

Concept and Preliminary Sizing of a Dual DC-Bus Homopolar Generator Using Diode Rectifiers

A. Vannini, L. Papini, A. Marfoli, C. Gerada, P. Bolognesi

Abstract—Synchronous Homopolar Generators represent an attractive solution for the future of power generation in propulsive systems based on hybrid turbo-electric drivetrains. In fact they feature both a fully controllable excitation, similar to wound-field synchronous machines, and a very robust rotor like switched reluctance machines. The above features make this machine topology a promising alternative also to PM synchronous generators, even when DC power buses have to be supplied via diode rectifiers. The preliminary electromagnetic sizing of a 15 kW @ 15 krpm homopolar synchronous generator is proposed as a concept demonstrator, and then validated via 3-D Finite-Element analysis. The generation system is designed on purpose to supply two independent 600 V DC power buses for improved redundancy, by using 4 winding sections supplying 2 three-phase diode rectifiers connected in series per each bus to reduce the voltage ripple.

Index Terms—Diode-Rectifier, Electric Propulsion, Generator, Homopolar Synchronous Machine, 3-D Finite-Element

I. INTRODUCTION

Due to the regulations and the net-zero emission target necessary to limit the global temperature rise and the impact of climate change, the propulsive systems for transportation are continuously evolving towards electrified solutions. Turbo- and hybrid-electric architectures have been widely proposed for both aeronautical [1] and naval propulsion drivetrain systems [2]. The on-board electric power generation system has then to meet the huge increase in power demand due to propulsion, while still satisfying stringent reliability requirements and limited installation volumes. In this scenario, the use of primary medium-voltage DC power distribution feeders is envisioned due to the superior performance and lower weight [3], [4].

The Wound-Field Synchronous Generators (WFSGs) represent a mature, well-proven and reliable technology, featuring a complete field controllability, which also permits a reliable de-excitation in case of failure [5]. The field winding is hosted in the rotor, making it rather bulky and prone to insulation failure especially in case of high speed operation, such as in case of aerospace applications [6]. When the popular brushless excitation system is used [5], the rotating exciter armature and the rectifier represent further weak elements. For this reason, alternative machine topologies have been investigated as potential competitors to the WFSGs for the

on-board power generation. Permanent Magnet Generators (PMGs) [7] are often considered a promising solution thanks to their high power density and efficiency, but PMs are costly and delicate components [8]. On the other hand, Switched Reluctance Machines (SRMs) [9] are well suited for high speed and harsh environment operation, featuring enhanced robustness and reliability.

However, for both PMGs and SRMs, proper active AC/DC power electronic converters are required as controlled interfaces between the generator and the on-board grid. The presence of full-rated active power converters, which may feature a very complex structure especially in high-voltage and multi-phase scenarios [10], penalise significantly the overall weight and reliability [11], thus becoming the bottleneck of the system. In fact, such configuration involves several drawbacks, especially when high degrees of modularity and fault-tolerance are required:

- internal short-circuit faults in the PMGs are troublesome due to the lack of de-excitation capability;
- the presence of active power switches, possibly featuring low MTBF values, introduces reliability issues which are amplified by the presence of complex control and sensing systems;
- common hard-switched converters typically exhibit higher power losses than diode rectifiers, mainly due to the forced commutations;
- active power converters generate high-frequency step-wise waveforms which can give rise to increased voltage stress in the windings [12], thus representing an additional challenge for the insulation.

As an alternative, the architecture of a generation system based on homopolar generators equipped with diode front-end rectifiers has been proposed in [13], targeting improved robustness, reliability, cost-effectiveness, efficiency, and simplicity [5]. In fact, it is widely reported that diodes feature just 25% the failure rate of active switches [5], [14]. In Section II a literature review of Synchronous Homopolar Machines (SHMs) is proposed. In Section III, a possible architecture for a generation system supplying two independent DC power buses is presented. Section IV finalises the preliminary electromagnetic design of a 15 kW @ 15 krpm homopolar generator intended as a concept demonstrator for the proposed system configuration, which is then validated in Section V using a 3-D Finite-Element (FE) model.

Amedeo Vannini, Alessandro Marfoli, and Chris Gerada are with the Power Electronics, Machines, and Control Group, University of Nottingham, UK (e-mail: amedeo.vannini1@nottingham.ac.uk).

Luca Papini and Paolo Bolognesi are with the University of Pisa - DESTEC, Italy (e-mail: paolo.bolognesi@ing.unipi.it)

II. SYNCHRONOUS HOMOPOLAR GENERATORS

In the context of high-speed power generation, SHMs are gaining more and more interest for applications that require high reliability, thanks to the fact that all delicate components are located in the stator. This means that the rotor structure can be reduced to a properly shaped magnetic core supported by the shaft, turning out very robust and reliable [15], [16]. In this way, the rotating speed can be increased up to the centrifugal stress limit of the core material, similarly to SRMs. However, SHMs are equipped with a stationary field winding permitting to fully control the machine excitation.

The most common SHM configuration, which is depicted in Fig. 1, consists in two identical and magnetically linked stator core sections equipped with armature windings featuring a field winding located between them. The rotor, which could be either laminated or solid, features two salient cores. The excitation winding, typically solenoidal, provides the homopolar magnetization of the machine by means of an axial flux path along the shaft, which then radially crosses the air gap throughout the two stator sections. An external magnetic sleeve, connecting the two stator sections, permits the axial closure of the magnetising flow path. The resulting air-gap flux density is therefore unipolar in each section. Opposite polarity is instead featured when the two sections are compared [17]. The operating principle of this machine topology is widely described in [17], [18].

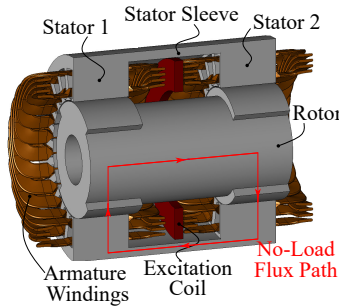


Fig. 1. Typical layout of SHMs

In the context of passive rectification [13], the field controllability is a key-feature for the DC-bus voltage regulation. Furthermore, the possibility to control the excitation field permits a reliable de-excitation (by current reduction possibly down to the null current condition) in case of failure, which is not possible in PMGs. The absence of PMs improves also the overall reliability, since any risk of demagnetization or high-temperature performance degradation [8] is avoided.

In contrast to WFSGs, in SHMs the field winding is stationary, and therefore the rotor results in a simplified structure: the mechanical issues related to the presence of rotating windings are then avoided [6]. In WFSGs, the extraction of the heat from the rotor due to the field winding losses results challenging since it is difficult to implement effective cooling systems in rotating elements [5]. On the other hand, conventional cooling techniques usually implemented for stators can be well used for a field winding located in the stator.

Furthermore, neither slip-ring systems nor complex and bulky rotating brushless exciters (e.g., three-stages WFSGs [5]) are required to feed the excitation winding.

However, the main weak point of SHM is the pretty low torque and power density level that can be achieved with respect to other machine topologies [18]. In fact, the usual alternation of north and south magnetic poles along the tangential direction, typical for WFSGs, is now split between the two sections, resulting in a axial length which is at least doubled [18]. Nevertheless, the simple and robust structure of the passive power converter helps to reduce the overall weight and volume of the power generation system.

Different variants have been proposed in the literature for SHMs. For what concerns the stator winding, two different layouts are mainly considered as depicted in Fig. 2:

- *pass-through armature*, whereas the armature coils span along the whole machine length crossing the mid plane (e.g., [16]); in this case an angular shift between the two rotor pole sections is typically used in order to achieve a variable coupling between each armature phase and the field winding.
- *split armature*, whereas each armature coil spans just one of the two sections and the coils are then properly connected as necessary (e.g., [13]): this arrangement offers a higher flexibility in the machine architecture, e.g. easily permitting to create 2 three-phase separate winding located in the 2 stator sections.

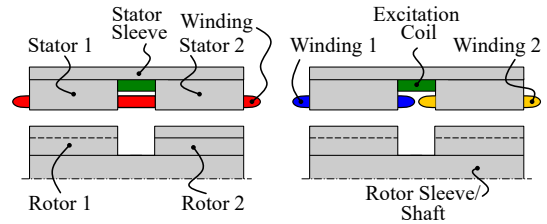


Fig. 2. Types of winding layouts for SHMs: *pass-through armature*, in red on the left, and *split armature*, in blue and yellow on the right

Despite the second winding arrangement offers more flexibility in the implementation of fault-resilient and redundant features, the presence of end-windings in between the 2 stator sections unavoidably increases the overall length of the machine. On the other hand, the first solution requires a more complex manufacturing related to the realisation of the winding passing through the two stators, and less room for the excitation winding is available.

III. SYSTEM ARCHITECTURE

From an architectural point of view, the electric propulsion features a high flexibility, thus enabling innovative technological solutions such as the concept of distributed propulsion for future aircraft [19] or the integrated power system for naval vessels [20]. Within this context, there is a critical need for reliable electric drive architectures with reduced mass and volume [10]. In fact, a typical concept

of architecture is the hybrid turbo-electric layout [21], [22], whereas multiple prime movers drive electrical generators which feed independent DC power buses, thus offering several possibilities to effectively implement fault management strategies [2]. A typical layout is depicted in Fig. 3: each prime mover "PM" (e.g., gas turbine or diesel engine) drives an electrical generator which feeds two independent power systems via AC/DC rectifiers. The overall power system of the vehicle can be obtained as a composition of this basic drivetrain system, according to the number of necessary prime movers. Only three motors per power channel are shown in this case, but the number can be purposely selected according to the desired degree of distributed propulsion [19].

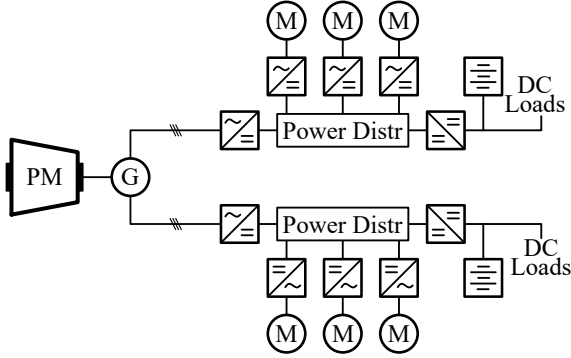


Fig. 3. Example of a simplified hybrid turbo-electric drivetrain system. PM is the prime mover, G the electrical generator, M the electric motor driving the propeller

The push towards higher DC voltages has been justified by the need to achieve lightweight solutions. In general, the increased voltage rating does not always determine a reduction in the gross section of cables because of the thicker insulation required for high operative voltages. Furthermore, the combination of high-voltage levels with high-frequency switching of power conversion units in low-pressure environments (e.g., aerospace applications) may introduce additional issues for the electrical insulation systems [12]. The short voltage transients related to the operation of fast switching devices, combined with long feeder cables, can lead to significant over-voltages at the motor side, due to the impedance mismatch at the interface.

The selection of a bipolar DC distribution with a grounded middle point (e.g., ± 1.5 kV [4]) may reduce the aforementioned issues by decreasing the maximum line-to-chassis voltage in any condition, effectively halving the phase-to-chassis insulation required. Such solution permits also to exploit the two different voltage levels available for the loads when a neutral feeder connected to the central point is included. However, with this arrangement, the overall load distribution in the positive and negative buses may become unbalanced, requiring specific attention in the design. On the other hand, when properly designed, the system can still operate even with one pole out of service still possibly delivering half of the rated power by means of the undamaged pole [4].

A number n_s of AC/DC converters connected in series at the DC side may be employed to reduce the insulating requirements for the machine [3]. In this paper the architecture proposed for the generation system assumes $n_s = 2$ series-connected 3-phases bridge rectifiers per each DC bus, with central connected to the chassis and possibly available as a neutral feeder, according to the scheme in Fig. 4. The split armature layout is adopted to permit one generator to supply two independent power buses in a convenient arrangement, while keeping segregated the coils composing each of the two sections. The electromagnetic interaction between the 2 sections is also drastically reduced, enabling an easier implementation of fault detection and management solutions.

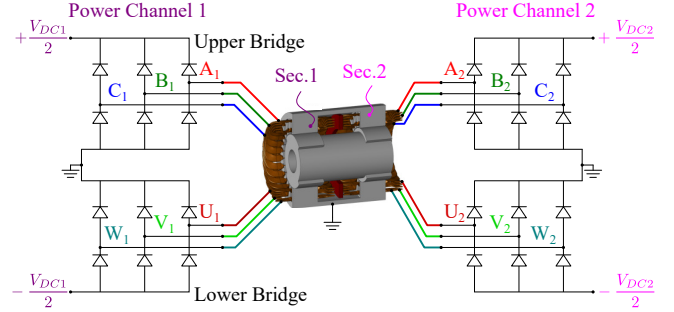


Fig. 4. Integrated dual DC-bus generation system architecture: each section supplies one independent DC power bus via two diode rectifiers connected in series. Each sections features 2 three-phase systems, named ABC and UVW, supplying the upper and lower bridge rectifiers, respectively

The excitation system permits to control the air-gap flux density in both machine sections and thus the phase voltages and ultimately the voltages of the DC buses. This allows to reduce the overall complexity, as long as there is no necessity to have different excitation levels in the 2 machine sections. This is the case at no-load and for any symmetrical loading condition, provided that the machine is properly designed. When instead the loading condition becomes rather different for the 2 sections, a single excitation system can be used to control two independent DC voltages only if the generator is designed in such a way to feature a limited armature reaction effect. In fact, this permits to bound the voltage variations within an acceptable range, e.g. $\pm 10\% V_n$ [4]. Nevertheless, it must be kept into account that the reduction of the armature reaction implies that in case of short-circuit the fault currents may become large.

IV. PRELIMINARY ELECTROMAGNETIC DESIGN

The electromagnetic design of a Synchronous Homopolar Generator (SHG) featuring the layout proposed in Section III and the specifications reported in Tab. I is proposed.

A. Basic Characteristics

The number of pole pairs has been selected as $p = 2$, as a trade-off between the minimisation of the cores weight and the iron losses. In fact, this leads to a rated electrical frequency $f_n = 500$ Hz: the JFE 10JNRFTM steel, intended

TABLE I
CASE STUDY SPECIFICATIONS

Symbol	Parameter	Unit	Value
$2 \times P_n$	Rated Power	kW	2×7.5
Ω_n	Rated Speed	krpm	15
V_{DC}	Rated DC Voltage	V	600
I_{DC}	Rated DC Current	A	12.5
I_e	Rated Excitation Current	A	25
p	Pairs of Poles	-	2
f_n	Rated Frequency	Hz	500

for high-speed machines, has been then selected for both stator and rotor cores, aiming to reduce the iron core losses.

Keeping in mind the envisioned high power application, a Y connection of the phases in each of the 3-phase windings was assumed to further reduce the insulation requirements. Considering the system architecture, this means that each phase will operate in 3 modes during the period T :

- 1) a positive conduction interval, during which its output terminal will be connected to the positive bar of the DC bus and the current will flow out of it; at no load, such interval will last $T/3$, and during it no other phase will feature a higher voltage;
- 2) a negative conduction interval, during which its output terminal will be connected to the negative bar of the DC bus and the current will flow into it; at no load, such interval will last $T/3$, and during it no other phase will feature a lower voltage;
- 3) 2 non-conduction intervals separating the above conduction intervals, lasting $T/6$ at no load.

The minimization of the voltage ripple at the DC output of the rectifiers implies that the waveform of phase voltages in the conduction intervals should be as flat as possible. Such a flat trend lasting for $T/3$ could be easily obtained in principle by using a concentrated winding layout with coil pitch reduced by $T/3$ and a simple square-wave flux density waveform in the main air-gap. Moreover, stator windings featuring a concentrated layout are particularly attractive for applications that require high reliability (e.g., aerospace), thanks to the electric and thermal separation of the phases which also increases the fault tolerance capability of the machine. Therefore, a concentrated winding layout is considered, assuming the deployment reported in Fig. 5 which involves a double-radial-layer arrangement and a number of slots per machine section $Q = 12$.

Given the rated values of DC bus voltage V_{DC} and generated power per each machine section P_n , the preliminary electromagnetic sizing of the machine can be obtained under the following assumptions:

- negligible H-field in the ferromagnetic materials;
- negligible slotting effect;
- negligible variation of the flux density along the axis in the main air-gap of each section;
- negligible armature reaction effects;
- ideal behaviour of the diodes.

First of all, reasonable values are chosen for the air-gap thickness $\varepsilon_G = 1$ mm and for the desired no-load maximum

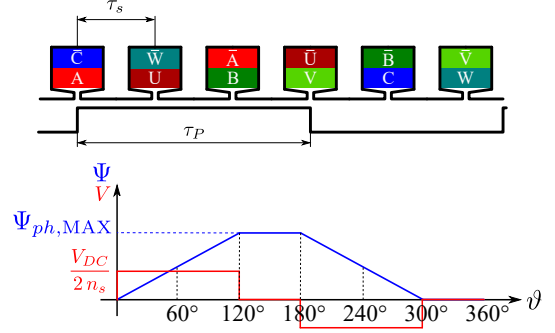


Fig. 5. Concentrated winding layout for $Q = 12$ slots and the related ideal phase voltage (in red) and linkage flux (in blue) with respect to the rotor electrical angular position ϑ

air-gap flux density $B_{G,MAX} = 0.7$ T. The m.m.f. required to the excitation winding $\mathcal{F}_e$ is then obtained by applying the Ampere's law at no-load condition:

$$\mathcal{F}_e = \frac{2 \cdot \varepsilon_G \cdot B_{G,MAX}}{\mu_0} \quad (1)$$

where μ_0 is the permeability of vacuum. In such conditions, the magnetic flux flowing via each rotor pole is obtained as $\Phi_P = B_{G,MAX} \cdot \tau_P \cdot \ell$, where ℓ is the active length of one machine section and τ_P is the pole pitch width. The total homopolar flux flowing into both stator and rotor sleeves is given by $\Phi_0 = p \cdot \Phi_P$. The minimum radial cross section of the sleeves S_{SL} can then be determined from the desired value of average flux density $B_{SL,MAX}$ as follows:

$$S_{SL} = \frac{\Phi_0}{B_{SL,MAX}} = \frac{B_{G,MAX} \cdot \tau_P \cdot \ell \cdot p}{B_{SL,MAX}} \quad (2)$$

The radial thickness of the sleeves is left constant, both for practicality and aiming to compensate the higher m.m.f. drop in the magnetic parts of the longer path of the field lines crossing the air-gap close to the external sides. The peak voltage that each phase will produce at no load can be ideally expressed as

$$V_{ph,MAX} = p \cdot N_S \cdot \frac{2/3 \cdot \Phi_P}{1/3 \cdot T_n} = N_S \cdot 2 \cdot \Phi_0 \cdot f_n \quad (3)$$

where T_n is the rated electric period and N_S is the number of turns per each stator coil. The latter can be then determined taking into account the relationship with the desired DC bus voltage $V_{DC} = 2 \cdot n_s \cdot V_{ph,MAX}$, which provides:

$$N_S = \frac{V_{DC}}{4 \cdot n_s \cdot p \cdot \Phi_P \cdot f_n} \quad (4)$$

B. Improved Armature Winding Layout

According to Fig. 4, two different three-phase windings are hosted in the same machine section, thus potentially interacting. When the concentrated winding layout sketched in Fig. 5 is used, the phase voltage waveforms produced result shifted by $T/6$ or multiples. On the other hand, each of the 2 windings in the same machine section feeds a 3-phase bridge rectifier, whose natural commutations occur just

every $T/6$ [23]. Therefore, in this arrangement the diode commutations would take place simultaneously in the two rectifiers connected in series. Since in reality the current cannot be switched instantaneously from one phase to another, an additional ripple in the DC voltage can be observed due to the simultaneous conduction. Such adverse effect can be mitigated by displacing the commutations occurring in the 2 rectifiers, in such a way to double the frequency and halve the peak amplitude of the ripple just as in the classic 12-pulses arrangement for large AC/DC rectifier systems [23]. To such purpose, a $T/12$ shift has to be introduced in the voltage waveforms generated by the two 3-phase windings: this requires doubling the number of slots to reduce the slot pitch to $\tau_P/6$. The resulting layout makes available 24 coils per each machine section, arranged as shown in Fig. 6.

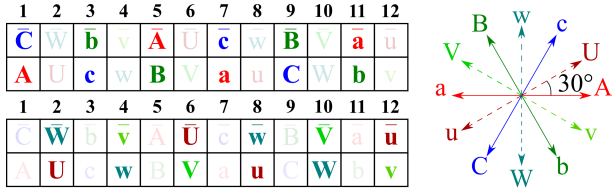


Fig. 6. Winding layout minimizing the commutation effects: 1 pole pair

As highlighted in the phasor diagram, which effectively represents the phase displacement of voltage waveforms, each 3-phase winding is then composed of 2 coils connected in anti-series per each phase (A-a, B-b, C-c and U-u, V-v, W-w, respectively) in each pole pair.

The main design outputs following the preliminary electromagnetic sizing of the machine are reported in Tab. II.

TABLE II
PRELIMINARY SIZING OUTPUTS

Symbol	Parameter	Unit	Value
n_E	Number of Phases	-	2×12
$B_{G0,MAX}$	Max. Air-Gap Flux Density	T	0.7
ε_G	Air-gap Thickness	mm	1
ℓ	Active Axial Length per Section	mm	55
ℓ_{tot}	Total Active Axial Length	mm	196
D_r	Rotor External Diameter	mm	110
D_s	Stator External Diameter	mm	197
N_s	Number of Stator Turns	-	13
N_e	Number of Excitation Winding Turns	-	45
Q	Number of Slots	-	24
w_t	Tooth Width	mm	7.3
k_{fill}	Slots Fill Factor	-	0.4
J_s	Max. Stator Current Density	A/mm ²	4
J_e	Max. Excitation Current Density	A/mm ²	8

V. 3-D FE MODELING AND ANALYSIS

The design of the SHG carried out in the previous section was validated by means of the Finite-Element Analysis (FEA) using the specialized software JMAG®. The inherent 3-D nature of the field map expected basically forces to use a 3-D model [15]. This makes even more important to shrink as much as possible the size of the modeled region

to limit the computational burden. Since 2 identical pole pairs are present, just half of the machine can be modeled along the tangential direction by imposing even periodic boundary conditions on a cut plane passing through the rotation axis. Moreover, since the 2 sections of the machine are assumed to be identical and aligned along the tangential direction, when the same operative conditions are assumed for both sections, the mid plane orthogonal to the rotation axis becomes a symmetry plane: just half of the machine length may be modelled, by applying the orthogonal field boundary condition.

The quality of the mesh was selected after a sensitivity analysis. The air-gap was split into 4 thin mesh layers along the radial direction, imposing a regular fine division along the tangential direction. Particular attention was given to the mesh quality in the regions where local magnetic saturation is expected (e.g., stator teeth tips). The reduced 3-D FE model and the related mesh is shown in Fig. 7. The simulations were carried out considering for the cores both a high permeability linear material (with $\mu_r = 10^4$) and the selected laminations.

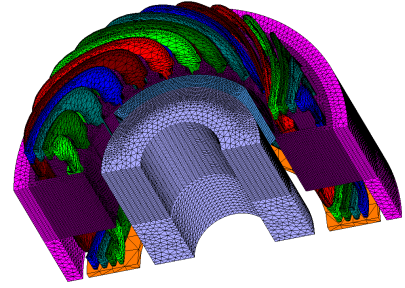


Fig. 7. 3-D FE reduced model mesh. The excitation coil is depicted in orange; the armature windings color pattern is the same of Fig. 6

A. Magneto-Static Analysis

Simple magneto-static simulations permit to validate the no-load maximum air-gap flux density selected for the preliminary design, and to refine the magnetic sizing of the cores according to the actual magnetic loading, when real saturable materials are considered. For such purpose, the field coil is supplied with the rated current $I_e = 25$ A, while the armature currents are zeroed. The ensuing field map is shown in Fig. 8.

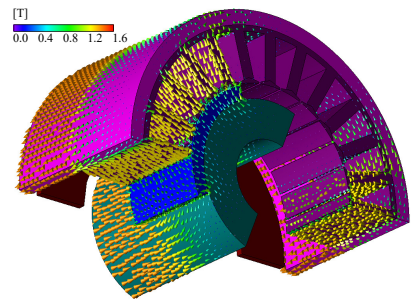


Fig. 8. No-Load flux density field map: axial view

The main field lines feature an axial path along the sleeves, then turn towards the radial direction flowing through

the section yokes and teeth to finally cross the main air-gaps. The field map ensuing in the considered condition is shown in Fig. 9, where an axial section is reported for a plane including the rotation axis alongside the cross section related to the mid symmetry plane of the machine.

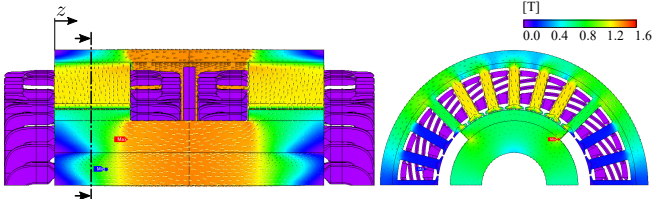


Fig. 9. No-Load flux density field map: axial (left) and cross-section (right) views

The magnetic loading of the rotor sleeve results almost independent on the rotor angular position, meaning that a bulk part can be used without significantly increasing iron losses. On the opposite, the flux density in the stator sleeve is not uniform along the tangential direction and is dependent on the rotor position, meaning that local field variations are expected during the operation: this requires using solutions able to contrast the eddy currents, such as soft magnetic composites, to avoid excessive losses. Moreover, since the magnetic loading of the sleeves decreases towards the end sides, their section could be eventually reshaped in a conical fashion to reduce their weight, thus improving the power density. One can also notice that the assumption of constant value of B_G along the axial direction within each section is confirmed as a very good approximation.

In Fig. 10 the trend of the radial component of the flux density evaluated along the mean air-gap surface in the middle cross-section plane of the modeled section is shown for null armature currents. The results obtained using both linear and saturable core materials are compared against the design target $B_{G0,MAX} = 0.7$ T. The typical unipolar trend featured by homopolar machines can be noticed, as well as a very good match of the top value. In fact, for the linear case a mere 2.8% discrepancy can be observed, mainly due to the leakage fluxes and the presence of the slot openings, whereas the mismatch rises up to 10% when the magnetic saturation is kept into account.

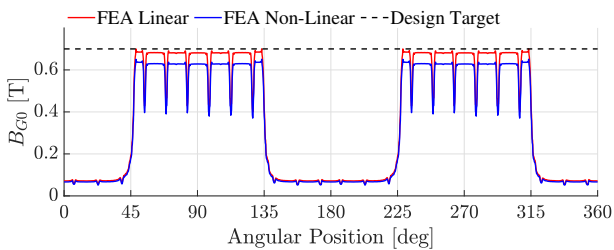


Fig. 10. No-Load radial component of flux density in the air-gap of one machine section vs. angular position: ideal and real FE results vs. design target. The waveform is extracted at $z = \ell/2$

B. Transient with Motion Analysis: No-Load Conditions

A transient analysis with motion at the rated speed with null armature currents permits to evaluate the trend of linkage flux per each phase, and the associated no-load voltage waveforms. The fluxes trends against rotor position for the 3-phase sets of the modeled section are shown in Fig. 11 for the magnetically linear scenario. Only half of a period is shown, since the machine structure generates even-symmetrical waveforms.

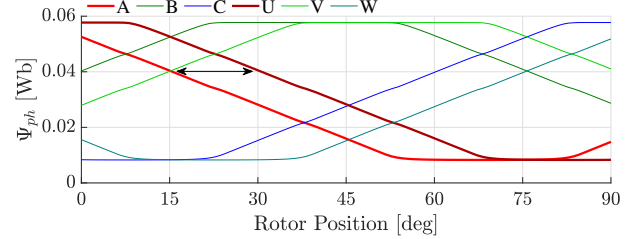


Fig. 11. Linkage fluxes functions with respect to the rotor angular position. The $T/12$ angular-shift between phase A and U is highlighted

The flux waveforms feature the desired trapezoidal trend with the expected displacement corresponding to $\tau_p/6$, confirming the appropriate design of the matched winding layout and rotor core profile. The maximum value of the linkage flux function (57.6 mWb) matches very well the target assumed in the preliminary sizing stage (58.2 mWb), as the relative error is just 0.95%. Nevertheless, the minimum flux value is appreciably higher than the ideal null value assumed in the preliminary sizing procedure (Fig. 5). Therefore, the actual amount of the flux variation during the transition intervals is smaller than the value assumed in the design stage, meaning that the induced voltage is expected to be somewhat lower than the desired value estimated according to (3). To compensate for such discrepancy, several options are available, including a more salient profile for the rotor poles and/or a larger number of turns in the stator coils.

VI. CONCLUSIONS

This paper presented the concept and the preliminary electromagnetic design of a synchronous homopolar generator intended to supply 2 independent DC buses via passive diode rectifiers, as a part of the power system on board of a large vehicle such as an aircraft or a ship. The proposed solution aims to provide a highly reliable and efficient generation unit, capable to permit regulating the output voltage. The 2 axial sections of the machine host two 3-phase Y-connected windings each, which feed 2 diode rectifiers connected in series to supply one of the DC buses. The machine is designed to generate at no-load 2 sets of six-steps voltages per each section, featuring a phase displaced by $T/12$ to minimize the ripple in the DC output voltages. The preliminary electromagnetic sizing of a small-scale concept demonstrator was presented as well as its initial validation by means of 3-D FE simulations at no-load conditions. Further developments are planned including a deeper electromagnetic analysis under

different operative conditions, a more extensive validation and refinement of the design, as well as the investigation of thermal and mechanical aspects. The ensuing results will be presented in future papers.

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BIOGRAPHIES

Amedeo Vannini received the M.Sc. (*cum laude*) in Electrical Engineering from the University of Pisa, Italy, in 2020. He enrolled then in the Ph.D. in Electrical Engineering at the University of Nottingham, UK. His main research interests include the modelling, analysis and design of electric machines, in particular permanent magnet, induction and homopolar, for various applications including industrial motors and generation systems.

Luca Papini received the bachelor's (Hons.) and master's (Hons.) degrees in Electrical Engineering from the University of Pisa, Italy, in 2009 and 2011, respectively, and the Ph.D. degree from the University of Nottingham, U.K., in 2018. He has been a Research Assistant with the University of Nottingham, since 2013. He was JSPS Fellow in 2018 and is currently a Senior Researcher with the University of Pisa. His research interests include high-speed, high power density electric machines, machine control, and levitating systems.

Alessandro Marfoli received the M.Sc. in Electrical Engineering from the University of Pisa, Italy, in 2015, and the Ph.D. degree in electrical machine design from the University of Nottingham (UK) in 2020 where he is currently a Research Fellow. His main research interests include modelling, analysis and optimization of electrical machines.

Chris Gerada received the Ph.D. degree in numerical modelling of electrical machines from the University of Nottingham, U.K., in 2005. He subsequently worked as a Researcher with the University of Nottingham on high-performance electrical drives and on the design and modelling of electromagnetic actuators for aerospace applications. He was appointed a Lecturer in electrical machines in 2008, an Associate Professor in 2011, and Professor in 2013. His core research interests include the design and modelling of high-performance electric drives and machines. Prof. Gerada is an Associate Editor of the IEEE Transaction on Industry Applications. He has secured major industrial, European and U.K. grants, authored more than 200 papers and was awarded a Royal Academy of Engineering Research Chair to consolidate research in the field.

Paolo Bolognesi received the M.Sc. in Electrical Engineering *cum laude* and the Ph.D. degree from the University of Pisa, Italy, in 1995 and 1999 respectively. Since 2001 he is with the University of Pisa as a Senior Researcher and Appointed Professor. His research interests are mainly focused on the theoretical and simulation modeling and analysis of electromagneto-mechanical devices and on their design also using innovative manufacturing technologies, including unconventional and high performance electric machines and electromechanical actuators. He is also interested in innovative topologies and modulation/control techniques for power converters and drives, as well as in specific applications such as electric and hybrid vehicles, aerospace, industrial automation and renewable energies.