Abstract—Deploying electrical systems for aircraft propulsion has been identified as a potential solution for reducing the environmental impact of increasing air transport usage. However, the implementation of this system needs to be done at a suitable voltage and current combination. The aim of this work is to propose a clear procedure for deriving a suitable voltage and current for an electrical propulsion system based on the aircraft dimensions and thrust requirement. The approach presented considers feasibility and minimum mass as boundary and target, respectively. The results show that the fan configuration and thrust requirement directly influence the choice of optimal voltage and current. This is due to the varied impact on device sizes and overall propulsion system performance. Major drivers of the selected voltage and current are the loading coefficient, speed, and torque requirements of the fan. The knowledge of these is a requirement to arrive at an optimal voltage for the propulsion system.

Index Terms—Design, motor, sizing, speed, torque.

I. INTRODUCTION

The need to reduce the contribution of aviation activities to greenhouse emissions has become imperative due to the increasing demand in the sector. Significant efforts have been put in to develop a framework for this reduction together with other environmental impacts, such as noise. The NASA N + 3 goals [1], [2] set in 2008 and the European Flightpath 2050 [3] set in 2011 are examples. These frameworks highlight a holistic approach to solving these problems by viewing aviation activities as a single integrated system. A broad division into aircraft, airports, logistics, and passengers is made [4], and these require individual and synergistic optimization.

On the aircraft’s side, several concepts have been proposed to improve the performance in terms of efficiency (fuel burn per mission) and noise pollution [2], [5]. These rely on the hypothesis that optimum thermal and propulsive efficiencies can be achieved if the power generation and propulsion components of turbfans are decoupled [6].

Two major approaches for decoupling the fan and turbine are either mechanical or electrical transmission. The mechanical approach would require a gearbox between the turbine and the fan shaft. The electrical approach, on the other hand, involves the deployment of electromechanical power conversion devices (generators and motors) along with cables to transmit the power from the turbine to the fan shaft. Such an arrangement offers not only the benefits of running individual propulsors at optimized speed as with gearboxes but also frees up the design space by offering the flexibility in positioning them around the aircraft. This helps achieve noise reduction and more efficient thrust production through higher bypass ratios.

Despite these promises, several challenges need to be overcome in order to achieve such a propulsion system. These are related to size, large power management, and servicing. Therefore, the design aim is to achieve electric propulsion of aircraft at the safest, most economic, and power-dense cost.

A few aircraft have been proposed [7]–[12], which can broadly be categorized into the more-electric aircraft (MEA) and all-electric aircraft (AEA). These, respectively, propose to partly or totally replace the mechanical propulsion system with electrical systems. Several possible configurations abound with overlapping attributes of the two major approaches. Fig. 1 shows a classification of electrical powertrains proposed in [11].

To obtain a certain amount of thrust, the delivery of sufficient speed and torque to the fan shaft requires that the motor be operated at a corresponding voltage and current combination. The selection of such voltage and current is an important part of the electrical propulsion systems design. This is to ensure not only that the devices are optimally sized but also that they function normally while delivering the required amount of power. A gap is found in the literature for a clear procedure to determine what voltage level is optimal for a given configuration and aircraft.

This work presents a method for the selection of the optimal voltage and current, specific for a given electrical aircraft propulsion system. Only a turboelectric configuration using conventional (nonsuperconducting) devices and no batteries is considered. Component-level details are presented to reflect the influence of voltage and current magnitudes on the overall devices that constitute the propulsion system. Focus is given first to the overall mass since it is shown in [13] that the electrical propulsion systems’ specification has significantly less impact on efficiency than it has on mass.

II. BACKGROUND

Reference [14] presented a method for the calculation of safe voltage rating for an aircraft wiring system. This was only
for the insulated cables and did not consider the impact of the selected voltage on the sizing and performance of the other devices in the system. Similar studies aimed at the selection of the optimal voltage at the optimal propulsion system size were performed in [15] and [16]. The two studies covered parameter selection and design procedures based on superconducting technologies. However, a nonoverlapping voltage range was proposed for the same aircraft from both studies. A study of electrical components, sizing of electrical machines, and investigation of superconducting turboelectric aircraft was performed in [17]. The voltage selection was done for only the electrical machines. Reference [18] presented an approach for modeling superconducting motors using a modified conventional machine dynamic model. An assumed voltage of 28 V was used, and a detailed propeller to motor matching is performed, but other electrical components were not considered. The power flow technique was used in [19] to correctly predict bus voltages and currents at different torque–speed combinations. This was obtained for steady-state and transient-state scenarios involving a sudden change (increase) in torque demand. The approach only covers power circulation within a system with a known system voltage. No initial architecture voltage derivation or components’ sizing was considered. A fixed motor voltage of 1000 V was used in [13] for the investigation of a hybrid-electric propulsion system. It was shown that the system mass was optimized if the upstream components were adapted to variable voltages to eliminate the voltage regulation devices. Also, as the ratio of voltage to power increased, the total mass increased, while the efficiency decreased.

The reviewed voltage and current derivation approaches were based on components in isolation, superconducting technologies, and battery-powered aircraft. They did not cover nonsuperconducting turboelectric aircraft and also do not demonstrate the influence of aircraft dimensions and thrust requirements on the derived operational voltage and current.

III. VOLTAGE DERIVATION

The derivation of voltage and current presented here follows the motor demands since its performance has the most impact on the reliability of the propulsion system. This approach proposes that the voltage of the motor driving the fan to be imposed on the entire electrical system. Therefore, it begins with the knowledge of the speed and torque requirement and then a derivation of the motor flux linkage necessary to meet this demand. Fig. 2 shows the procedure for the modeling approach.

First, aircraft size and a mission design point are selected (usually, the point of maximum power demand) in order to properly estimate the propulsor size and power. Next, the motor sizing and dynamic performance and then other components’ sizing follow.

A. Ducted Fan Modeling

The modeling of the ducted fan is done to obtain the required power, rotations per minute (RPM), and the diameter of the fan to deliver the needed thrust. The derived power and RPM become the rating of the electrical motor to drive the fan. This affects the aerodynamic and electrical coupling and saves for the inclusion of a gearbox.
Fig. 2. Flowchart of the modeling procedure.

Fig. 3 shows the flowchart representation of the procedure. This is actualized by considering the thermodynamic and aerodynamic characteristics of the air through the duct in five typical stages: freestream, inlet, duct stream (before and after the fan), and nozzle.

The net thrust $F_n$ produced by the ducted fan is expressed as follows [20]:

$$F_n = \dot{m} (V_8 - V_0) + A_8 (p_8 - p_0)$$

where $\dot{m}$ is the mass flow rate of the air into the duct, $V_0$ and $V_8$ are the freestream and exit velocities of the air, and $A_8$ is the area of the nozzle, while $p_0$ and $p_8$ are the static pressures of the freestream air and nozzle, respectively.

The power required to produce the net thrust is obtained as

$$P = \dot{m} C_p \Delta T$$

where $C_p$ is the isobaric specific heat capacity of air and $\Delta T$ represents the difference between the total air temperature upstream and downstream of the fan. The procedure for deriving $\Delta T$ has been adopted from [20]. With the change in temperature across the fan derived, it is possible to obtain the fan RPM using

$$\text{RPM} = \sqrt{\frac{C_p \Delta T \times 60}{2\pi r_f}}$$

where $r_f$ is the radius of the fan, while Load is the fan stage loading coefficient. The mass of the fan is also required for the computation of the total inertia load on the motor shaft. This will enable the consideration of transient performance in the design process. Table I shows a comparison between estimated fan parameters and published values from [21] for the same aircraft.

From (3) and Table I, it could be deduced that for given maximum power and fixed fan diameter, operating the fan at a higher RPM would require a lowering of the stage loading coefficient, subject to tip speed limits, as shown in Fig. 4.

The graph in Fig. 4 represents seven different propulsion system configurations, in which the rated speed of the fan is varied between 1500 and 6000 RPM. It is generated, assuming isothermal operating condition across the fan. However, in reality, this implies either a change in the fan configuration, ambient conditions, thrust, or power. Therefore, the sizing of the motor would be required to cater for both the points of the maximum power and the selected maximum RPM if they do not coincide. The typical load profile of such a fan is presented in Fig. 5 for a given thrust target and maximum power.
Fig. 4. Variation of fan RPM at different loadings.

Fig. 5. Typical torque–speed curve for the ducted fan.

The profile graph considers the aviation requirement for the propulsion system to be capable of delivering 95% of its rated thrust within 5 s from an idle scenario [22]. This requirement also needs to be considered in matching the motor to the fan.

B. Electrical Machines Sizing

Electrical machines refer to motors that convert electrical to mechanical power and generators that convert mechanical to electrical power. These are the main enablers for electrical propulsion. Electric machines currently deployed in small aircrafts propulsion are permanent magnet synchronous machines (PMSMs) [23], [24]. These have also been proposed in [25] and [26] for future medium and large aircraft, due to their high specific power and minimal complexity, thus enabling them to be seamlessly deployed as motors or generators by the application of voltage or torque, respectively. Following this trend, a PMSM is considered for the sizing approach presented in this work.

Previous works [13], [25] on electrical machines design and sizing are predominantly analytic, requiring quantitative details at the start of design, such as the number of slots, rotor size, and some internal dimensions. The major challenge with such procedures is with the determination of the required number of conductors (turns) per slot to achieve the target rating. This is due to several possible approaches for winding the machines in pursuit of an optimal design. Accommodating the myriads of winding patterns is beyond the scope of this research, primarily because they are mostly required at the production stage. Other approaches [27] use specific power of the state-of-the-art (SOA) machines, which may not cater to individual design peculiarities, such as the ability to withstand transients and to deliver the required torque and speed. These requirements are also a product of voltage and current combination, which must be optimally selected as per design.

The design approach presented in this work is based on the torque capability, flux propagation, machine impedances, and the interactions, which occur between them in order to fulfill the required aerospace targets. These express the static (mass and volume) and dynamic (electromechanical) characteristics.

The flowchart in Fig. 6 shows the procedure for sizing the motor with an embedded voltage and current derivation. The approach also includes temperature consideration and a mass optimization loop.

The static characteristics of the motor are estimated based on the close relationship between the required power, speed, and volume, comprising the air-gap diameter. For internal rotor machines, this diameter is referred to as the rotor diameter [17], [28], while, for external rotor machines, it is referred to as the stator outer diameter [29]. In this research, external rotor machines are considered since the design principles presented are inferred from earlier presented machines proposed for aerospace applications [31], [32], which has the same topologies.

With known power and speed, the machine air-gap volume $v_{AG}$ can be derived using

$$v_{AG} = \frac{P_m}{\omega_m K_w B_f A_L \sqrt{2}} \quad (4)$$

where $K_w$ is the fundamental winding factor, $B_f$ is the air-gap flux density (T), $A_L$ is the armature loading (A/m), and $P_m$ and $\omega_m$ are the required motor power (W) and speed (rad/s), respectively. The importance of deriving the
air-gap volume of the machine is because of its influence on
the flux propagation. The usual aim of the optimized motor
design is to fit as much flux required to deliver a given power
in the smallest possible air gap. The air-gap diameter of the
machine $D_{AG}$ can be obtained using

$$D_{AG} = \frac{4D_{AG}R_{DL}}{\pi}.$$  \hspace{1cm} (5)

The machine diameter $D_M$ is given by

$$D_M = R_{MAG}D_{AG}.$$  \hspace{1cm} (6)

The air-gap area $A_{AG}$ is given by the following equation [32]:

$$A_{AG} = \pi D_{AG}L.$$  \hspace{1cm} (7)

The total air-gap flux $\varphi$ and flux linkage per phase $\lambda$ can be obtained using the following equations, respectively:

$$\varphi = B_e \times A_{AG}$$  \hspace{1cm} (8)

$$\lambda = \frac{\varphi}{N_{ph}k_{ph}}$$  \hspace{1cm} (9)

where $R_{DL}$ is the air-gap diameter-to-length ratio, $R_{MAG}$ is the machine-to-air-gap diameter ratio, $L$ is the machine length, and $N_{ph}$ and $k_{ph}$ are the number of phase and phase connection factor, respectively. $k_{ph}$ is 1 for a delta-connected winding and $\sqrt{3}$ for a wye-connected winding.

The voltage of the motor and, hence, of the electrical
network can be obtained using the following induced voltage equation:

$$V_{Des} = \frac{\omega_e \lambda}{\sqrt{2}}.$$  \hspace{1cm} (10)

where $\omega_e$ is the electrical speed of the machine (rad/s) given as

$$\omega_e = 2\pi \left( \frac{\text{RPM} \times p_s}{120} \right).$$  \hspace{1cm} (11)

The bracketed term represents the motor rated frequency, where $p_s$ is the stator pole count. It is important to state also that the derived voltage is the rms value for a motor with delta internal winding connection. The obtained value should be multiplied by $\sqrt{3}$ for a wye-connected winding.

With known voltage, current $I$ can be obtained using the electrical power equation as

$$I = \frac{P_m}{V_{Des}}.$$  \hspace{1cm} (12)

In order to establish that the designed machine can deliver the required torque and speed, a matching of the static and dynamic characteristics is required. Equations (13)–(22) express the dynamic characteristics of the PMSM. The orthogonal components of the stator currents are represented by the direct axis current $I_d$ and quadrature axis current $I_q$. These are given by the following equations, respectively:

$$I_d = \int \left( -R_s I_{d,i-1} + \left( \frac{\omega_0 p_s}{2} \right) L_q I_{q,i-1} - V_d \right) dt$$  \hspace{1cm} (13)

$$I_q = \int \left( -R_s I_{q,i-1} + \left( \frac{\omega_0 p_s}{2} \right) L_d I_{d,i-1} + V_q \right) dt.$$  \hspace{1cm} (14)

where $V_d$ and $V_q$ are the $d$- and $q$-axis voltages obtained through an ABC-DQ transformation, $R_s$ is the stator resistance, and $L_q$ and $L_d$ are the $d$- and $q$-axis inductances.

The inductance $L_m$ of the machine can be obtained using

$$L_m = \frac{\sin \left( \frac{\lambda}{180} \right) \lambda}{I}.$$  \hspace{1cm} (15)

where $\lambda$ is the phase angle (in degrees). The $d$- and $q$-axis inductances $L_q$ and $L_d$ are obtained with the following equations, respectively:

$$L_q = \left( \frac{2}{3} \right) L_m$$  \hspace{1cm} (16)

$$L_d = \sqrt{L_m^2 - L_q^2}.$$  \hspace{1cm} (17)

The synchronous reactance $X_s$ and resistance $R_s$ are obtained using the following equations, respectively:

$$X_s = L_m \omega_e$$  \hspace{1cm} (18)

$$R_s = \frac{X_s}{r_{X/R}}$$  \hspace{1cm} (19)

where $r_{X/R}$ is the ratio of synchronous reactance to resistance.

The electromagnetic torque $T_e$ that does the electrical to mechanical power conversion, and vice versa, is given by

$$T_e = \frac{3}{2} \left[ \left( \frac{p_s}{2} \right) (\lambda I_q) + (L_d - L_q)(I_q I_d) \right]$$  \hspace{1cm} (20)

while the machine mechanical speed is given by

$$\omega_m = \int \left( \frac{T_e - T_L - B \omega_{m,j-1}}{J} \right) dt$$  \hspace{1cm} (21)

where $T_L$ is the load torque, $\omega_{m,j}$ is the instantaneous speed, $J$ is the total inertia on the rotor, and $B$ is the coefficient of viscous damping in the shaft bearings, while $\theta$ is the angular displacement of the rotor from a reference point of rest and is expressed as

$$\theta = \left( \frac{p_s}{2} \right) \int (\omega_m) dt.$$  \hspace{1cm} (22)

As already mentioned, a PMSM can also operate as a generator with the application of torque at the shaft rather than the voltage at the cable terminals. The generator modeling follows a similar procedure, using the same equations as motor modeling. However, because the motor voltage is imposed on all network components, the static equations are applied in reverse order from (11)–(4) in order to obtain the volume and then the mass of the generator. The inner diameter is selected as the optimization handle since the voltage is already known. The RPM utilized for obtaining $\omega_e$ in this case is the turbine shaft speed driving the generator. Also, due to the reversal of power flow compared with the motor, (21) is rewritten as

$$\omega_m = \int \left( \frac{T_e + T_L - B \omega_{m,j-1}}{J} \right) dt.$$  \hspace{1cm} (23)

The inertia derivation for the generator only considers the rotor mass and dimensions.

The assumed ratios in (5), (6), and (19) can be derived using a scaling approach from existing machines, preferably the SOA. Fig. 7 shows the notional cross section of the considered machine. The concentric annuli from $i$ to $n$, respectively,
Fig. 7. Electrical machine cross section.

map to the machine housing, rotor shell, permanent magnets, windings, stator yoke, heatsink, ground cylinder, and shaft.

The mass $m_m$ of the motor can be derived as a sum of the masses of all the constituent parts as

$$m_m = (\pi r_n^2 L \rho_n) + (\pi r_i^2 \rho_i + \frac{1}{1} + \frac{1}{1} \rho_{t+1} L) + \left(\sum_{i=1}^{n-1} \pi (r_i^2 - r_{i+1}^2) \rho_i \right)$$

where the first term represents the shaft mass, the second term represents the endplate mass, and the third term represents the sum of the masses of the concentric annuli. $r$ in each case refers to the outer radius of the referred annulus, and $\rho$ represents the density of the material constituting the referred annulus, while $L$ is the length of the machine. A hollow factor of 0.35 is considered for the heatsink layer to account for the air channel included.

For such external rotor topology, three main mechanical challenges are identified; the rotor radial expansion, installation feasibility, and load transmission. In [31], the structural analysis and installation feasibility for such motor topology were performed. It was proposed to be installed by fastening the base structure to the aircraft for reliable transmission of the thrust to the aircraft. In order to achieve radial expansion integrity, the motor housing thickness should be selected with due consideration of the tip speed at the magnet external radius $V_{tip}$ [33] and allowable temperature change $\Delta T$ of the permanent magnets beneath. This can be obtained using

$$T_H = \frac{V_{up} \rho_m}{2r_o \Delta T \alpha E} \left(\frac{r_o^2}{r_i^2} - \frac{r_i^2}{r_o^2}\right)$$

where $\rho_m$ and $\alpha$ are the density and linear expansion coefficient of the magnetic material, respectively, $r_o$ and $r_i$ are, respectively, the outer and inner radii of the annulus comprising the magnets, and $E$ is Young’s modulus of the motor housing material.

To ensure that the machines are operating within their maximum allowable temperature, thermal consideration is also performed. This depends on the efficiency and heat capacity of the machine components. In [33], it was stated that the stator winding has the most susceptibility to heat due to being enclosed while directly handling the machine’s electrical power. The stator maximum temperature is the sum of the ambient temperature and temperature change due to the losses given by

$$T_M = T_{Amb} + P_L R_{th_W}$$

where $T_{Amb}$ is the ambient temperature, $R_{th_W}$ is the thermal resistance of the winding, and $P_L$ is the power loss in the winding given by

$$P_L = I^2 R_w + V I_0$$

where $I$ is the machine current, $R_w$ is the winding resistance, $V$ is the machine voltage, and $I_0$ is the machine no load current. Assuming an air-core stator, the thermal resistance of the winding layer is given by

$$R_{th_W} = \frac{\ln \left(\frac{r_{AG}}{r_S}\right)}{2\pi L k_W}$$

where $r_{AG}$ is the air-gap radius, $r_S$ is the radius of the stator, $L$ is the active length of the machine, and $k_W$ is the thermal conductivity of the winding. The cooling method for such motor topology has been adopted from [33], where forced air convection has been deployed with the inclusion of a heatsink annulus beneath the stator. Fig. 8 shows a comparison of the values obtained using the presented approach with published data in [30] for a machine proposed for aerospace applications.

C. Power Electronics Sizing

Power electronics devices proposed for aerospace propulsion include rectifiers, inverters, voltage regulators, and circuit protection devices. They are comprised of semiconductor-based switches, integrated circuits, inductors, and capacitors. The main design principles utilized in this article have been adopted from already validated device models in [34] and [35]. These models are based on the sizing and performance of the devices on six functional components: the switches, capacitors, gate drivers, controllers, bus bars, and support structures.
Switch adopted in this work is the more efficient silicon carbide metal–oxide–semiconductor field-effect transistor (SiC MOSFET). Their selection is reliant mostly on the current and voltage handling limits. SOA is considered to reflect the improved power density of the currently available switches. Hence, the mass of each switch is obtained from the manufacturer database. However, the efficiency of these switches depends on the operating currents and voltages. This is a function of the switching device power loss, given by

\[ P_S = P_T + P_D \]  

(29)

where \( P_T \) and \( P_D \) are the switching transistor and diode losses, respectively. These are given by the following equations [36], respectively:

\[ P_T = I_{rms}^2 R_{DS,ON} + f_{sw} E_{sw} \]  

(30)

\[ P_D = I_{f,av} V_{F0} + I_{rms}^2 R_F + f_{sw} E_{Rec} \]  

(31)

where \( I_{rms} \) is the rms value of the input current to the device, \( R_{DS,ON} \) is the drain–source ON-state resistance of the switch, \( I_{f,av} \) is the average value of the current through the diode, \( V_{F0} \) is the diode ON-state zero-current voltage, and \( R_F \) is the resistance of the diode. \( f_{sw} \) is the switching frequency, while \( E_{sw} \) and \( E_{Rec} \) are the switching and recovery energies in the transistor and diode, respectively. \( E_{Rec} \) can be obtained using

\[ E_{Rec} = \frac{(Q_{rr} \times V_D)}{4} \]  

(32)

where \( Q_{rr} \) is the reverse recovery charge and \( V_D \) is the supply voltage. Table II shows the design parameters of the switch adopted in this work.

Capacitors modeled here are the dc-link capacitors. In order to obtain the appropriate size, the capacitance required to filter a given voltage ripple value is derived using the following equation [16]:

\[ C = \frac{I_{dc}}{2 f_{ac} \Delta V_{dc}} \]  

(33)

where \( I_{dc} \) is the dc current, \( \Delta V_{dc} \) is the dc voltage ripple, and \( f_{ac} \) is the supply frequency.

The total power loss of the capacitor is given by

\[ P_C = P_D + P_R \]  

(34)

where \( P_D \) is the dielectric power loss and \( P_R \) is the resistive power loss. These are given by the following equations [37], respectively:

\[ P_D = (0.1 V_{dc})^2 \pi f_0 C \tan \delta_0 \]  

(35)

\[ P_R = I^2 \times R_S \]  

(36)

where \( f_0 \) is the fundamental frequency and \( \tan \delta_0 \) (2e-4) is the dielectric dissipation factor. Table III shows the operational parameters of the capacitor adopted for this work.

The other components that constitute the power electronics are sized using a mass scaling factor \( m_k \). This is multiplied by the switch count since these components are auxiliaries of the switching process. Table IV shows data for the four converters considered for this derivation. A low-power bench test converter is added to the initially referenced converters. The New mass refers to the converter mass excluding the capacitor and all cooling accessories since they are sized separately. The scaling factor obtained for each converter expresses the ratio of the New mass to the total switch mass. A value of 9.1 is obtained as the mass scaling factor for all switching devices, which includes the rectifier, inverter, and protection devices. These are modeled next.

1) Rectifier Sizing: The rectifier acts to convert the alternating current (ac) from the generator to direct current (dc) to enable it to be controlled. Modern high-power rectifier circuits are often implemented using solid-state switches. Fig. 9 shows a typical rectifier configuration.

The inports 1–3 are for the three phases of ac voltage output from the generator. The outports 4 and 5 are the positive and negative dc terminals, respectively. The filter...
capacitor smooths the output of the switches to improve the power quality. The rectification is performed based on a preset design value (system design voltage) by sending switching control signals to the respective switches using signaling ports A1–C2. Other supporting components include the heatsinks, other cooling devices, and mounting accessories.

The total mass of the rectifier is derived using

$$m_{Rec} = m_s C_{S,Rec} m_k + m_C C_{C,Rec}$$

where $$m_s$$ and $$m_C$$ are the unit masses of the switching device and the capacitor, respectively, while $$C_{S,Rec}$$ and $$C_{C,Rec}$$ are their respective total counts. These are derived using the following equations, respectively:

$$C_{S,Rec} = \frac{V_{Des}}{V_{max,S}} \times \frac{I_{Des}}{I_{max,S}} \times 6$$

(38)

$$C_{C,Rec} = \frac{V_{Des}}{V_{max,C}} \times \frac{I_{Des}}{I_{max,C}}$$

(39)

where $$m_k$$ is a multiplier to account for the mass of the circuit board and support structures and, hence, is the switch mass scaling factor, $$V_{Des}$$ and $$I_{Des}$$ are the system design voltage and currents, respectively, $$V_{max,S}$$ and $$I_{max,S}$$ are, respectively, the voltage and current handling limit of the switch, while $$V_{max,C}$$ and $$I_{max,C}$$ are, respectively, the voltage and current handling limit of the capacitor. $$I_{Des}$$ here refers to the maximum surge current during motor transients. This could be obtained by putting (20) into (21) and solving for $$I_t$$, considering the transient duration.

The efficiency of the rectifier is obtained using

$$\eta_{Rec} = 1 - \left( \frac{P_s 2C_s \frac{m_k}{6} + P_C C_{C,Rec}}{P_{in}} \right)$$

(40)

where $$P_{in}$$, $$P_s$$, and $$P_C$$ are the input power and power loss of one switch and capacitor, respectively. In normal operation, only two switching channels are delivering power at a time and, hence, contribute to the instantaneous loss of efficiency of the device.

2) Inverter Sizing: The inverter performs the speed control of the motor using one of several available techniques. However, common techniques exploit the relationship between current, motor inductance, and flux by means of pulsedwidth modulation (PWM). Fig. 10 shows a typical inverter configuration. The imports 1 and 2 connect to the dc source, while outports 1–3 are connected to the three phases of the motor. Ports A1–C3 are the PWM control signals’ channel. The mass and switch count of the inverter are derived by the following equations, respectively:

$$m_{Inv} = m_s C_{S,Inv} m_k$$

(41)

$$C_{S,Inv} = \left[ \frac{V_{Des}}{V_{max,S}} \right] \times \left[ \frac{I_{Des}}{I_{max,S}} \right] \times 6.$$  

(42)

The efficiency $$\eta_{Inv}$$ of the inverter is given by

$$\eta_{inv} = \left( 1 - \frac{P_s 2C_s \frac{m_k}{6}}{P_{in}} \right)$$

(43)

where $$C_{S,Inv}$$ is the switch count comprising the inverter, while $$P_s$$ is the power loss of a single switch and $$m_k$$ is the switch mass scaling factor. $$I_{Des}$$ here refers to either the maximum surge current for the motor transient or the set fault current between the motor and the inverter. This is because the inverter drive is proposed to perform both the motor speed control and fault protection functions based on experiments conducted in [36].

3) Protection Sizing: The protection device helps to isolate its principal device from faults occurring upstream or downstream of it. This can be achieved by limiting the magnitude of the current passing through or by completely blocking off the flow resulting in device unavailability. Two types of protection devices are considered: the ac circuit and dc circuit protection devices. The total protection system mass would depend on how many of such devices are deployed. This should vary by aircraft configuration. The mass of the protection system $$m_P$$ deployed per channel at a given point is derived by

$$m_P = m_V C_V + m_s C_S m_k$$

(44)

where $$m_V$$ is the mass of the varistor, $$m_k$$ is the switch mass scaling factor, and $$C_V$$ is the varistor count, given by

$$C_V = \left[ \frac{V_{Fault}}{V_{max,V}} \right] \times \left[ \frac{I_{Fault}}{I_{max,V}} \right]$$

(45)

where $$V_{max,V}$$ and $$I_{max,V}$$ are the varistor surge voltage and current arresting capability, respectively. $$C_S$$ is the switch count comprising the protection device, given by

$$C_S = \left[ \frac{V_{Fault}}{V_{max,S}} \right] \times \left[ \frac{I_{Fault}}{I_{max,S}} \right] \times 2.$$  

(46)
$I_{\text{Fault}}$ here also considers the maximum surge current or the set fault current whichever gives the higher number since switches have different current ratings for normal, surge, and short circuit operation. The $RC$ snubber circuit is not sized separately since it is accounted as part of the mass scaling factor $m_k$ based on the design presented in [35] and [38]. Based on the same principle, it is seen that the value of fault voltage $V_{\text{Fault}}$ can be kept at 1.2 times the rated dc equivalent. The sized protection system is per channel. Therefore, when deployed on a dc line, only one is required. However, for a three-phase line, all three channels have one device each since each channel carries both positive and negative voltages in an alternating manner.

D. Transmission Sizing

The transmission system is sized in terms of the length, operating temperature limit, and connection type. For dc and ac segments of the transmission lines, the interconnection would require two cables and three cables, respectively. The cable comprises a conductor core, an insulation layer, and an exterior sheath layer. The minimum radius of the conductor is expressed as

$$r_C = \sqrt{\frac{P_{\text{Rated}}(1 - \eta_C)}{(T_{\text{max}} - T_{\text{Amb}}) \rho_C C_{p,C}}}$$

(47)

where $P_{\text{Rated}}$ is the rated power through that segment, $T_{\text{max}}$ is the maximum allowable transmission cable temperature, $\eta_C$ is the efficiency of the transmission cable, and $\rho_C$ and $C_{p,C}$ are the density and specific heat capacity of the transmission core material, respectively.

With a known core radius, the mass of the transmission cable of a given length can be derived using

$$m_T = v_C \rho_C + v_{\text{ins}} \rho_{\text{ins}} + v_{sh} \rho_{sh}$$

(48)

where $v_C$ and $\rho_C$ are the volume and density of the conductor, respectively, $v_{\text{ins}}$ and $\rho_{\text{ins}}$ are those of the insulation material, and $v_{sh}$ and $\rho_{sh}$ are those of the sheath material.

The efficiency of the cable is obtained by

$$\eta_C = \frac{I_C V_C - I_C^2 (R_C + X_C)}{I_C V_C}$$

(49)

where $V_C$ and $I_C$ are, respectively, the transmission voltage and current, while $R_C$ and $X_C$ are the resistance and inductive reactance of the transmission line, respectively. $X_C$ is ignored for dc segments of the transmission line.

E. Cooling Sizing

The cooling sizing of the power electronics is based on the power loss and upper temperature limit of the cooled devices. The upper temperature limit of the device is given by

$$T_J = T_{\text{Amb}} + P_L (R_{JS,D} + R_{CS})$$

(50)

where $T_{\text{Amb}}$ is the ambient temperature, $P_L$ and $R_{JS,D}$ are, respectively, the maximum power loss and junction-to-sink thermal resistance of the cooled device, and $R_{CS}$ is the thermal resistance of the cooling system. Apparently, the most adjustable parameter with which to keep the value of $T_J$ below its maximum is $R_{CS}$. Therefore, the ultimate goal of the cooling system sizing is to achieve a low $R_{CS}$ value with a minimum mass penalty. $R_{CS}$ can be estimated using

$$R_{CS} = \left( \frac{1}{hA_E} + \frac{1}{\rho_f C_{p,f} V} \right)$$

(51)

where $h$ and $A_E$ are, respectively, the heat transfer coefficient and exposed surface area of the cooling plate attached to the cooled device, while $\rho_f$, $C_{p,f}$, and $V$ are, respectively, the density, specific heat capacity, and volumetric flow rate of the cooling fluid flowing across the cooling plate. The mass of the cooling system is given by

$$m_C = m_{TP} + m_{TA}$$

(52)

where $m_{TP}$ is the total mass of the cooling plate, $m_{TA}$ is the total mass of the cooling fluid, and $m_{TA}$ is the total mass of the accessories required for implementing the cooling system. The cooling method adopted in this work is an extension of methods earlier presented in [39]. However, this utilizes the ducted fan’s ram air as the cooling fluid, taking advantage of its force velocity while avoiding extra cooling fluid and accessory mass. Hence, a finned cooling plate is deployed, whose heat transfer coefficient is derived using methods presented in [40].

IV. RESULTS AND DISCUSSION

This section presents and discusses the results of the proposed approach. These have been implemented using a tool developed on MATLAB Simulink. As already highlighted, the derivation of optimal voltage needs to be tailored to an aircraft and design point(s). The propulsive fuselage airframe concept shown in Fig. 11 (adapted) is selected because of the availability of enough background literature.

Fig. 12 shows the layout of electrical components for the proposed aircraft concept. The comprising devices are the
Fig. 13. Motor current and voltage versus RPM.

Fig. 14. Total system mass and efficiency.

generator (GEN), transmission (TXN), rectifier (REC), circuit breaker (CB), inverter (INV), and motor (MOT). The power flow is in the direction of the arrows from the wing-mounted generators through the bus bar to the motor driving the tail fan.

Fig. 13 shows the obtained voltages and currents for the range of RPM shown in Fig. 4. It is observable that the derived range of voltage is above the safe limit of breakdown voltage (327 V) due to the gap and surrounding air pressure. However, the single conductor transmission cable modeled earlier offers the opportunity to install the cables considering safe gaps between them. Also, the voltage is seen to almost double over the range of RPM investigated. The inverse variation of current with increasing RPM occurs at a slightly lower rate compared with voltage.

The total system mass and efficiencies at these voltages and currents are presented in relation to the motor speeds in Fig. 14. From the graph, it could be seen that the point of optimal mass is at 1500 RPM. This does not coincide with the point of optimal efficiency at 6000 RPM. However, considering boundaries of tip speed, stage loading coefficient, and the specific fuel consumption of the turbines, a suitable RPM and the corresponding voltage and current could be selected as optimal.

Fig. 15 shows the contribution of the system components to the total system mass. The masses of the devices that appear as twins in Fig. 12 have been presented as a sum of the two, with the assumption that they each handle equal power always. The main mass contributors are the electric machines due to their high torque requirement, as a direct drive without gearboxes has been considered. While the motor’s mass varies inversely with its speed, the generator’s mass varied directly. The generator’s speed is held at 4200 RPM, governed by the turbine shaft speed. Therefore, with increasing system voltage, a larger air-gap area is required, leading to a bigger and heavier machine. Other devices exhibit a direct proportionality in their mass variation with increasing motor RPM. The dc transmission segment (TXN2) also has a noticeable mass contribution due to its long span of 74 m total compared with the ac segments (TXN1 and TXN3), which is a total of 6 m. It is also noticeable that at lower RPMs, the systems’ mass tracks the motor and cooling mass, while the power electronics devices begin to have a more significant contribution as the RPM increases.

Fig. 16 shows the percentage contribution of each component to the overall systems’ mass. The direct proportionality between power electronics mass and increasing RPM is due to a significant increase in switch counts to cater for the transient currents. The transient current rise is due to the motor
Fig. 16. Components’ percentage mass contribution.

Fig. 17. Components’ efficiency profile.

needing the capability to accelerate the fan to near maximum speed within 5 s. The surge in current to meet this regulatory requirement rises significantly as rated fan speed increases.

Fig. 17 shows the efficiency of the devices at the various motor speed configurations. The power electronics devices exhibit the highest efficiencies, while the electric machines and ac transmission segments contribute the most losses. The dc transmission segment has lower losses despite a longer span due to the absence of alternating components. The cooling efficiency represents the efficiency of the fins on the cooling plate and, unlike other components’ efficiency, only considers the power dissipated into it and not the total propulsion system power.

Fig. 18 shows the maximum temperature change of the machines for the various architectures. The trend for the motor is seen to rise with increasing speed due to lesser efficiency and heat capacity due to decreasing size. This trend reverses for the generator due to increasing size.

Fig. 19 shows a comparison of published values of components’ mass and efficiency in [27]. The point of optimal mass is selected to match the sizing approach in the referenced literature. This is at 1500 RPM with corresponding voltage and current of 549 (V) and 4746 (A), respectively. The derived
masses are seen to be in a close range for a majority of the devices except for the motor and cooling system mass. In terms of efficiency, only the transmission system shows the devices except for the motor and cooling system mass. Further investigation would be required to see how different motor topologies impact the derived voltage, size, and performance of the propulsion system. The extent of adaptability of the proposed methods to superconducting and battery-powered aircraft has not been investigated. Also, a procedure to determine the range of stage loading coefficient of fans, proposed for electrical propulsion, is required. This would consider blade configurations and will further narrow the voltage selection process.

V. CONCLUSION

The procedure for selecting the optimal voltage for a turboelectric propulsion system has been presented. This is based on the relationship between fan size and RPM to meet the thrust requirement and the motor voltage to deliver the RPM. The obtained voltage level for the motor is imposed on all devices that constitute the propulsion system. The presented approach shows a close relationship between the fan dimensions, thrust requirement, and the system operational voltage. Major determinants of the selected voltage are the stage loading coefficient, speed, and torque requirements of the fan. At low speeds and voltages, the propulsion system mass was driven more by the electric machines and cooling system, while, at high speed and voltages, the power electronics devices had more contribution.

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