increase switching losses [1,2]. Apart from the above points, the high power of machines and other equipment requires the voltage of the generation and distribution network to be as high as possible. However, it should be noted that high voltage means the need for better insulation which increases the weight of the insulators as well as the need for sophisticated power electronics. In addition, according to the Paschen law, with decreasing pressure (increasing height), the insulation breakdown voltage decreases, which can be problematic given the high voltage level of the network [3–6].

Weight constraints. In a hybrid-electric propulsion system, the being low of the weight means high values of specific power (SP) and specific energy (SE). Superconducting machines have

efficiencies close to unity and very high values of SP and SE [7]. However, due to the lack of suitable cooling technology, such machines are currently not commercial yet [8,9]. Typically, every 10 °C increase in operating temperature halves the machine's life [10]. Similarly, due to the lack of batteries with suitable power and energy densities (in the range of 500–750 Wh/kg), it is necessary to use a significant number of current batteries to maintain aircraft's range, which greatly increases the weight of a hybrid-electric aircraft compared to a non-hybrid configuration [11].

Thermal challenges. Powering the cooling equipment in hybrid propulsion systems is not as easy as in internal combustion engines due to the addition of electrical components [12]. In addition, these components must be equipped with a fast thermal management system to issue the appropriate command without delay due to their high temperature sensitivity, much lower maximum allowable temperature than non-electrical systems, and limited operating temperature range [13]. Inadequate heat dissipation results in high winding losses. The excess temperature also reduces the amount of remanence and the coercivity of the machine magnets, which results in demagnetization and a reduction in machine power and torque [10,14].

1.1. The Contribution of This Paper

This paper is focused on the integration of power and thermal management systems in hybrid propulsion systems. The integration of EMS and TMS is of high importance, especially in aircrafts, because it can significantly reduce weight and consequently increase the operational capabilities of the aircraft. Accordingly, the specific objective of this research is: "Development of a simulator of a HEAPS with simultaneous consideration of power and thermal flows, investigation of the performance of the digital model, development of the control system, and finally, model-in-the-loop (MIL) testing." Specifically, the tasks considered in this paper are:

1. Development of a simulator of a HEAPS with series configuration
2. Development of high-level and low-level control systems for HEAPSs
3. Investigation of the potential of HEAPS in general aviation sector
4. Integration of thermal performance of key components of HEAPSs.

By understanding the dynamic behavior of the HEAPS, effective measures can be taken in the early design stages.

In this regard, first, in Section 2, a review of the work done in this field is given. Then, the structure of the propulsion system along with the mathematical model of the components are provided in Section 3, and the control system design is examined in Section 4. In Section 5, the developed model is used in simulating the performance of a 2-seater general aviation aircraft that uses a series hybrid-electric propulsion system.

2. Literature Review

In recent years, as the benefits of coordinating and integrating control subsystems such as EMS, TMS, and ECS have become apparent, and due to the need for propulsion systems with a higher level of reliability, integrated subsystem management in hybrid-electric propulsion systems has received attention.

In the field of hybrid-electric vehicles, Lescot et al. (2010) [15] have considered the importance of integrating EMS and TMS. Lescot et al. used ICE temperature, along with ICE effective torque and battery SOC, as one of the state variables of the optimal control problem. Shams-Zahraei and Kouzani (2012) [16] similarly used ICE temperature along with SOC as a set of state variables, and considered the effect of temperature on fuel consumption. With providing a new Hamiltonian function, it was shown that this control strategy can reduce fuel consumption in driving cycles that include multiple cold starts.

Using ECMS, Pham et al. (2012) [17] showed that if the power consumed for battery heating is less than the power saved due to reducing battery losses and using hybrid propulsion mode, heating the battery to a certain temperature increases the efficiency and SOF of the battery and also reduces

fuel consumption. Li, Evangelou, and Lot (2018) [18] considered the power consumption of the ICE cooling system in the energy management problem and, using ECMS, showed that for a TTR-hybrid vehicle, using an integrated controller will reduce fuel consumption by 2.7%. Zhang and Ivanco (2014) [19] investigated the effect of the power consumption of the battery cooling system on fuel consumption. By simulating a series hybrid propulsion system of a tractor in AMESim® and MATLAB/Simulink® and using an integrated thermostat controller, it was found that under severe conditions (high ambient temperature, rough road, etc.), fuel consumption can increase by up to 5%. By designing an integrated optimal controller and using DP, this figure was reduced to 3.7%.

Pham et al. (2015) [20] used two thermal loops: one for the ICE and the other for the electrical and electronic equipment. It was found that using a rule-based integrated controller: a) fuel consumption will be lower; b) the power consumption of the cooling system will be halved; c) the equivalent fuel consumption and CO₂ emissions will be reduced. Chen and Li (2020) [21], while emphasizing the importance of the work of Jiménez-Espadafor and Guerrero, noted the need to develop integrated controllers based on optimization methods. Considering the ICE and battery cooling systems, thermal terms were added to the objective function of the optimal control problem.

Freudiger et al. (2020) [22] focused on BTMS. By linearizing the power-thermal models, the optimal control problem was transformed into a convex programming problem and it was shown that the use of convex programming can provide solutions similar to DP in a much shorter time (9 times faster). Hu and Amini (2020) [23] considered fuel consumption as a function of the ICE coolant temperature and assumed that the surrounding air is cool. With this assumption, BTMS is naturally discarded. They added temperature to the fuel consumption function arguments, and in this way, linked the two problems of energy and heat management. Using MPC with two time horizons, Hu et al. showed a 4.6 to 17.6 percent reduction in fuel consumption (depending on the driving conditions), and thus judged the effectiveness of this method.

These researches were in the field of hybrid-electric vehicles, but in the aviation sector, the topic of integrated power and thermal management has received less attention. The paper by Freeman and Osterkamp (2014) [24] is probably the first paper published in this field. They introduced the TOGW framework from ESAero and showed that with the help of this framework, it is possible to determine the characteristics of the components of the EMS and TMS, determine their effects on each other.

Abolmoali and Donovan (2020) [25], by combining thermal models and aircraft dynamic model, and proposing a new TMS includes a larger amount of polyalphaolefin heat sink and a new thermal cycle, showed that for a medium-altitude, long-range aircraft weighing 11,901 pounds and flying for 18 hours, by choosing the appropriate speed, altitude, and SOC, an 18% reduction in fuel consumption can be observed. Of course, using the new thermal system neutralizes 94% of this weight reduction.

Emphasizing that in rotorcrafts, fuel is the most important heat sink, Roumeliotis et al. [26] presented an integrated model of the UH-60 helicopter with a parallel hybrid-electric propulsion system. While calculating the heat losses of various components, they introduced a new fuel-oil cooling (FOC) system with fuel return to the tank. Simulations performed in the AMESim® software showed that the original FOC design used in the helicopter cannot be used for a hybridization degree of more than 10%. For higher hybridization levels up to 20%, it is necessary to increase the thermal capacity of the oil (polyalphaolefin) by recirculating the fuel to the tank, while preheating the tank fuel.

Following the work of Roumeliotis, Ouyang et al. (2024) [27] focused on evaluating the capability of a passive cooling system in two critical phases of flight, takeoff and climb. For an aircraft similar to the ATR72, replacing the main propulsion system with a parallel hybrid propulsion system results in an increase in weight of 841 kg. By examining the performance of the HEA in three cold, standard and hot days, it was shown that the use of an active system is mandatory in the final stages of climb, due to the reduction of the battery temperature below the minimum permissible temperature (15 °C). However, Ouyang and his colleagues (2025) [28], also used an active cooling system with PGW40 coolant and showed that 97% of the pump power and 98% of the induced drag of the air passage

system are related to the cooling operation of the motor-inverter. Here, an integrated cooling circuit was used for the motor-inverter and the gas turbine. Accordingly, the researchers concluded that it is generally necessary to use two separate cooling circuits for the motor-inverter and the gas turbine, and it may even be necessary to use a higher-capacity coolant in the motor-inverter cooling circuit.

It can be seen that in the issue of integrated power and thermal management in hybrid-electric vehicles, researchers have focused on providing a high-level control system design. In this regard, the ICE coolant temperature and the battery thermal state have been used to integrate the two issues of energy and thermal management. In contrast, in the aviation sector, researchers have mainly focused on the cooling system design and thermal processes, and there has been little emphasis on the control system design and closed-loop operation of the system. Accordingly, the aim of the present study is to simultaneously consider both cases, and to consider the thermal state of various components in developing an appropriate closed-loop model and evaluating its performance.

3. Propulsion System Design

The propulsion system configuration is as shown in Figure 1 (a typical series configuration). Accordingly, the motor is responsible for delivering the power required by the propeller that is provided by the generator or battery. The ISG acts as a starter when the propulsion system starts up, and after the start-up process enters the generator mode. A low-voltage battery (LVB) is used to provide the power required by the on-board network loads. The LVB is connected to the main high-voltage battery (HVB) through an isolated full-bridge dc-dc converter to be charged by it if necessary. Thus, the subsystems and components studied in this study can be described as follows:

- 1. Air-cooled gas turbine as well as fuel control unit and BOV control
- 2. Liquid-cooled motor and starter/generator
- 3. High-voltage and low-voltage batteries
- 4. Battery management system (BMS) including BTMS and SCU
- 5. Three-phase MOSFET-based liquid-cooled rectifier and inverter
- 6. non-isolated MOSFET-based DC-DC chopper between HVB and DC-link
- 7. Isolated MOSFET-based DC-DC chopper between HVB and LVB
- 8. Gearboxes between turbine and ISG, and motor and propeller
- 9. HVAC system and its controller
- 10. Two separate vector control systems for motor and ISG
- 11. Adaptive control system for DC-DC chopper between HVB and DC-link
- 12. Switching control system based on phase shift method for isolated DC-DC chopper.

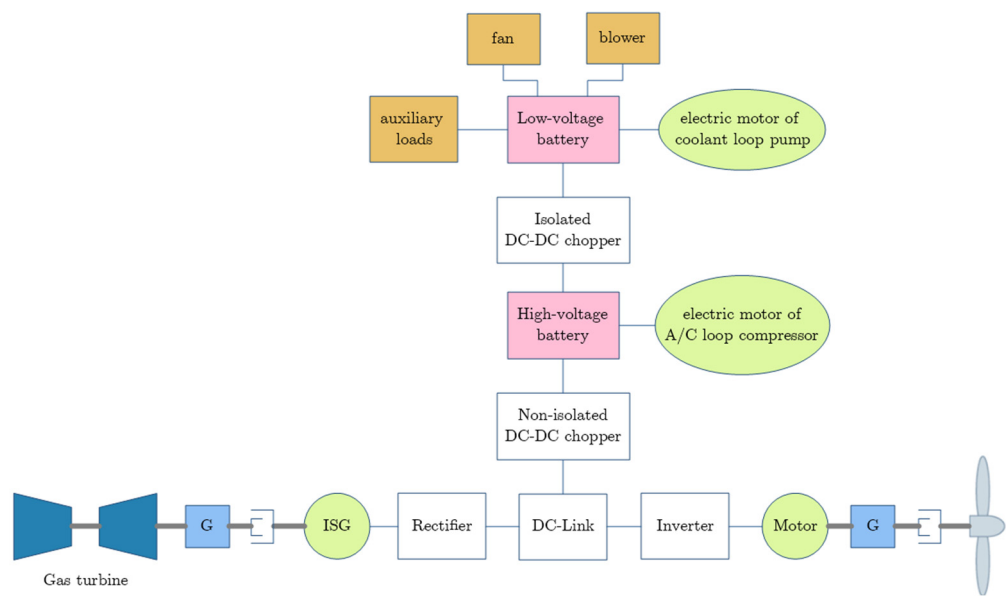


Figure 1. Configuration of the series hybrid propulsion system under consideration; ISG: Integrated Starter/Generator, G: Gearbox.

3.1. Gas Turbine

The gas turbine unit used here is a twin-spool turboshaft engine, which consists of a one-stage centrifugal compressor, driven by a single-stage axial gas generator turbine, which is cooled by air bled from the outlet of the compressor. Part of the air from the outlet of the centrifugal compressor is also used to cool the combustion chamber. The power turbine is uncooled and of the axial type as well. It should be noted that some air is also bled from the outlet of the compressor for seal pressurisation and power turbine balance piston. The mathematical model of the gas turbine is described below.

In general, a compressor is described by the following two functions:

$$p_3/p_2 = f_1(\dot{m}_{2c}, \omega_{ggc}), \quad \eta_{isc} = f_2(\dot{m}_{2c}, \omega_{ggc}), \quad (1)$$

where

$$\dot{m}_{2c} = \frac{\dot{m}_2 \sqrt{\theta_2}}{\delta_2}, \quad \omega_{ggc} = \frac{\omega_{gg}}{\sqrt{\theta_2}}, \quad \theta_2 = \frac{T_2}{T_{std}} \quad \text{and} \quad \delta_2 = \frac{p_2}{p_{std}} \quad (2)$$

The degree of BOV opening at any instant is determined by the HMU and is described by the following function:

$$op_{BOV} = f_3(\omega_{ggc}). \quad (3)$$

Considering the air bleed from the compressor outlet, we can write

$$\dot{m}_3 = \dot{m}_2(1 - b_1 - b_2 - b_3), \quad (4)$$

where b_1 to b_3 are functions of ω_{ggc} and $\dot{m}_{2c}$. Also, we have

$$T_3 = f_4\left(T_2, \frac{p_3}{p_2}, \eta_{isc}\right) \simeq T_2 + \frac{T_{3s} - T_2}{\eta_{isc}} = T_2 \left(1 + \frac{\left(\frac{p_3}{p_2}\right)^{\frac{\gamma-1}{\gamma}} - 1}{\eta_{isc}}\right). \quad (5)$$

Burner efficiency is a function of burner loading and is given by the following equation [29]

$$\eta_b = 1 - \left[(1 - \eta_{des}) \exp\left(B \log \frac{\Omega}{\Omega_{des}}\right)\right]. \quad (6)$$

Ω and B are burner loading and burner loading constant, respectively, and

$$\Omega = \frac{\dot{m}_{sw}}{p_3^{1.8} \exp(T_3/300) V'} \quad (7)$$

where V is the combustion chamber volume. The values of the temperature and pressure at the inlet of the gas generator turbine, T_4 and p_4 , are obtained by combining the species kinetic equations, mass conservation equations, and the equation of state, and then solving the resulting system of equations. From the gas generator turbine efficiency map, we have

$$\eta_{isgg} = f_5(\dot{m}_{4c}, \omega_{ggc}). \quad (8)$$

So, it follows that

$$T_5 \simeq T_4 \left[1 - \eta_{isgg} \left(1 - \left(\frac{p_5}{p_4}\right)^{\frac{\gamma-1}{\gamma}}\right)\right]. \quad (9)$$

For the power turbine, the same can be done as for gas generator,

$$p_6/p_5 = f_6(\dot{m}_{5c}, \omega_{ptc}) \quad \text{and} \quad T_6 = T_5 \left[1 - \eta_{ispt} \left(1 - \left(\frac{p_6}{p_5}\right)^{\frac{\gamma-1}{\gamma}}\right)\right], \quad (10)$$

where $\dot{m}_5 = \dot{m}_4 + \dot{m}_2 b_3 = \dot{m}_3 + \dot{m}_f + \dot{m}_2 b_3$. Finally, the torque values are obtained from the following equations:

$$\tau_c = \frac{1}{\omega_{gg}} \dot{m}_2 (h_3 - h_2), \quad \tau_{gg} = \frac{1}{\omega_{gg}} \dot{m}_4 (h_4 - h_5), \quad \tau_{pt} = \frac{1}{\omega_{pt}} \dot{m}_5 (h_5 - h_6) \quad (11)$$

Also, the equations expressing the dynamics of the shafts are as follows

$$J_{gg} \frac{d\omega_{gg}}{dt} = \tau_{gg} - \tau_c - \tau_{damp,gg}, \quad J_{pteq} \frac{d\omega_{pt}}{dt} = \tau_{pt} - \frac{1}{gr_{pt}} \tau_g - \tau_{damp,pt}, \quad (12)$$

where

$$J_{pteq} = J_{pt} + \frac{J_g}{gr_{pt}^2} \quad \text{and} \quad gr_{pt} = \frac{\omega_{pt}}{\omega_g}. \quad (13)$$

3.2. Gearbox

Here the gearbox is of reduction type for the generator. Another reduction gearbox reduces the high speed of the motor to the lower speed of the load (propeller). We have

$$\frac{\omega_1}{\omega_2} = \frac{\tau_2}{\tau_1} = \eta \frac{N_2}{N_1} = \eta \cdot gr. \quad (14)$$

3.3. Generator and Motor

The power produced by the power turbine is delivered to a generator which produces electrical power. This electrical power is used to power the motor and, when needed, to charge the battery. Both the motor and generator are three-phase permanent magnet synchronous machines. The following describes the mathematical model used for the motor and generator.

For a three-phase machine, the three-phase voltage values are given as

$$\begin{bmatrix} v_a \\ v_b \\ v_c \end{bmatrix} = \begin{bmatrix} R_s & 0 & 0 \\ 0 & R_s & 0 \\ 0 & 0 & R_s \end{bmatrix} \begin{bmatrix} i_a \\ i_b \\ i_c \end{bmatrix} + \frac{d}{dt} \begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \end{bmatrix}. \quad (15)$$

Using the modified Park transformation as [30]

$$T_{abc \rightarrow dq0} = \frac{2}{3} \begin{bmatrix} \cos \theta & \cos \left(\theta - \frac{2\pi}{3} \right) & \cos \left(\theta + \frac{2\pi}{3} \right) \\ -\sin \theta & -\sin \left(\theta - \frac{2\pi}{3} \right) & -\sin \left(\theta + \frac{2\pi}{3} \right) \\ \frac{1}{2} & \frac{1}{2} & \frac{1}{2} \end{bmatrix}, \quad (16)$$

where θ is the angle between the a -axis of the fixed coordinate system and the d -axis of the rotating frame. The values of the voltages in the rotating frame $dq0$ are

$$\begin{bmatrix} v_d \\ v_q \\ v_0 \end{bmatrix} = T_{abc \rightarrow dq0} \begin{bmatrix} v_a \\ v_b \\ v_c \end{bmatrix}. \quad (17)$$

Current is also transferred to the frame $dq0$ in the same way. Flux linkages in the frame $dq0$ are expressed as

$$\lambda_d = \lambda_d(i_d, i_q, T_1, T_2, T_3, \dots), \quad \lambda_q = \lambda_q(i_d, i_q, T_1, T_2, T_3, \dots), \quad \lambda_0 = l_{ew} i_0. \quad (18)$$

i_0 is the homopolar axis current. It should be noted that the model used here is a nonlinear model and therefore writing the d and q axes flux linkages as the product of inductance and current has been avoided. The flux linkage functions are obtained through finite element analysis. The derivatives of the flux linkages are given as follows

$$\frac{d}{dt} \begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \end{bmatrix} = \begin{bmatrix} L_d & M_{dq} & M_{d0} \\ M_{qd} & L_q & M_{q0} \\ M_{0d} & M_{0q} & L_0 \end{bmatrix} \cdot \frac{d}{dt} \begin{bmatrix} i_d \\ i_q \\ i_0 \end{bmatrix}, \quad (19)$$

where

$$L_d = \frac{\partial \lambda_d}{\partial i_d} \quad L_q = \frac{\partial \lambda_q}{\partial i_q} \quad L_d = l_{ew}, \quad (20)$$

$$M_{dq} = M_{qd} = \frac{\partial \lambda_q}{\partial i_d}, \quad M_{0d} = M_{d0} = \frac{\partial \lambda_0}{\partial i_d}, \quad M_{0q} = M_{q0} = \frac{\partial \lambda_0}{\partial i_q}. \quad (21)$$

The loss function is equal to

$$Loss = SCL + Loss_{SCL} = 3R_s i^2 + Loss_{iron} + Loss_{yoke} + Loss_{tooth}, \quad (22)$$

where i is the magnitude of the stator winding current. All the non-copper loss functions on the right-hand side of the above equation are functions with the same arguments as the flux linkage functions. the copper losses of the windings are a function of temperature. The change in the value of the DC and AC losses of the winding due to temperature change are proportional to the coefficient K_{RDC} and K_{RAC}

$$K_{RDC} = 1 + \alpha_T(T_w - T_{ref}), \quad K_{RAC} = K_{RAC}(i_d, i_q, \omega, T_1, T_2, T_3, \dots), \quad (23)$$

where α_T is the temperature coefficient of the DC resistance and T_w is the winding temperature. Finally, the total resistance of the stator winding is obtained from

$$R_s = R_{sDC} + R_{sAC}. \quad (24)$$

The value of the electromagnetic torque is obtained from the following equation

$$T_{em} = \frac{3P}{2} (\lambda_d i_q - \lambda_q i_d) - \frac{Loss_{SCL}}{\omega_{mech}}. \quad (25)$$

The equation of the motor-propeller shaft dynamics can also be written as

$$J_{meq} \frac{d\omega_m}{dt} = \tau_m - \frac{1}{gr_m} \tau_p - \tau_{damp,m} \quad \text{where} \quad J_{meq} = J_m + \frac{J_p}{gr_m^2} \quad \text{and} \quad gr_m = \frac{\omega_m}{\omega_p}. \quad (26)$$

3.4. Rectifier and Inverter

Figure 2 shows the layout of the rectifier-inverter assembly located between the generator and the motor. Each inverter and rectifier arm consists of two modules where each module is consisting of a MOSFET and a flyback diode. Here, the MOSFET has two states: on and off, and the switching dead time is taken into account. The conduction and switching losses are calculated based on the characteristics of the semiconductor. Table 1 shows the resistance and voltage values of each module in different states.

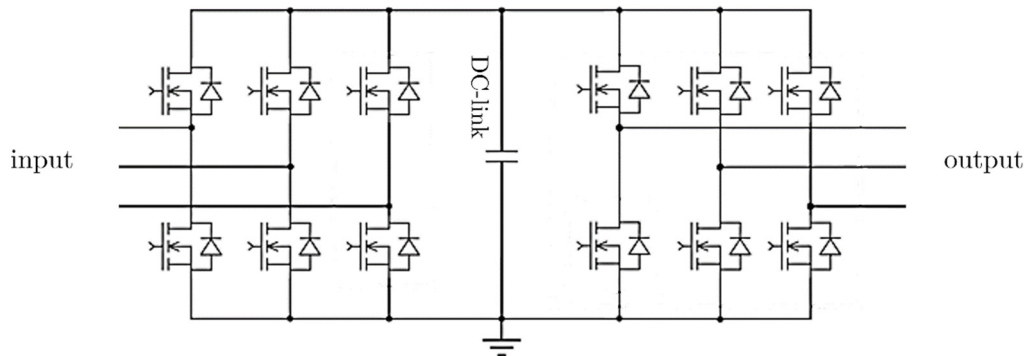


Figure 2. Three-phase rectifier-inverter set.

Given the dependence of the characteristics and performance of the semiconductor and diode on temperature, a Foster network is used for thermal modeling of MOSFET and diode. In this study, an integrated module is used, meaning that there is no separate diode, but a freewheel diode is present as a body diode next to the MOSFET, and therefore the MOSFET and diode together form an

integrated module. Thus, the diode and MOSFET operate on one junction and a single Foster network is used for the entire module.

Table 1. Resistance and voltage of the MOSFET-diode module in different states.

state	r_m	v_m
No current flows through the module	R_{off}	0
MOSFET conduction	$R_{on,M}$	$V_{on,M}$
diode conduction	$R_{on,d}$	$-V_{on,d}$
MOSFET in reverse mode	$R_{on,Mr}$	$-V_{on,Mr}$

The inverter and rectifier used here are liquid-cooled. The Foster network parameters model the junction-fluid thermal path. Given that the cooling is done by cold plate (direct liquid cooling), the Foster network is directly in contact with the cooling circuit. The junction temperature is equal to the temperature of the first thermal capacitance in the equivalent Cauer network, and the output heat flux is equal to the heat flux of the last thermal resistor

$$T_{junc} = T_{C_{th,1}} \quad \text{and} \quad \phi_{th} = \phi_{th_{R_n}}. \quad (27)$$

Figure 3 shows the electrothermal model of the liquid-cooled module. The coolant itself is cooled by ram air. Cooling circuit calculations are carried out using the pump characteristic map, the ε -NTU model for heat exchanger [31], and conductive and convective heat transfer relationships [31] for the tube and the fluids in contact with the tube. A similar cooling circuit is also used for the motor and generator.

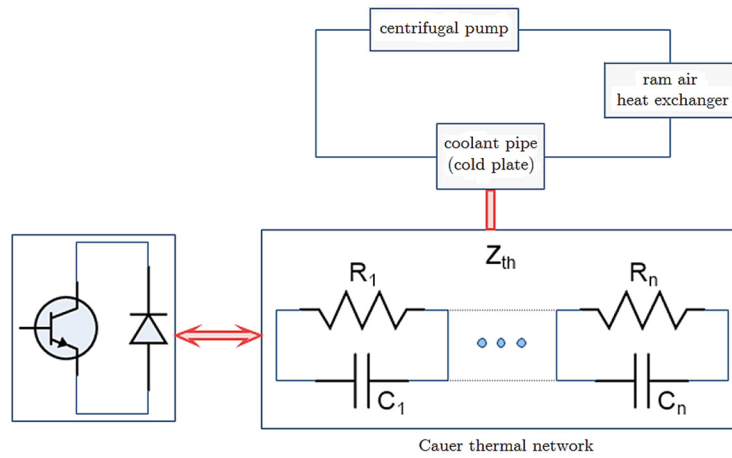


Figure 3. Electrothermal model of liquid-cooled integrated MOSFET-diode module.

3.5. Battery

The equivalent circuit of the battery is shown in Figure 4. The value of the ohmic resistance is different in charging and discharging conditions and is a function of SOC, temperature and current in each mode

$$R_{ohm}^{ch} = R_{ohm}^{ch}(SOC, T, I), \quad R_{ohm}^{dch} = R_{ohm}^{dch}(SOC, T, I). \quad (28)$$

The battery voltage is obtained from the following equation

$$U = OCV - RI. \quad (29)$$

Here we assume that OCV is independent of the battery operating mode (charging or discharging) and is only a function of SOC and temperature. The OCV value is obtained from the following equation

$$OCV = OCV_{T_{ref}}(SOC) + \frac{dU}{dT}(T - T_{ref}). \quad (30)$$

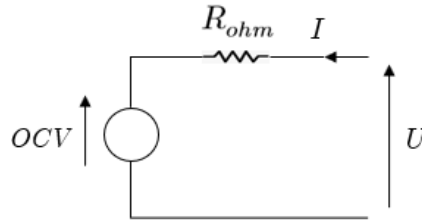


Figure 4. Equivalent circuit of battery.

$\frac{dU}{dT}$ is the entropic heat coefficient (EHC) and its value is a function of SOC. The OCV value at reference temperature is available as a function of SOC, as one of the battery performance curves. The SOC value is calculated via the Coulomb counting method

$$SOC(t) = SOC(t_0) + \int_{t_0}^t \frac{\eta(\tau)I(\tau)d\tau}{C_{bnom}}. \quad (31)$$

Here we ignore aging and capacity loss of the battery. The value of the Coulombic efficiency η is also a function of SOC, temperature and available current

$$\eta = \eta(SOC, T, I). \quad (32)$$

Figure 5 shows how the battery is thermally modeled. The cells and casing are separated for better thermal modeling. The battery heat is dissipated through radiation, convective heat exchange with the surrounding air, and heat transfer to the coolant.

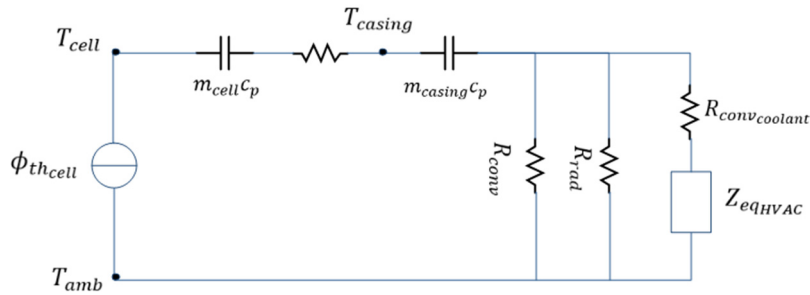


Figure 5. Thermal model of battery.

3.6. DC-DC Chopper Between HVB and DC-Link

In order to regulate the charging and discharging voltage and current, ensure the stability of the DC-link voltage, and maintain the quality of the power delivered to the motor, a DC-DC converter is used between the main battery and the DC-link. This converter is a non-isolated, non-inverting, four-quadrant full-bridge type, as shown in Figure 6. The advantages of the four-quadrant full-bridge converter are its fast dynamic response, high efficiency, and high power density.

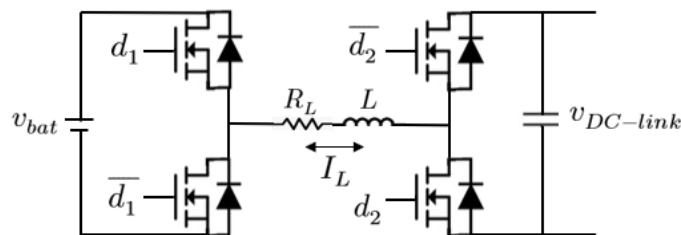


Figure 6. DC-DC chopper between HVB and DC-link.

Each module of this converter consists of a MOSFET and a diode. The integrated MOSFET-diode module follows the model presented in the previous section.

By analyzing the different operating modes of the converter, we can arrive at a suitable model for control purposes. First, we will examine the discharge mode. In the buck mode, $d_2 = 0$ and $\overline{d}_2 = 1$. Using Kirchhoff's voltage law, we have

$$L \frac{dI_L}{dt} = -R_L I_L + d_1 V_{bat} - V_{DC}. \quad (33)$$

In boost mode $d_1 = 0$ and $\overline{d}_1 = 1$. Again, using Kirchhoff's voltage law we conclude

$$L \frac{dI_L}{dt} = -R_L I_L + V_{bat} - (1 - d_2) V_{DC}. \quad (34)$$

A similar analysis can be done for the charging mode. Finally, it follows that the equations for the buck and boost modes in charge mode are the same as the equations for the boost and buck modes in discharge mode, respectively.

3.7. DC-DC Chopper Between HVB and LVB

An isolated full-bridge unidirectional DC-DC converter is used between HVB and LVB to prevent fault propagation in the event of any system contingency. The converter is a full-wave inverter on the primary side and a center-tapped rectifier on the secondary side. Due to the low voltage and relatively high current on the secondary side, a center-tapped rectifier is used. A MOSFET- flyback diode module is again used on the primary side. Figure 7 shows the converter design.

The equations describing this converter are obtained using Kirchhoff's voltage and current laws:

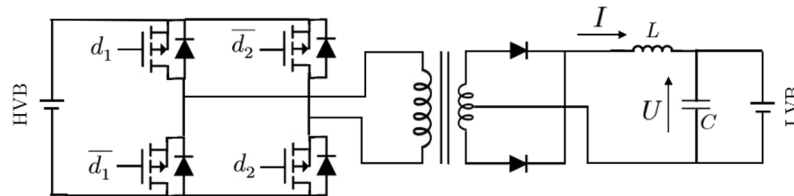


Figure 7. Isolated DC-DC chopper between HVB and LVB.

$$L \frac{dI}{dt} = \frac{n}{2} \frac{(d_1 + d_2)}{2} V_{HVB} - U \quad (35)$$

$$C \frac{dU}{dt} = I - \frac{OCV_{LVB} - U}{R_{LVB}}, \quad (36)$$

where $n = N_s/N_p$ is the ratio of the secondary side turns of the transformer to its primary side turns. To control the computational burden of the problem, the thermal modeling of choppers has been omitted.

3.8. HVAC System

The HVAC system is responsible for battery thermal management and ensuring cabin air quality. The system layout is presented in Figure 8. The A/C system is of the VCS type and consists of a compressor, condenser, TXV, and evaporator. The power required for the compressor is provided by an electric motor powered by the HVB. The refrigerant passing through the condenser is cooled by heat exchange with air sent to the condenser by a fan. The power required for the fan is provided by the LVB. A TXV is used as an expansion device. The air entering the evaporator to heat the refrigerant is a combination of recirculated cabin air and ambient air, which are delivered to the evaporator by a blower. The control system determines the proportion of cabin air and fresh ambient air, according to the requirements of cabin temperature, pressure, and humidity.

The cabin is cooled by the air coming out of the evaporator and the battery is cooled by the radiator and the refrigeration circuit including the chiller. If the temperature difference between the battery and the target temperature is less than a certain limit, the hydraulic valve opens the radiator circuit and keeps the chiller circuit closed. Otherwise, the hydraulic valve closes the radiator circuit and activates the chiller circuit. The coolant cooled in the chiller is sent to the BHXS by a pump to absorb the battery heat. The power required by the pump is provided by a motor powered by the LVB.

The cabin and battery are heated by PTC heaters, respectively, by increasing the air and coolant temperatures. The battery heating circuit is activated by a hydraulic valve. The electrical power of both PTC heaters is supplied by the HVB. Table 2 shows the mathematical description of the HVAC system.

Table 2. mathematical model of the HVAC system.

compressor	$\dot{m}_{compA/C} = \dot{m}_{compA/C} \left(pr_{compA/C}, \omega_{compA/C} \right), \quad \eta_{iscompA/C} = \frac{h_{disis} - h_{suc}}{h_{dis} - h_{suc}}$		
	$\simeq \frac{T_{disis} - T_{suc}}{T_{dis} - T_{suc}}, \quad T_{disis} = T_{suc} \left(\frac{p_{dis}}{p_{suc}} \right)^{\frac{\gamma-1}{\gamma}}$		
	$\tau_{compA/C} = \frac{\eta_{mech}(h_{dis} - h_{suc})\dot{m}_{compA/C}}{\omega_{compA/C}}$		
condenser and evaporator	$Nu_{lam} = 3.66$		(ref [31])
	single-phase flow	$h = Nu \frac{\lambda}{D_h} :$	$Nu_{turb} = \frac{\left(\frac{\xi}{8}\right)(Re-1000)Pr}{1+12.7\sqrt{\frac{\xi}{8}}\left(Pr^{\frac{2}{3}}-1\right)}$ (ref [32])
	refrigerant-side heat transfer	condenser:	$h = h_L \left[(1-x)^{0.8} + \frac{3.8x^{0.76}(1-x)^{0.04}}{p_r^{0.38}} \right]$ (ref [33])
	two-phase flow	evaporator:	$h = \sqrt[3]{h_{cv}^3 + h_{NCB}^3}$ (ref [32])
	$Nu_{lam} = 3.66$		(ref [31])
	air-side heat transfer	$h = Nu \frac{\lambda}{D_h} :$	$Nu_{turb} = 0.03 Re^{0.8} Pr^{1/3}$ (ref [31])
	pressure drop	$-\frac{dp}{dx} = \frac{\xi \rho \bar{u}^2}{2D_h}$	(ref [31])
	temperature and heat flux	$\phi_{int} = h_{refrig} A_{int} (T_{refrig} - T_{wall}),$	$\frac{dT_{refrig}}{dt} = \frac{-\phi_{int}}{m_{refrig} c_{p,refrig}}$
		$\phi_{ext} = \eta_f h_{air} A_{ext} (T_{air} - T_{wall}),$	$\frac{dT_{air}}{dt} = \frac{-\phi_{ext}}{m_{air} c_{p,air}}, \quad \frac{dT_{wall}}{dt}$
		$= \frac{\phi_{int} + \phi_{ext}}{m_{wall} c_{p,wall}}$	
TXV	p_{bulb} : 1st quadrant of TXV map, l_v : 2nd quadrant of TXV map		
	$dT_b = \frac{T_e - T_b}{\tau_{valve}},$	$T_{outTXV} = T_e - SH,$	$h_{inTXV} = h_{outTXV}$
chiller	refrigerant-side heat transfer, and pressure drop	same as condenser and evaporator	
	coolant-side heat transfer	$Nu = \sqrt[3]{Nu_{free}^3 + Nu_{forced}^3}, \quad Nu_{free, lam} = 0.6(Gr Pr)^{\frac{1}{4}},$ $Nu_{free, turb} = 0.15(Gr Pr)^{\frac{1}{3}}$	

	(ref [33])
	$Nu_{forced_{lam}} = 3.66$ (ref [31]), $Nu_{forced_{turb}} = \frac{(\frac{\xi}{8})(Re-1000)Pr}{1+12.7\sqrt{\frac{\xi}{8}}(Pr^{\frac{2}{3}}-1)}$ (ref [32])
temperature and heat flux	$\phi_{coolant} = h_{coolant}A_{coolant}(T_{coolant} - T_{wall}), \quad \frac{dT_{wall}}{dt} = \frac{\phi_{refrig} + \phi_{coolant}}{m_{wall}c_{p_{wall}}}$
coolant-side heat transfer, and pressure drop	same as chiller
BHXS battery-side heat transfer (conduction)	$\phi_{bat} = \frac{k_{bat}A_{bat-pipe}(T_{bat} - T_{wall_{pipe}})}{L_{bat-pipe}}$
fan/blower	$P_{f/b} = V_{f/b}I_{f/b}, \quad V_{f/b} = V_{LVB}, \quad I_{f/b} = I(\omega_{f/b})$
radiator	ε - NTU model for heat exchangers (ref [31])
coolant-loop pump	$T_{coolant} = constant, \quad p_{out} - p_{in} = f_p(Q, \Omega_p, T)$ (f_p : characteristic map of pump)
PTC heaters	$R_{th} = R_{th}(T), \quad V_{th} = V_{HVB}, \quad I_{th} = \frac{V_{th}}{R_{th}}$
	$Nu = \sqrt[3]{Nu_{free}^3 + Nu_{forced}^3}, \quad Nu_{free} = 0.68 + \frac{0.67Ra^{\frac{1}{4}}}{\left(1 + \left(\frac{0.492}{Pr}\right)^{\frac{9}{16}}\right)^{\frac{4}{9}}}, \quad Nu_{forced} = \frac{(0.037Re^{0.8} - 871)Pr^{\frac{1}{3}}}{(ref [32])}$
cabin	$\frac{dT_w}{dt} = \frac{\phi_{solar} + \phi_{int} + \phi_{ext}}{m_w c_{p_w}}, \quad \phi_{solar} = \alpha q_s A_{ext}$
	$\frac{dT_{int}}{dt} = \frac{dT_{cab}}{dt} = \frac{1}{\rho_{cab} v_c c_{p_{cab}}} \left(\dot{m}_{air_{cabin}}(h_{in} - h_{out}) + h_{int} A_{int}(T_w - T_{cab}) \right), \quad \rho_{cab} = \frac{1+ah}{r_{da} + r_{wp}} \frac{p}{T}$

4. Control System

4.1. Gas Turbine

The gas turbine control unit here is of the EEC type which actually consists of an ECU and an HMU. The general structure of the control unit is largely similar to the design presented in NASA Technical Report 100991 [34]. The ECU controls the speed and inlet temperature of the power turbine, preventing it from overspeeding and overheating. The HMU determines the desired fuel flow rate by measuring the speed of the gas generator, the inlet temperature of the compressor, the pilot demand and the ECU command. In addition, the HMU has another very important task, which is regulating the fuel pressure via the outlet pressure of the compressor. The control of BOV opening is also with the HMU.

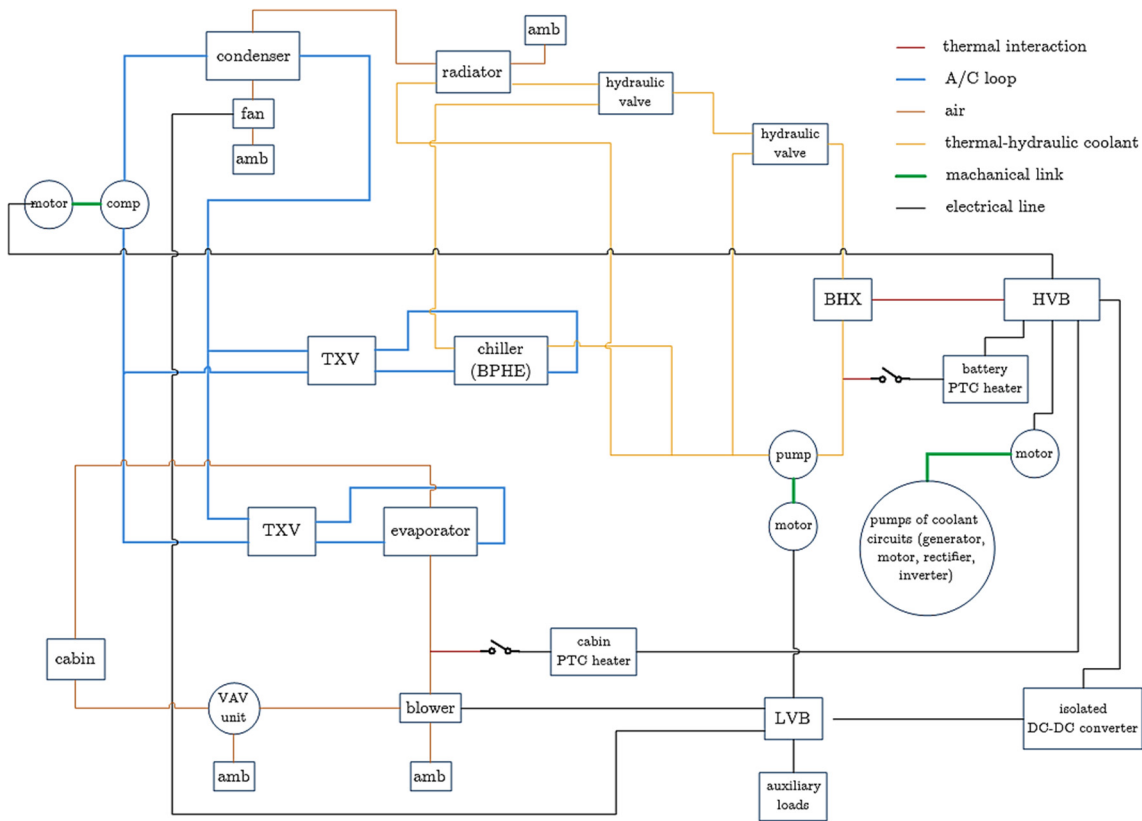


Figure 8. HVAC system scheme.

Figure 9 shows the ECU design. After measuring the speed of the power turbine and calculating the speed error, the governor takes the necessary steps to compensate for the speed error, as a result of which an error signal in volts is generated. This error signal is then converted to the desired error signal by a phase-lag compensator in governor dynamics compensation block (to compensate for the phase lag and slowdown of the system response imposed on the system by the governor). Simultaneously with the measurement of the power turbine speed, the inlet temperature of the power turbine is also measured by a thermocouple and the temperature error signal is converted to the desired temperature error signal (in volts) by another phase-lag compensator. For better control of the power turbine, the larger error signal is selected from these two, and after compensation with a PI controller, is sent to the torque motor.

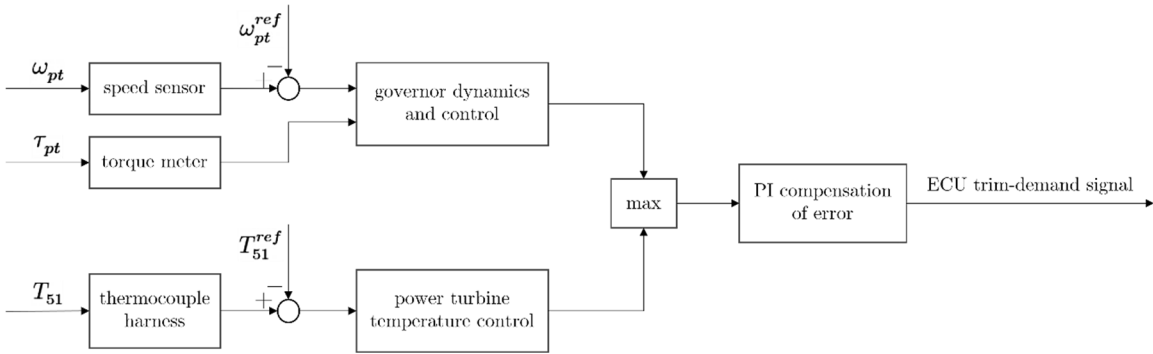


Figure 9. Gas turbine control system: ECU.

The ECU output signal, as shown in Figure 10, will be one of the inputs to the HMU. After the torque motor receives the command and takes the necessary action, the torque motor status is evaluated by an LVDT and after the LVDT output signal passes through the integrator and gain, the

fuel flow signal corresponding to the ECU command (necessary to correct the power turbine state) is obtained. Another signal is obtained based on the pilot demand. Apart from these, the HMU also works with five other fuel signals, two of which are related to the current state of the gas generator (combustion chamber temperature and pressure, gas generator speed) and three of which (deceleration, idle and acceleration schedules) are related to the permissible operating limits of the gas turbine. The droop control block ensures that the desired power is achieved by adjusting the gas generator speed, and the chamber control block prevents the chamber temperature and pressure from entering the undesirable range by adjusting the fuel signal based on the compressor inlet air temperature. The idle schedule determines the minimum fuel flow rate required to continue operating the gas turbine. The acceleration schedule determines the maximum allowable fuel flow rate to prevent compressor stalling during acceleration. Similarly, the Deceleration schedule determines a minimum limit for the fuel flow rate so that the flame does not go out during deceleration. Finally, after comparing the final fuel signal (the sum of four fuel signals) with the allowable limits corresponding to the acceleration, deceleration, and idle programs, the final appropriate value for the fuel flow rate is obtained, and after pressure regulation with the compressor outlet pressure, fuel is injected into the chamber.

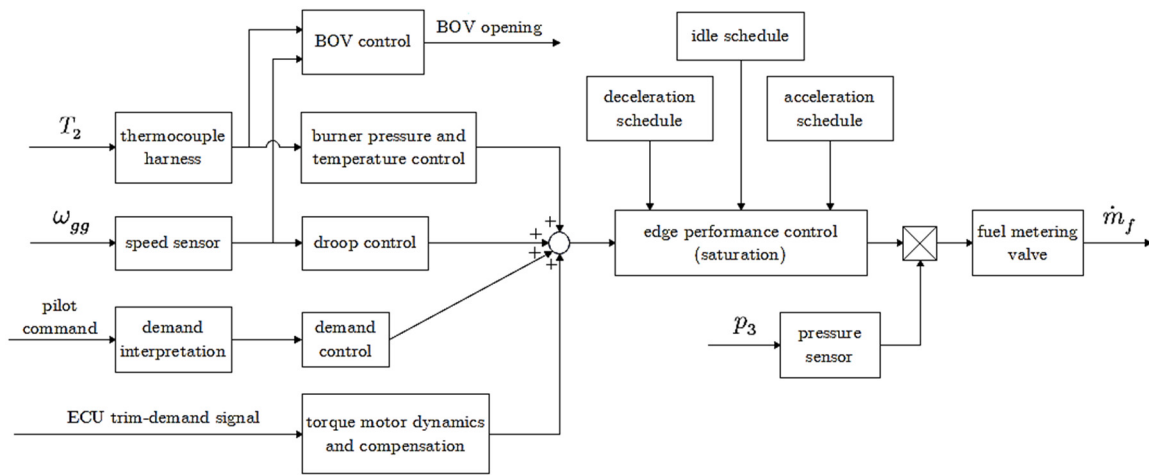


Figure 10. Gas turbine control system: HMU.

4.2. Generator and Motor

In this paper, vector control is used as one of the high-performance methods for machine control. The control system design is shown in Figures 11–13. The control system has an outer loop for speed control and an inner loop for torque control. The output of the outer loop is the torque reference value, which is obtained using PI control, by considering the speed deviation from the desired speed value. The inner loop consists of four blocks: 1) torque-to-current conversion block, 2) current control block (current-to-voltage conversion), 3) switching sequence determination, and 4) conversion of the three-phase values (measured by sensors) to corresponding values in the frame $dq0$ for feedback to the control blocks.

In the torque control block, first the optimal value of i_d is determined with the help of a lookup table, which is a function obtained based on the MTPA approach:

$$i_d^* = f_d(\tau_{ref}). \quad (37)$$

Accordingly, the reference value of i_q is equal to

$$i_q^* = \frac{\frac{\tau_{ref}}{\frac{3}{2}P} + \lambda_q i_d^*}{\lambda_d}, \quad (38)$$

λ_d and λ_q are functions of i_d and i_q . If we denote the maximum allowable stator current by i_s , then the following condition must hold

$$-i_s \leq i_d^* \leq i_s. \quad (39)$$

We also have

$$-i_{q_{max}}^* \leq i_q^* \leq i_{q_{max}}^* \quad \text{where} \quad i_{q_{max}}^* = \sqrt{i_s^2 - i_d^{*2}}. \quad (40)$$

The error values of the d and q axes currents are

$$e_{i_d} = i_d^* - i_d, \quad e_{i_q} = i_q^* - i_q. \quad (41)$$

The current error values are converted into voltage error values by passing through a PI block. Then, back emf compensation is performed and after passing the values obtained from the voltage saturation blocks, the voltage reference values are obtained

$$v_d^* = \text{saturation} \left(e_{v_d} + R_s i_d + \frac{d\lambda_d}{dt} - \omega \lambda_q \right), \quad v_q^* = \text{saturation} \left(e_{v_q} + R_s i_q + \frac{d\lambda_q}{dt} + \omega \lambda_d \right) \quad (42)$$

The maximum allowable voltage (line to neutral) of the stator is obtained according to the DC voltage of the converter

$$v_s = \frac{v_{DC_{converter}}}{\sqrt{3}}. \quad (43)$$

The saturation block limits v_d^* to v_s :

$$-v_s \leq v_d^* \leq v_s. \quad (44)$$

The maximum allowable voltage value for the q -axis is equal to

$$-v_{q_{max}}^* \leq v_q^* \leq v_{q_{max}}^* \quad \text{where} \quad v_{q_{max}}^* = \sqrt{v_s^2 - (v_d^*)^2}. \quad (45)$$

The voltage reference values in the third block are converted to three-phase voltage values using the modified Park transform and these values are fed to the PWM block. The output of this block is the switching scheme that is applied to the gates of the switches to provide the desired voltage. The PWM can be implemented by any suitable modulation method.

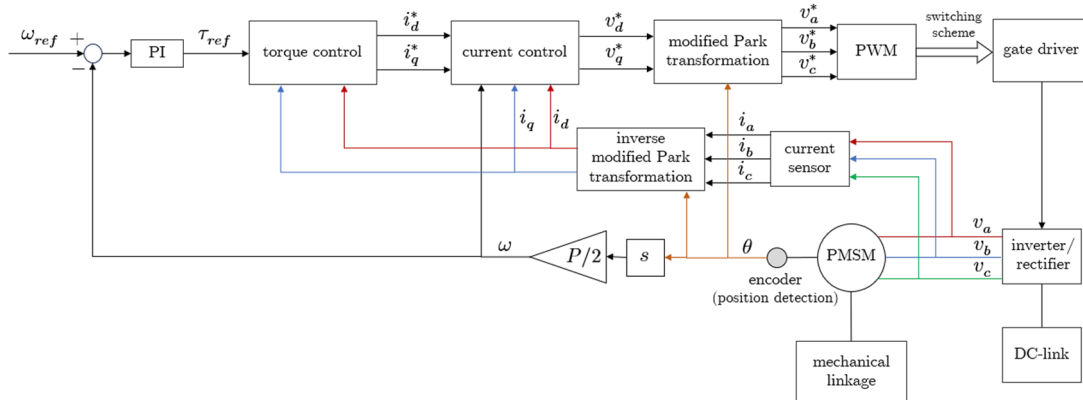


Figure 11. Vector control system for generator and motor.

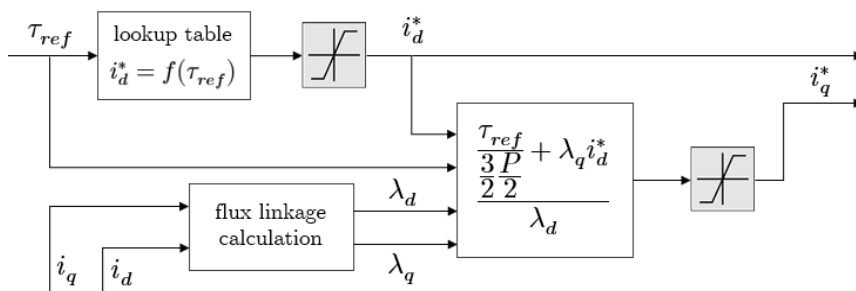


Figure 12. Torque control block.

4.3. Battery

If the battery has the configuration n_sS n_pP and the battery current and terminal voltage are equal to I and U respectively, then, given the previous assumption that all cells behave exactly the same,

Now, if we represent the maximum and minimum values allowed for the voltage of each cell, which are characteristics of the battery cell, by U_{cmin} and U_{cmax} , then

I_{min}^{cont} and I_{max}^{cont} indicate the maximum and minimum allowed continuous current of the battery. Since the pulse current can be greater than the continuous current, when the current exceeds I_{max}^{cont} or falls below I_{min}^{cont} , the pulse clock is turned on ($t = t_0^{pulse}$). If the allowed time for the pulse is $\Delta t_p^{pulse}(I^{pulse})$, then for currents greater than I_{max}^{cont} or smaller than I_{min}^{cont} we have

where $I_{max_{dch}}^{pulse} < 0$ and $\alpha^p = \frac{t-t_0^{pulse}}{\Delta t_p^{pulse}}$. In order to ensure that the battery temperature does not exceed the maximum allowed temperature, the SCU uses the coefficient α_T

Eventually

$$\begin{cases} I_{min}^{bat} = \alpha_T I_{min} \\ I_{max}^{bat} = \alpha_T I_{max} \end{cases} \quad \text{where} \quad \begin{cases} I_{min} = \max(I_{min}^{cont}, I_{min}^{pulse}) \\ I_{max} = \min(I_{max}^{cont}, I_{max}^{pulse}) \end{cases} \quad (50)$$

4.4. Non-Isolated DC-DC Chopper

In the modeling section, it was shown that the DC-DC chopper between HVB and DC-link obeys the following model

$$\text{charge, boost/discharge, buck:} \quad d_1 = \frac{L \frac{dI_L}{dt} + R_L I_L + V_{DC}}{V_{bat}}, \quad (51)$$

$$\text{charge, buck/discharge, boost:} \quad d_2 = \frac{L \frac{dI_L}{dt} + R_L I_L + V_{DC} - V_{bat}}{V_{DC}}. \quad (52)$$

If we represent d_1 and d_2 with the general symbol d , then we can write

$$d = \frac{L \frac{dI_L}{dt} + R_L I_L + A}{B}. \quad (53)$$

Accordingly, for buck discharge or boost charge mode, $d = d_1$, $A = V_{DC}$ and $B = V_{bat}$. On the other hand, for boost discharge or buck charge mode, $d = d_2$, $A = V_{DC} - V_{bat}$ and $B = V_{DC}$. If we call the desired reference value of the inductor current I_L^* , then we can write

$$\frac{L \frac{dI_L^*}{dt} + R_L I_L^* + A}{B} = \Psi^T \Theta, \quad (54)$$

where

$$\Psi = \begin{bmatrix} \frac{dI_L^*}{dt} & I_L^* & 1 \end{bmatrix}^T \quad \text{and} \quad \Theta = \begin{bmatrix} \frac{L}{B} & \frac{R_L}{B} & \frac{A}{B} \end{bmatrix}^T = \frac{1}{B} [L \quad R_L \quad A]^T. \quad (55)$$

Here it is assumed that we do not know the exact value of L and R_L . With this assumption, if we set $e = I_L - I_L^*$, using the control law

$$d = \Psi^T \hat{\Theta} - \zeta e, \quad (56)$$

will ensure the optimal and stable performance of the system. ζ is a positive constant and $\hat{\Theta}$ is an estimate of Θ .

To understand this, suppose the Lyapunov function V is as follows

$$V = \frac{1}{2} (Le^2 + (\tilde{\Theta})^T \Lambda^{-1} \tilde{\Theta}), \quad (57)$$

where $\tilde{\Theta} = \hat{\Theta} - \Theta$ and $\Lambda = \text{diag}(\lambda_1, \lambda_2, \lambda_3)$. λ_1 to λ_3 are constants greater than zero. In this case

$$\dot{V} = Le\dot{e} + (\tilde{\Theta})^T \Lambda^{-1} \dot{\tilde{\Theta}} = Le\dot{e} + (\tilde{\Theta})^T \Lambda^{-1} (\dot{\hat{\Theta}} - \dot{\Theta}). \quad (58)$$

The error dynamics according to the definition of e and equations (53) and (54) is as follows

$$\dot{e} = \dot{I}_L - \dot{I}_L^* = \frac{Bd - R_L I_L - A}{L} - \frac{B\Psi^T \Theta - R_L I_L^* - A}{L} = \frac{B(d - \Psi^T \Theta) - R_L e}{L}. \quad (59)$$

If $d = \Psi^T \hat{\Theta} - \zeta e$ then

$$\dot{e} = \frac{B(\Psi^T \hat{\Theta} - \zeta e) - R_L e}{L} \quad \text{or} \quad \dot{e} = \frac{B}{L} \Psi^T \tilde{\Theta} - \left(\frac{B\zeta + R_L}{L} \right) e. \quad (60)$$

By substituting the above relation into equation (58), we get

$$\dot{V} = e (B\Psi^T \tilde{\Theta} - Ke) + (\tilde{\Theta})^T \Lambda^{-1} \dot{\tilde{\Theta}} - (\tilde{\Theta})^T \Lambda^{-1} \dot{\Theta} \quad \text{where} \quad K = B\zeta + R_L. \quad (61)$$

Thus, it can be written

$$\dot{V} = -Ke^2 + (\tilde{\Theta})^T (\Lambda^{-1} \dot{\tilde{\Theta}} + B\Psi e - \Lambda^{-1} \dot{\Theta}). \quad (62)$$

This clearly leads us to use the law of adaptation

$$\dot{\tilde{\theta}} = -\Lambda B \Psi e + \dot{\theta}. \quad (63)$$

In this situation, to prove the stability of the closed-loop system, it is sufficient to show that $e = 0$ is the equilibrium point of the error dynamics. Naturally, we turn to the LaSalle-Yoshizawa theorem. But first we must ensure that $\dot{e}$ is locally Lipschitz in e and continuous in t (the conditions of the LaSalle-Yoshizawa theorem).

By looking carefully at equation (60), we find that for $\dot{e}$ to be continuous in t , it is necessary that e and $\Psi^T \tilde{\theta}$ are continuous in t . The continuity of e and Ψ is obvious, since these two are functions of the induction current and the induction current has continuous changes. For $\tilde{\theta}$, we note that according to the adaptation law (63), $\dot{\tilde{\theta}}$ is continuous in t and since $\dot{\tilde{\theta}}$ is integrable (this can be easily concluded from the definition of θ and the adaptation law (63)), then $\tilde{\theta}$ and consequently $\Psi^T \tilde{\theta}$ will be continuous in t .

Now we note that in the neighborhood of $e = 0$, e is bounded (this is clear from the definition of e and the continuity of the induction current). Consequently, $\tilde{\theta}$ is also bounded according to the adaptation law (63), and this guarantees the boundedness of $\dot{e}$. Therefore, there is at least one neighborhood of $e = 0$ for which the Lipschitz condition holds for a $L_e > 0$

$$\|\dot{e}(e_2, t) - \dot{e}(e_1, t)\| \leq L_e \|e_2 - e_1\|. \quad (64)$$

Therefore $\dot{e}$ is locally Lipschitz around $e = 0$ and so we can use the LaSalle-Yoshizawa theorem for $\dot{e}$. We need to show that there exist functions γ_1 and γ_2 of class $\mathcal{K}_\infty$ and also a continuous function W such that the following inequalities hold for them

$$\gamma_1(|e|) \leq V \leq \gamma_2(|e|), \quad \dot{V} \leq -W(e) \leq 0, \quad (65)$$

where V denotes the Lyapunov function (57). With a little mathematical operation, it can be shown that the following functions satisfy the above inequalities:

$$\gamma_1(|e|) = \frac{L}{2} e^2, \quad \gamma_2(|e|) = K_1 e^2, \quad W(e) = \frac{e^2}{K_4}. \quad (66)$$

where

$$K_1 = \frac{L + \lambda_m K_2^2 K_3^2}{2}, \quad \lambda_m = \max(\lambda_1, \lambda_2, \lambda_3), \quad K_2 = B^2 |\Lambda|^2, \quad K_3^2 = \sup(\|\Psi\|^2) \quad (67)$$

$$K_4 = K_5 + \frac{1}{K_5}, \quad K_5 = \frac{K_6 \zeta + R_L}{L}, \quad K_6 = \sup B \quad (68)$$

Thus $e = 0$ is the equilibrium point and since the function W is positive definite, the equilibrium point $e = 0$ is globally uniformly asymptotically stable. Therefore, the control law (56) together with the adaptation law (63) ensure the stable and desirable performance of the closed-loop system. Figure 14 shows the scheme of the non-isolated chopper adaptive control system.

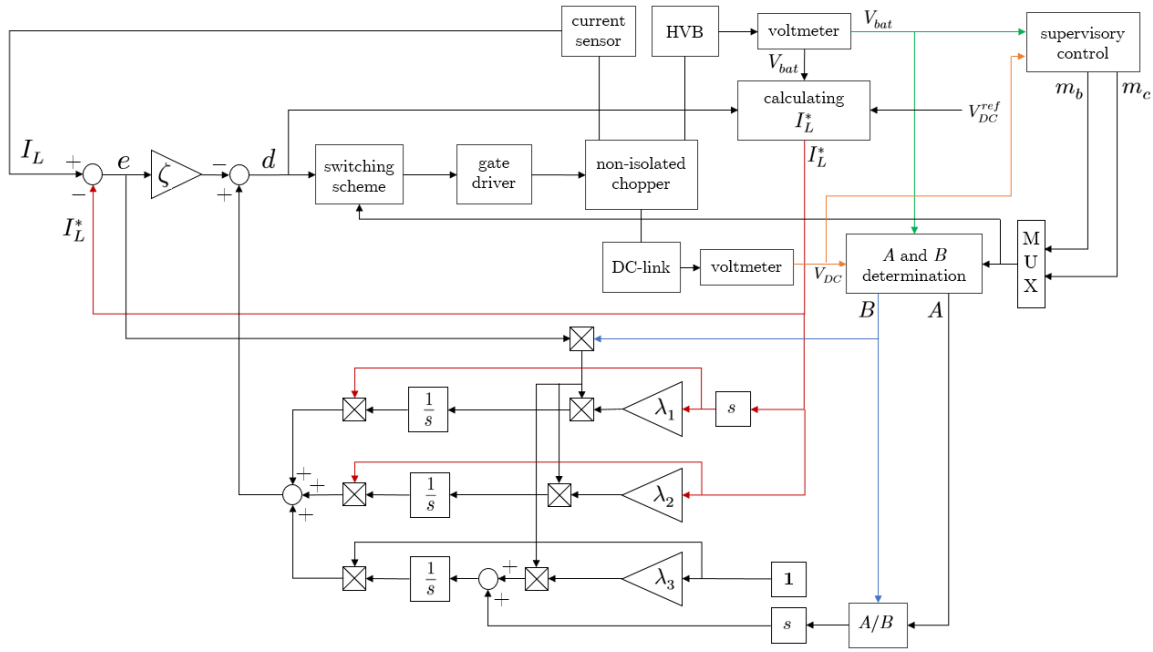


Figure 14. Adaptive control system for non-isolated DC-DC chopper between HVB and DC-link.

4.5. Isolated DC-DC Chopper

In this paper, the phase shift method is used to control the switching of the isolated DC-DC converter. The amount of phase shift between the two arms is determined based on the difference between the battery current and the desired current using a PI block. Being on or off of switches is determined based on the time t , the phase shift value (or equivalently the time shift), and the duty cycle. First the time signal t is received by the controller. To determine the value of d_1 , this signal t is multiplied by the switching frequency f_s and then the decimal part of the resulting value is evaluated. If the decimal part of the resulting value is less than the duty cycle, the on state is assigned to switch 1, otherwise the off state is assigned to it. The state of switch 2 will be the opposite of the state of switch 1 at any time. In mathematical terms

$$\text{frac}(t \cdot f_s) < D \Rightarrow d_1 = 1, \bar{d}_1 = 0, \quad \text{frac}(t \cdot f_s) \geq D \Rightarrow d_1 = 0, \bar{d}_1 = 1 \quad (69)$$

To determine the state of the switches in arm 2, $t + \Delta t$ is used. So

$$\text{frac}((t + \Delta t) \cdot f_s) < D \Rightarrow \bar{d}_2 = 1, d_2 = 0, \quad \text{frac}((t + \Delta t) \cdot f_s) \geq D \Rightarrow \bar{d}_2 = 0, d_2 = 1 \quad (70)$$

Figure 15 shows the control system layout.

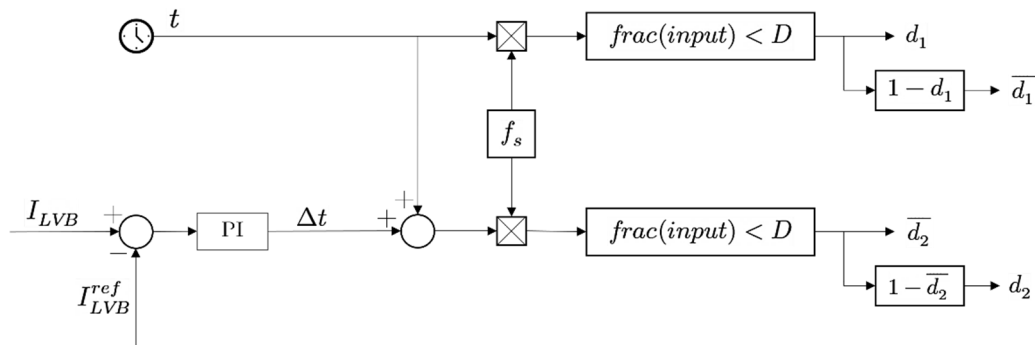


Figure 15. phase-shift control of isolated DC-DC chopper between HVB and LVB.

4.6. HVAC System

The HVAC system controller is a thermostat controller that, by continuously monitoring the battery temperature, and the cabin temperature and humidity, determines the following items at any moment to bring the temperature and humidity values to the desired values:

1. the status of the hydraulic valve for heating/cooling the battery
2. the status of the hydraulic valve for radiator/chiller branch
3. reference torque for the motors supplying power to the A/C loop compressor and coolant loop pump
4. the status of PTC heaters
5. reference value of fan and blower speeds
6. the share of recirculation line air in the air entering the evaporator

4.6. Supervisory Control

The high-level controller is responsible for coordinating the lower-level controllers with each other and issuing appropriate commands to them. Figure 16 illustrates the logic governing the supervisory controller. This controller is a rule-based controller and, after determining the desired requested power at any time, determines how much each of the battery and generator contributes to providing this power. In emergency situations, if the gas turbine fails, the power required by the motor is supplied by the battery until the gas turbine returns to the circuit. If the gas turbine has not failed, and if for any reason, the supervisor detects an emergency, it increases the battery's share in generating and supplying power as necessary.

The important feature of the supervisor here is that during takeoff and climb, it calls on the battery to help in order to perform the power-intensive phases of takeoff and climb in the best possible way. In addition, the supervisor, when needed and if possible, instructs the generator to produce an additional amount of power to charge the battery. Meanwhile, when the aircraft propeller is in windmill mode, if the SOC of the battery is less than SOC_{max} , the motor connected to the propeller changes its role and acts as a generator to charge the battery.

The flowchart of the closed-loop model is shown in Figures S1–S3 of the Supplementary Material.

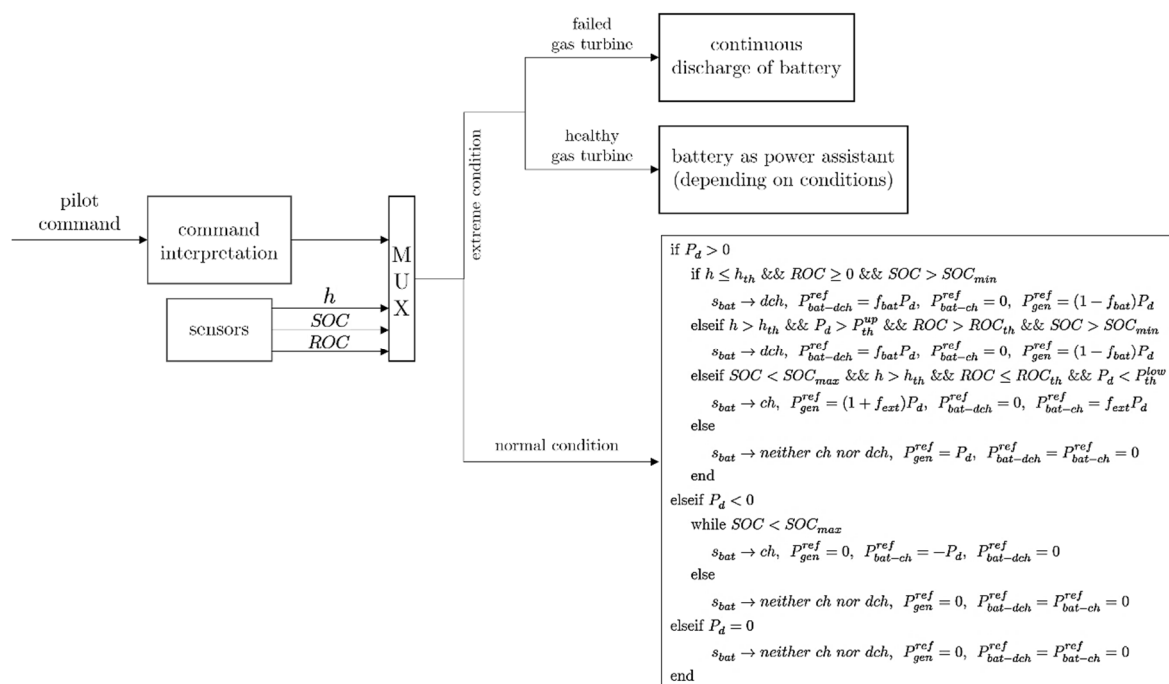


Figure 16. Controlling the way power is generated and distributed by supervisory controller.

5. Case Study

In this section, we will examine the performance of the developed closed-loop model for an aircraft in the Cessna 162 class. The standard model of aircraft utilizes a Continental O-200D piston engine with a rated power of 100 hp (75 kW). Here, however, a hybrid-electric propulsion system is used instead of the standard piston engine. The aim is to:

- First:* ensure the correct operation of the closed-loop system in flight conditions, and
- Second:* compare the performance of two hybrid and non-hybrid propulsion systems.

Table 2 shows the characteristics of the aircraft and Tables 3–6 show the characteristics of the components of the propulsion system.

Table 2. Performance characteristics of Cessna 162 aircraft [35].

maximum seating capacity	2
maximum ramp weight	600 kg
usable fuel capacity	24 gal
maximum cruise speed	118 knt
rate of climb at sea level	898 ft/min
certified ceiling	14625 ft
engine type	Teledyne Continental O-200D
engine horsepower	100 bhp @ 2800 rpm

Table 3. Specifications of the gas turbines used in the two propulsion systems.

	hybrid	non-hybrid
rated power (kW)	63	75
nominal speed (rpm)	37000	34000
gas generator		
power turbine	34000	32000
compressor pressure ratio	3.2	3.5
turbine entry temperature (°C)	900	950
specific fuel consumption (kg/kW.h)	0.46 @ 63 kW	0.36 @ 75 kW

Table 4. Specifications of the motor and generator used in the hybrid propulsion system.

	generator	motor
type	IPMSM	IPMSM
pole number	8	8
three-phase winding	star	star
maximum continuous power (kW)	60	75
peak power (90 s) (kW)	90	130
maximum torque (N.m)	143	205
base speed (rpm)	6000	6000
nominal DC-link voltage (V)	350-400	350-400

Table 5. Specifications of the rectifier/inverter used in the hybrid propulsion system.

switch type	SiC MOSFET
switching frequency	10 kHz
PWM method	Third Harmonic Injection
number of Foster network nodes for each module	4
coolant	EGW 50/50

Table 6. Specifications of the HVB and LVB used in the hybrid propulsion system.

	HVB	LVB
configuration	105S1P	6S1P
capacity (Ah)	7.3	80
type	NCA-C	Pb-lead
energy (kWh)	2.75	1.03
peak power (kW)	15	1
nominal voltage (V)	400	12.8
minimum allowable cell voltage (V)	2.7	1.8
maximum allowable cell voltage (V)	4.2	2.4
permissible operating temperature range (°C)	15-45	15-45

Data from a real flight is used to examine the performance of the developed model in this paper. The flight was conducted by a Cessna 162 aircraft with registration number N40LF on December 11, 2021 and its route was from Ellington Airport in Houston to Texas Gulf Coast Regional Airport. The flight duration was 35 minutes. Flight information, including aircraft attitude, wind direction, ground speed, altitude, and ROCD (Rate Of Climb or Descent), has been received from the aircraft via ADS-B ground stations. This dataset was obtained from reference [36]. Figure 17 shows the aircraft flight path and Figure 18 shows the speed and altitude profiles.



Figure 17. Cessna 162 flight path from Houston Ellington (EFD) to Texas Gulf Coast Airport (LBX).

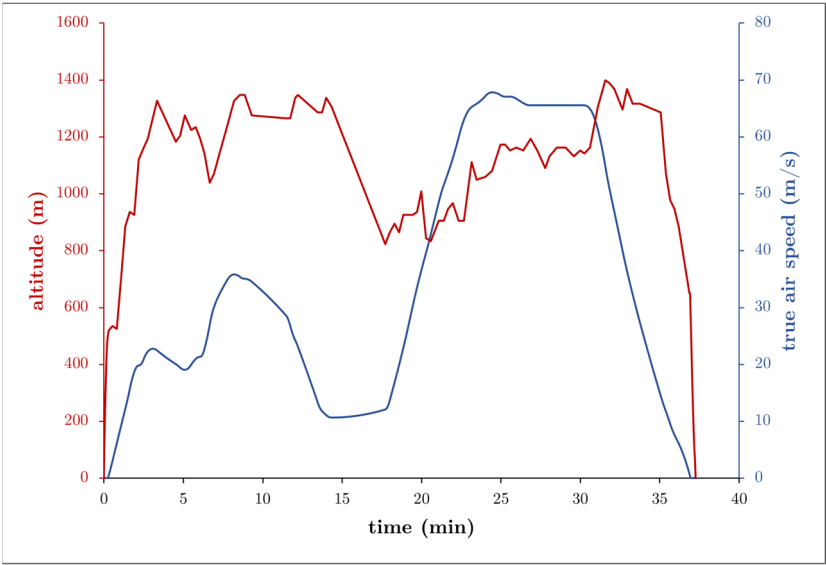
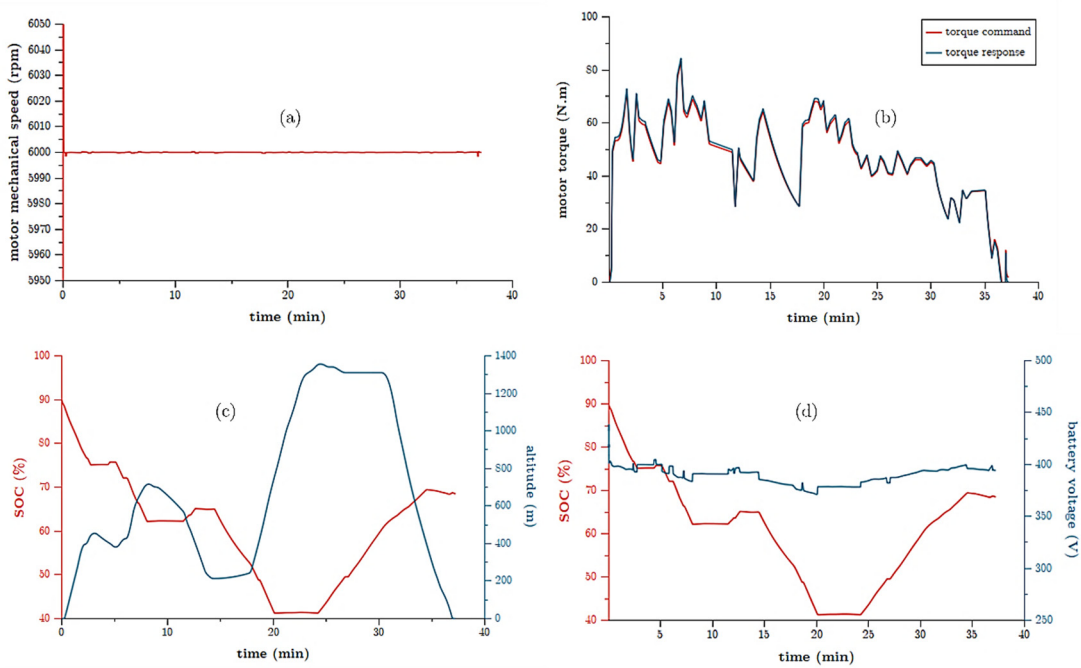


Figure 18. Mission profile.

5.1. The Performance of the Closed-Loop System

Using the values given in Table 7 for the supervisory controller parameters, the results in Figure 19 are obtained. It is observed that the motor vector controller has well maintained the engine speed at 6000 rpm (Figure 19a). Similarly, the torque requested by the propulsion system is also well responded to by the control system (Figure 19b).



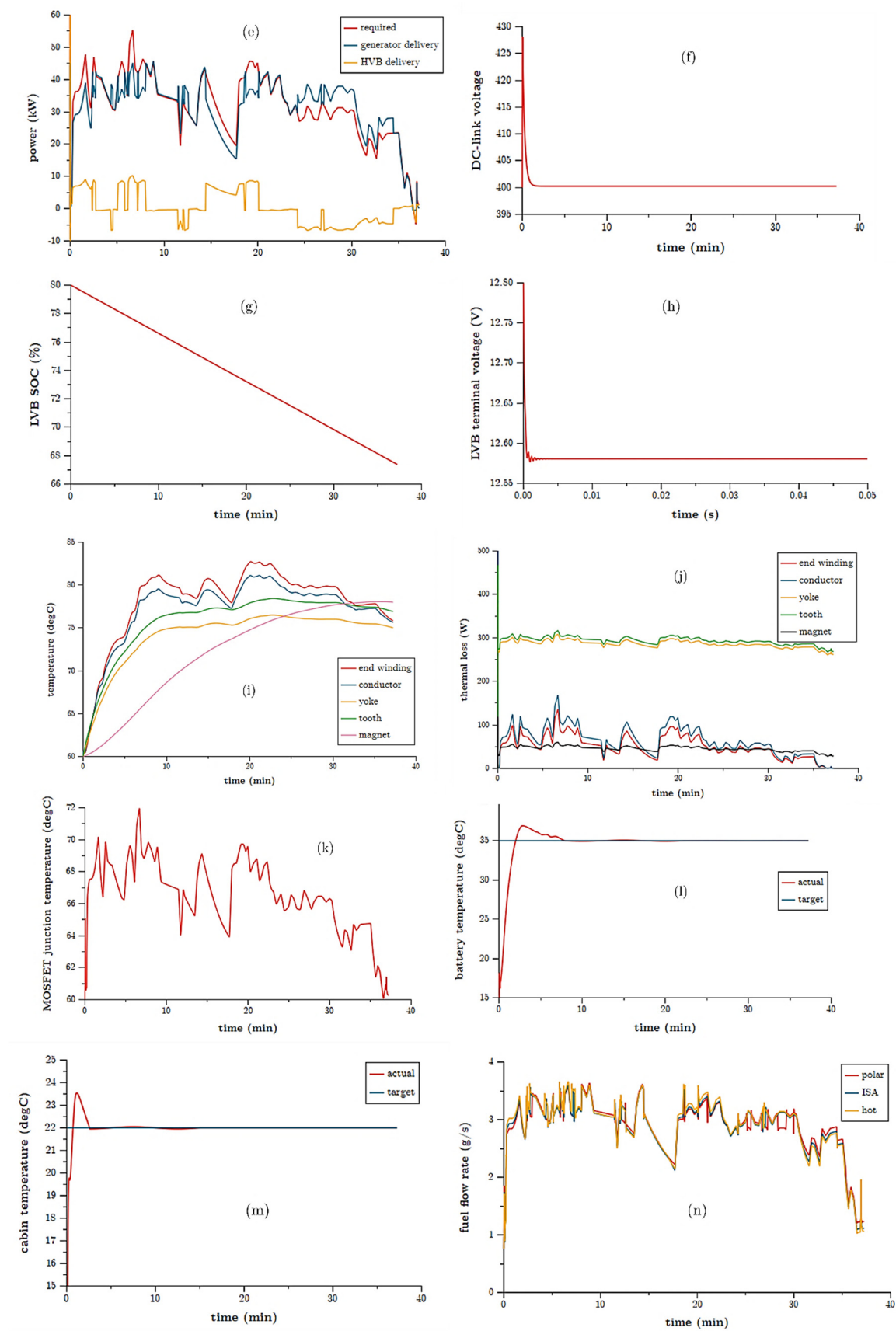


Figure 19. Results of closed-loop simulation of the propulsion system.

Figure 19c,d, while displaying the changes in altitude and HVB terminal voltage, clearly show that when HVB is not needed to provide the requested power to the motor, the supervisor commands

the generator to charge the battery. This power exchange is evident in Figure 19e. Also, Figure 19c,d show that during power-intensive phases of takeoff and climb, the supervisory controller puts the battery into the circuit. When the battery enters the circuit, 20% of the aircraft’s power requirement ($f_{bat} = 0.2$) is supplied by the battery. It is assumed that the initial SOC of the main battery was 90%. After the end of the flight, thanks to the HVB charging during the flight, the SOC of the battery has dropped only by 22.5%.

Table 7. The values of supervisory controller parameters.

h_{th}	400 m	P_{th}^{up}	40 kW
SOC_{min}	20%	P_{th}^{low}	30 kW
SOC_{max}	90%	ROC_{th}	0.5 m/s
f_{bat}	0.2	f_{ext}	0.2

Figure 19f,h show that the control of the two isolated and non-isolated choppers is also well done and the controllers have been able to bring a stable voltage level to the DC-link and LVB. In Figure 19h, it can be seen that the LVB voltage reached a stable level of 12.58 V in a very short time. The DC-link voltage also reached the desired voltage level within one minute.

Figures 19i to m show the results of the thermal analysis. As can be seen from Figure 19i, the highest temperature in the different parts of the machine belongs to the end winding which does not exceed 85 °C. Note that in order to achieve more accurate thermal results for the machine, the stator winding is divided into two parts: the main part, which is marked with conductor in Figure 19, and the end part, which is referred to as end winding. Also, the iron part of the machine is divided into three parts: yoke, teeth and rotor (iron core plus magnets). The losses of each part (Figure 19j) are a function of the current of the d and q axes, the speed of the machine, and the temperature of the different parts of the machine. It can be seen in this figure that the iron parts of the stator (yoke and teeth) have the highest heat losses. Figure 19k also clearly shows that the cooling system, the maximum junction temperature of the inverter MOSFET will be 72 °C. The performance of the HVAC system and its thermostat controller in bringing the battery and cabin temperatures to the desired values is also depicted in Figure 19l,m.

The point to note here is that in Figure 19l,k, the temperature starts at 60 °C. The reason for this is that the cooling system is designed in such a way that the base temperature of the coolant is 60 °C. As a further explanation, it should be said that EGW 50/50 has the highest heat transfer coefficient in the temperature range of 60 to 70 degrees Celsius. Therefore, in cooling circuits that use this coolant, it is usually an attempt to design the system in such a way that the coolant, after removing the heat taken from the hot source, reaches a base temperature that is in this temperature range so that it has the greatest potential for the next heat absorption cycle. It should be noted that in this simulation, the initial engine startup process is not considered and the simulation start time corresponds to the first moment after the startup process is completed and in this time period (startup process period), the coolant temperature is brought to the base temperature of 60 °C.

Figure 19n shows the fuel flow command signal issued by the HMU. In this figure, a comparison is made between the fuel flow consumed in three different temperature conditions: polar day, standard day (ISA) and hot day. The temperature value in terms of altitude for polar and hot days is obtained from the following equations [37]

$$T_{polar} = T_{ISA} + \begin{cases} -41.5 + 0.0115 * h & h \leq 1000 \\ -35.5 + 0.0055 * h & h > 1000' \end{cases} \tag{71}$$

$$T_{hot} = T_{ISA} + 24 - 0.0004 * h. \tag{72}$$

It is observed that in the first part of the flight, i.e., take-off and the initial phase of climb, at higher temperatures, more fuel is consumed, which is in line with our expectations, because as we know, as the temperature increases, the power required for take-off and climb of the aircraft increases. After passing through these stages, although the amount of air entering the engine decreases with

increasing temperature, the amount of drag force decreases as well, due to the decrease in air density. Often, the effect of reducing drag force is greater than the effect of reducing the air entering the engine, and as a result, usually in other phases of flight, the power required by the aircraft decreases with increasing temperature (Figure 20) and consequently, the fuel consumption rate also decreases. Figure 19n shows this well.

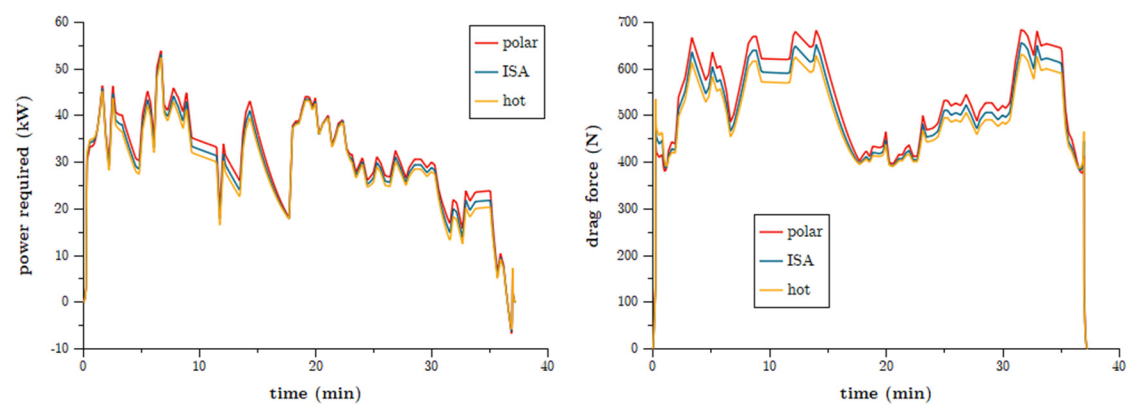


Figure 20. Variation of power required and drag force during the flight.

5.2. Comparison of Hybrid and Non-Hybrid Propulsion Systems

Figure 21 compare the fuel flow rate and fuel consumption for the hybrid and non-hybrid systems, under ISA condition. The non-hybrid system here refers to the propulsion system in which the power required by the propeller is directly supplied by the 75 kW gas turbine. Figure 21 shows that the hybrid system consumes about 17% less fuel than the non-hybrid system.

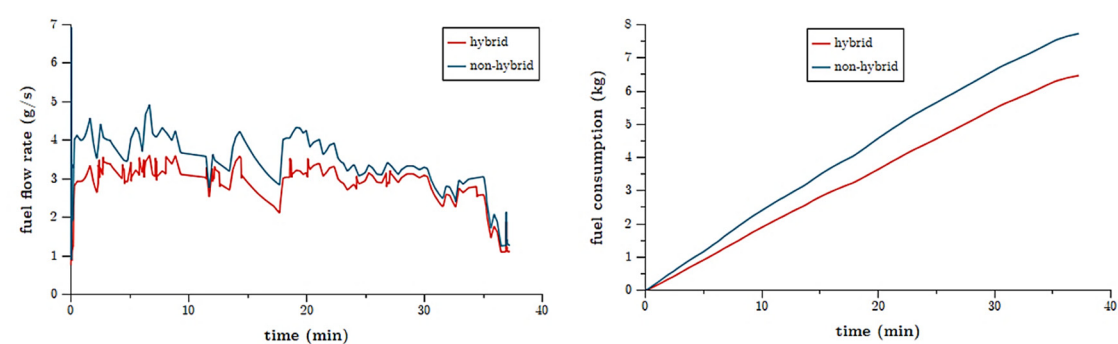


Figure 21. Comparison of fuel flow rate and fuel consumption for two hybrid and non-hybrid propulsion systems under ISA condition.

Table 8 presents the values of the fuel consumption of the two hybrid and non-hybrid propulsion systems. Table 9 too shows the amount of fuel saved when using the hybrid propulsion system. It is clear that as the temperature increases, the advantage of the hybrid propulsion system in terms of fuel consumption decreases. This issue can be examined from two aspects. The first is that for the system under consideration (aircraft and propulsion system), the power consumption of the HVAC system in polar condition is more than in hot condition and in hot conditions is more than in ISA condition. The second is the change in fuel consumption due to the change in the drag force and power demand of the propulsion system. As mentioned, in the initial phases of the flight, the highest fuel consumption is related to the hot condition and in the middle and final phases of the flight, the lowest fuel consumption is related to the hot condition. Therefore, from the perspective of the HVAC system power consumption, the polar condition has the highest potential for reducing fuel consumption due to the use of the hybrid propulsion system. On the other hand, from the perspective of the power demand and drag force, the polar condition can have the lowest or highest potential for

reducing fuel consumption depending on the flight phase. The sum of these effects causes the trend shown in Table 9 for the fuel consumption advantage in the system under consideration.

Table 8. Aircraft fuel consumption in grams at three temperature conditions.

	polar	standard (ISA)	hot
hybrid	6475	6467	6522
non-hybrid	7815	7732	7764

Table 9. The advantage of the hybrid propulsion system in terms of fuel consumption in grams.

	polar	standard (ISA)	hot
advantage of fuel consumption	1340	1265	1242

It should be noted that the fuel consumption of the hybrid system is completely dependent on the way the battery is used. In other words, by changing the parameters of the supervisory controller (Table 7), the amount of fuel consumption changes. Therefore, the advantage of a series hybrid propulsion system in terms of fuel consumption, depends on the thinking governing the power management system.

6. Conclusion

The research, focusing on series-configured hybrid propulsion systems, attempted to provide an efficient model for integrated power and thermal management in these systems. The aim of this work is to assess the feasibility of using hybrid propulsion systems as an alternative to present conventional propulsion systems. Accordingly, by considering various subsystems including gas turbine, generator, rectifier, high-voltage battery, low-voltage battery, DC-DC converters, inverter, and motor, a closed-loop model for integrated power and thermal management in series hybrid propulsion systems is presented. The aim of this model is to develop a virtual test bench and examine various performance characteristics of the propulsion system. Accurate thermal models are used for subsystems and the performance of high-level and low-level control systems is evaluated together and in interaction with each other. Considering all control systems allows for a better simulation and understanding of the closed-loop dynamics of the propulsion system.

The result of this study reveals that a smart integration of hybrid-electric propulsion system, taking into account their performance characteristics and environmental conditions, may reduce fuel consumption and increase the reliability of the propulsion system. In such a case, the load on the gas turbine, especially in the critical phases of takeoff and climb, is reduced and the battery is used to cover the power gap.

Supplementary Materials: The following supporting information can be downloaded at the website of this paper posted on Preprints.org.

Author Contributions: Conceptualization, M.N. and K.G.; methodology, software, validation, formal analysis, investigation, resources, data curation, visualization and writing—original draft preparation, M.N.; writing—review and editing, K.G and M.F.; supervision, K.G. and M.F. All authors have read and agreed to the published version of the manuscript.

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Abbreviations and Symbols

The following abbreviations and symbols are used in this manuscript:

α_T	Temperature coefficient of DC resistance of stator winding
γ	The ratio of specific heats
δ	Ratio of ambient pressure to standard pressure
η	Efficiency
θ	Ratio of ambient temperature to standard temperature, Angle of the d-axis with the a-axis of three-phase machine
λ	Flux linkage, Conductive heat transfer coefficient
ξ	Darcy friction factor
ρ	Density
τ	Torque, Time constant
ϕ	Heat flux
ω	Shaft speed
Ω	Burner loading, rotational speed
A	heat transfer area
A/C	Air conditioning
ah	Absolute humidity
amb	ambient
b_1	Mass fraction of bleed air for seal pressurisation
b_2	Mass fraction of bleed air for power turbine balance piston
b_3	Mass fraction of bleed air for cooling combustion chamber and gas generator turbine
B	Burner loading constant
BHXS	Battery heat exchange system
BOV	Blow-Off Valve
BTMS	Battery thermal management system
c_p	Specific heat at constant pressure
C	Capacitance, Battery capacity
d	Switch state (on or off)
D	Duty cycle
D_h	Hydraulic diameter
DP	Dynamic programming
ECMS	Equivalent consumption minimization strategy
ECS	Environmental control system
ECU	Electronic control unit
EEC	Electronic engine control
emf	electromotive force
EMS	Energy management system
f_{bat}	The share of the battery in providing the required power (fraction of P_d)
f_{ext}	Additional power that must be generated by the generator in order to charge the battery (fraction of P_d)
f_s	Switching frequency
FOC	Fuel-oil cooler
gr	Gear ratio
Gr	Grashof number
h	Specific enthalpy, Convective heat transfer coefficient, altitude
h_L	Convective heat transfer coefficient for liquid
HMU	Hydromechanical unit
HVAC	Heating, ventilation and air conditioning
HVB	High-voltage battery
i, I	Electrical current
ICE	Internal combustion engine
ISA	International standard atmosphere
ISG	Integrated starter generator
J	Mass moment of inertia of shaft
K_R	Resistance temperature correction factor
L	Inductance, Conduction heat transfer length

l_{ew}	end-winding leakage inductance
l_v	valve lifting
LVB	Low-voltage battery
LVDT	Linear Variable Differential Transformer
m	Mass
$\dot{m}$	Mass flow rate
M	Mutual inductance
MPC	Model predictive control
MTPA	Maximum torque per ampere
N	the number of gear teeth
Nu	Nusselt number
OCV	Open-circuit voltage
op_{BOV}	Opening of blow-off valve
p	Pressure, power
P	Machine pole number
P_d	Power demand
p_r	Reduced pressure
Pr	Prandtl number
PWM	Pulse width modulation
Q	Volumetric flow rate
q_s	Solar radiative flux
r	Gas constant (universal gas constant divided by molar mass)
R	Resistance
Ra	Rayleigh number
Re	Reynolds number
ROC	Rate of climb
R_s	Stator winding resistance per phase
s	Battery state (charge, discharge or none)
SCL	Stator copper loss
SCU	Safety control unit
SE	Specific energy
SH	Superheat
SOC	State of charge
SOF	State of function
SP	Specific power
t	Time
T	Temperature, Torque
$T_{abc \rightarrow dq0}$	Transformation matrix of the frame abc to the frame dq0
TMS	Thermal management system
TTR	Through-the-road
TXV	Thermal expansion valve
U	Voltage
v	Voltage
V	Combustion chamber volume, Voltage
v_c	Cabin volume
VCS	Vapor cycle system
x	Vapor quality
Z	Thermal impedance

Indices

The following indices and superscripts are used in this manuscript:

amb	ambient
b	battery, burner, bulb, blower
bat	battery
c	compressor
C	capacitance
cab	cabin

<i>cell</i>	cell of battery
<i>ch</i>	charge
<i>comp</i>	compressor
<i>conv</i>	convection
<i>cv</i>	convection (forced)
<i>d</i>	diode
<i>da</i>	dry air
<i>damp</i>	damping
<i>DC</i>	DC-link
<i>dch</i>	discharge
<i>des</i>	design
<i>dis</i>	discharge
<i>e</i>	evaporator
<i>em</i>	electromagnetic
<i>eq</i>	equivalent
<i>ext</i>	external side, extra (extra fraction of generator power generation to charge battery)
<i>f</i>	fuel, fan, fin
<i>g</i>	generator
<i>gen</i>	generator
<i>gg</i>	gas generator
<i>int</i>	internal side
<i>is</i>	isentropic
<i>junc</i>	junction
<i>lam</i>	laminar
<i>m</i>	motor
<i>M</i>	MOSFET
<i>mech</i>	mechanical
<i>Mr</i>	MOSFET in reverse mode
<i>nom</i>	nominal
<i>NcB</i>	Nucleate boiling
<i>ohm</i>	ohmic
<i>p</i>	propeller, pump
<i>pt</i>	power turbine
<i>rad</i>	radiation
<i>ref</i>	reference
<i>s</i>	stator
<i>std</i>	standard
<i>suc</i>	suction
<i>sw</i>	switching, swirler
<i>th</i>	thermal, threshold
<i>turb</i>	turbulent
<i>w</i>	winding, wall
<i>wv</i>	wet vapor

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