An Integrated Permanent-Magnet-Synchronous Generator–Rectifier Architecture for Limited-Speed-Range Applications

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Abstract—Conventional high-power ac–dc conversion architectures rely heavily on active rectifiers, which consist of fully controlled power-electronic switches. These make the system bulky, lossy, and less reliable. This article presents an alternative approach—integrating a multiport permanent-magnet synchronous generator (PMSG) with series-stacked power converters. An active rectifier processes only a fraction of the total converted power while regulating the dc bus. The remaining power is processed by diode bridges, which allows a substantial increase in overall efficiency, power density, and reliability. Theoretical analysis shows that for wind-power generation applications, the active rectifier processes a maximum of 39% of the rated power while the generator operates in a speed range similar to the conventional doubly fed induction machine. The conversion loss is reduced by 47%. Results from a laboratory-scale experimental setup corroborate the proposed architecture. This approach potentially increases integration of medium-voltage dc distribution to the megawatt-class mechanical-to-electrical energy conversion systems to achieve higher efficiency, higher power density, and lower cost compared to the conventional solutions based on a single-port PMSG with a full-power-rated converter.

Index Terms—AC–DC power conversion, dc power systems, power conversion, rectifiers.

I. INTRODUCTION

A C–DC power conversion is essential in many emerging applications, including wind energy and electrified transportation. For example, an intermediate dc bus is often necessary to connect a variable-speed wind-powered generator to the fixed-frequency ac grid [2]. Similarly, future electric ships are envisioned to have a medium-voltage dc (MVdc) grid connected to gas-turbine-driven ac generators [3]. The preferred electrical output of these megawatt-scale mechanical-to-electrical energy conversion systems is a regulated dc bus, while the prime-movers operate over a limited speed range. The operating speed range is driven by an improved energy-captured-to-cost ratio, as in a wind turbine [4] or by an increased fuel efficiency, as in a gas-turbine driven generator [5]. It is commonly limited to 55%, and 82% to the rated speed for wind turbines [6], and gas turbines [5], respectively.

From an electric-generator selection perspective, power converter rating is an important criterion. For example, in wind-power generation, a lower power-converter rating has made doubly fed induction generators (DFIGs) preferable to permanent-magnet synchronous generators (PMSGs). A DFIG-based system is typically designed to operate with power electronics rated to handle only one-third of the system-rated power while the generator operates over a limited speed range. A PMSG-based system requires a full-system-power-rated converter to control the energy flow. This required rating has restricted the adoption of PMSGs, despite its higher torque density, increased efficiency, improved reliability, better grid-fault-ride-through capability, and reduced maintenance compared to DFIGs.

From a converter perspective, creating a regulated dc bus from a variable-voltage-variable-frequency ac source at medium voltage and megawatt power is a daunting proposition. Power electronics switch rating and a high switching loss are the common limiting factors. The simplest solution is a six-pulse diode-bridge rectifier with the dc voltage ripple filtered by a capacitor. Although it has simple construction and high conversion efficiency, this architecture cannot actively regulate the dc-bus voltage and requires a bulky filter capacitor to meet a low-voltage-ripple requirement. Although a boost converter can be cascaded to a six-pulse diode bridge to provide dc-bus voltage regulation [7], it results in high dc-side ripple current. An interleaving boost converter architecture could reduce the current ripple, and consequently, the filter capacitor size. However, full output voltage and peak input current rating requirements for the power electronic switch remain major challenges. A three-phase active rectifier can regulate the dc-bus voltage and provide power-factor control on the ac side [8]–[11], but it faces the same switch-rating challenges as the boost converter [12]. Series-connecting multiple switches achieve a higher rating but require additional hardware to ensure equal voltage sharing.
Additional switches and hardware adversely affect system efficiency and reliability. Although the neutral-point-clamp multilevel topology allows dc-bus voltage regulation and power-factor control capability, while requiring lower-than-dc-bus voltage switch rating [13]–[15], capacitor voltage balancing is still a major challenge. A multilevel flying-capacitor converter offers redundant switching states for controlling capacitor voltage balancing and even loss distribution [16]–[18]. However, a large number of capacitors, precharging circuits, and a complex control scheme are required. Multiport PMSGs have been considered to reduce the individual power-converter rating by serially connecting the rectifier outputs to form the dc bus. The design philosophy has been to treat each three-phase-port similarly, as reported in [19] and [20]. The goal is to simultaneously offer dc-bus voltage regulation capability with low rating requirements for the power electronics switch, simplified circuitry and control scheme, and high conversion efficiency.

This article presents a series-stacked ac–dc conversion architecture integrated with a multiport PMSG, shown in Fig. 1. It achieves a regulated dc bus while substantially reducing the need for actively controlled power electronics switches. A prime-mover-driven PMSG creates multiple three-phase ac ports. Each is connected either to an active rectifier, consisting of actively controlled power electronics switches, or to a passive rectifier, comprising only diodes. Serially stacking the dc outputs of \( n \) active with \( m \) passive rectifiers creates a medium-voltage dc bus, while each rectifier provides a fraction of the voltage. The proposed architecture is similar to multilevel converters. However, integrating power electronics with individual PMSG electrical ports leads to a simpler control strategy because the passive rectifier output voltages are naturally balanced. This natural balancing is achieved by connecting the filter capacitors to identical and isolated ac ports via passive diode bridge rectifiers.

The capacitor voltage is fully determined by the source back electromotive force (emf), synchronous inductance, per-phase resistance, and dc load current [21]. The active rectifiers regulate the dc-bus voltage as the prime-mover speed decreases from the maximum/rated value. Supporting a fraction of the dc-bus voltage, they process only a part of the total power, leading to substantial loss reduction. Extensive use of diodes commuting at line frequency improves the power-circuit reliability because the diode failure rate is more than 10 times lower compared to a power transistor [22]. In addition, employing diodes on the high-voltage side of the dc bus simplifies the hardware, eliminating numerous isolated gate drivers and voltage sensors, further improving the overall system reliability. This architecture opens up multiple opportunities, including power-electronics–electric-machine integration and creation of new design tradeoffs for limited-speed range mechanical-to-electrical energy conversion systems.

This article extends the work presented in [1] by introducing a design methodology which considers nonideal characteristics of the conversion system, including PMSG stator resistance, synchronous inductance, and diode forward voltage drop. Experimental results are presented in more detail to corroborate the proposed system. The volt–ampere (VA) rating calculation is validated by the experimental measurements. These measurements show that two-port is the optimal configuration for the tested PMSG. Conversion efficiencies of the conventional single-port and the proposed two-port configurations are compared. The comparison emphasizes the advantage of the proposed architecture in increasing conversion efficiency across the whole operating speed range. In addition, dc-bus voltage waveforms are included to show the fast dynamic response of the system. A bumpless dc-bus voltage is maintained under variations of dc-load current and generator speed.
The rest of this article is organized as follows. Section II discusses the design aspects of the proposed architecture. Design examples are presented in Section III. Designs incorporating nonideal characteristics of the generator are illustrated in Section IV. Section V discusses the benefits of the proposed design in terms of reduction in power processed by the active rectifier and improvement in the overall conversion efficiency. Experimental results to corroborate the findings are presented in Section VII.

II. INTEGRATED DESIGN OF THE PROPOSED ARCHITECTURE

This section explores the design space for the proposed architecture. The design objective is to minimize both the voltage rating and the total volt–ampere rating of active rectifiers, while a constant dc-bus voltage is maintained across the entire generator operating speed range. The first objective is achieved by selecting the combination of number of passive rectifiers \((m)\) and number of active rectifiers \((n)\). One active rectifier and the rest passive rectifiers achieve the first objective, as presented in Section II-B. The second objective is achieved by selecting the number of PMSG three-phase ac ports \((k)\). Section II-C shows this optimum number constrained by the application-specific requirements, such as operating speed range and the power-versus-speed characteristic of the turbine.

A. Active Rectifier Voltage Requirement for a Generalized Architecture

A PMSG with \(k\) accessible three-phase ports is considered. At the maximum speed, the PMSG delivers a rated power of \(P_0\) and each ac port has a peak line-to-neutral back emf of \(E_0\). As the back emf is proportional to the operating speed, the peak line-to-neutral value is

\[
E_\omega = \omega E_0
\]

where \(\omega\) is the p.u. speed with the generator maximum speed as the base value. Neglecting the generator stator-winding resistance, synchronous inductance, and the diode forward voltage drop, the dc-output voltage of each passive rectifier is

\[
V_{\text{passive}} = \sqrt{3} E_\omega
\]

which depends only on the generator speed. In contrast, the dc-output voltage of the active rectifier depends both on the operating speed and the operating modulation index/duty ratio of the power-electronic switches. Assuming ideal switching devices and a space-vector modulation scheme to generate sinusoidal voltages, the dc-output voltage of each active rectifier is

\[
V_{\text{active}} = \frac{\sqrt{3}}{d} E_\omega
\]

where \(d\) is the modulation index that ideally ranges between 0 and 1. This modulation index becomes the control variable used to regulate the total dc-bus voltage.

Series-stacking the output of \(m\) passive and \(n\) active rectifiers, each connects to individual ac ports of a \(k\)-port PMSG, creating a total dc-bus voltage of

\[
V_{dc} = \sqrt{3} \left( m + \frac{n}{d} \right) E_\omega.
\]

The maximum value that \(d\) can take is unity. This implies that at the maximum speed, the total dc-bus voltage cannot be less than \(\sqrt{3}(m+n)E_0\). This lower bound sets the nominal dc-bus voltage, denoted by

\[
V_{dc,\text{nom}} = \sqrt{3} k E_0
\]

which must be regulated for all operating speeds. At a lower operating speed, the dc output of each passive rectifier governed by (2) decreases. The total decrease must be compensated by the series-connected active rectifiers to maintain the dc-bus voltage at \(V_{dc,\text{nom}}\). Using (1), (2), and (5), the total dc-output voltage of the active rectifiers depends on \(\omega\) as

\[
V_a = V_{dc,\text{nom}} - m V_{\text{passive}} = (k - m\omega) \sqrt{3} E_0.
\]

Normalizing (6) by the nominal dc-bus voltage \(V_{dc,\text{nom}}\), the total dc-output voltage of the active rectifiers in p.u. is

\[
v_a = 1 - \omega \frac{m}{k}.
\]

Equation (7) highlights that the total dc voltage requirements of active rectifiers depend on the generator speed, number of PMSG ac ports, and number of passive rectifiers. This generalized equation forms the basis for minimizing both the voltage rating and the VA rating of the active rectifiers.

B. Selection of Number of Passive Rectifiers

The next step in the design process is to find the number of passive and active rectifiers for a \(k\)-port PMSG such that the total dc-output voltage of the active rectifiers is minimized. Equation (7) shows that \(v_a\) decreases as \(m\) increases. As at least one active rectifier is essential to provide a dc-bus voltage regulation, the maximum possible value of \(m\) must be \(k - 1\). This design choice and (7) set the active rectifier output voltage requirement to a minimum value, that is

\[
v_a = 1 - \omega \frac{k - 1}{k}.
\]

This equation suggests that the active rectifier must support a higher dc voltage at a lower operating speed. Fig. 2(a) shows this effect for different \(k\). For example, a four-port PMSG requires the active rectifier to support 25% of the dc-bus voltage at 1 p.u. operating speed and 61% at 0.55 p.u. In contrast, a single-port PMSG requires the active rectifier to support 100% of the dc-bus voltage at all the operating speeds.

The maximum voltage support requirement of the active rectifier is its voltage rating. Equation (8) shows that the voltage rating corresponds to the minimum operating speed \(\omega_{\text{min}}\) and is given by

\[
v_{a,\text{rated}} = 1 - \frac{k - 1}{k} \omega_{\text{min}}.
\]

Fig. 2(b) illustrates the dependency of the voltage rating on \(k\) for two different \(\omega_{\text{min}}\) values. As expected, the voltage rating of the active rectifier decreases with an increase in number of ac
ports but at a diminishing rate. The figure also demonstrates the advantage of the proposed architecture in limited-speed-range applications—the narrower the operating speed range, the greater the reduction in the voltage rating requirement. The operating speed range is application dependent. For example, in a conventional DFIG-based wind turbine, the minimum-to-maximum operating speed ratio is frequently chosen to be 0.7:1.3 to achieve a high energy-captured-to-cost ratio [4].

C. Selection of Number of AC Ports

Although (9) suggests that it is better to keep increasing the number of ac ports, as it reduces the active power electronics voltage rating, from a design perspective, the active rectifier VA rating, defined as the product of the voltage and current rating, is a more important criterion. It governs the converter size, weight, and cost. The last step in the design process is to minimize the VA rating. This minimization is achieved by appropriately selecting the number of ac ports.

The active rectifier current rating is set by the maximum peak-ac-line current. The peak-ac-line current is determined by the ac-side and dc-side power balance. Assuming a unity power factor operation of the active rectifier, the power balance yields

$$\frac{3}{2} \omega I_{ac} = V_{active} I_{dc}$$  \hspace{1cm} (10)

where $I_{ac}$ and $I_{dc}$ are the peak-ac and dc-side currents, respectively. Use (1), (6), and set $m$ to $k - 1$,

$$I_{ac} = \frac{2}{\sqrt{3}} \left( \frac{k}{\omega} - k + 1 \right) I_{dc}. $$  \hspace{1cm} (11)

The peak-ac-line current is expressed in p.u. as

$$i_{ac} = \frac{2}{\sqrt{3}} \left( \frac{k}{\omega} - k + 1 \right) i_{dc}.$$

when normalized by $I_{dc,nom} = \frac{P_0}{V_{dc,nom}}$. The maximum value of the peak-ac-line current across the entire operating speed range determines the current rating and is given by

$$i_{a,\text{rated}} = \max_{\omega \in [\omega_{\text{min}}, 1]} \frac{2}{\sqrt{3}} \left( \frac{k}{\omega} - k + 1 \right) i_{dc}.$$

Computation of this normalized current rating requires $i_{dc}$ to be expressed as a function of $\omega$.

Therefore, the normalized VA rating of the active rectifier is

$$v_a = v_{a,\text{rated}} i_{a,\text{rated}}$$  \hspace{1cm} (14)

where voltage and current rating are given by (9) and (13), respectively. Evidently, the normalized VA rating is a nonlinear function of $k$. An appropriate number of ac ports $k$, given by

$$k_{\text{opt}} = \arg\min_{k \in \mathbb{N}^+} v_a$$

minimizes the active rectifier VA rating. This concludes the design process for the proposed architecture.

III. APPLICATION-SPECIFIC DESIGN EXAMPLES

The integrated design process explained in the previous section is next used in two specific applications, first, for a wind-turbine-driven and then, for a gas-turbine-driven multiport PMSG. These design examples illustrate that knowledge of the power-versus-speed characteristics of turbines is critical for the design procedure. Differences in these characteristics lead to different optimum numbers of ac ports.

A. Wind Turbine

The generator output power in wind-turbine application is typically a cubic function of the operating speed [23]. As the dc-bus voltage is maintained constant, the per-unit power and the per-unit dc-bus current are the same. This leads to $i_{dc}$ equal
to $3\omega$. With $\omega_{\text{min}}$ set to 0.55 and using (9) and (13), the VA rating for the active rectifier is given by

$$v_a = \left(1 - \frac{k - 1}{k}\right)\frac{0.55}{\omega_{\text{min}}(0.55, 1)}\max\left\{\frac{2}{\sqrt{3}} \left(\frac{1}{\sqrt{k}} - k + 1\right)\right\} \cdot 3\omega^3. \quad (16)$$

Using this equation, the active rectifier VA rating for different number of ac ports is shown in Fig. 3 by the solid-blue curve. It is shown that the minimum VA rating is obtained for a four-ac-port PMSG. This design choice requires a VA rating of 0.78 p.u. for the active rectifier. In contrast, a conventional single-ac-port PMSG would require an active rectifier of 1.15 p.u. Thus, the proposed architecture reduces the power rating requirement of the active rectifier by 32%.

### B. Gas Turbine

The same design procedure is carried out for a gas-turbine driven PMSG. However, unlike for the wind turbine, no closed-form relation between the generated power and speed exists that can maximize the gas-turbine’s thermal efficiency at all operating speeds [5]. A look-up table between the speed and generated power that maximizes turbine efficiency is created using [5, Fig. 3]. This look-up table is used for operating speed/active rectifier output dc-output current relation. Computing the optimum number of ac ports is identical except that $\omega_{\text{min}}$ is set to 0.82 in this application. Fig. 3 shows the VA rating of the active rectifier as a function of the number of ac ports by the dashed-orange curve. A six-ac-port PMSG minimizes the VA rating to 0.54 p.u. In this case, the proposed architecture reduces the power rating requirement of the active rectifier by 53%.

### IV. PRACTICAL DESIGN CONSIDERATIONS

This section considers the effect of the generator stator resistance $R$ and synchronous inductance $L$ on the selection of the optimum number of ac ports to minimize the VA rating of the active rectifier. The presence of generator synchronous inductance leads to finite commutation time of the generator phase currents that makes the passive rectifier output voltage dependent on the load current, commonly expressed as the rectifier regulating characteristic [21]. A high synchronous inductance can cause a severe voltage droop at the passive rectifier output, which may counter the benefit of the proposed integrated generator rectifier architecture. Therefore, the influence of these nonideal behaviors of the passive rectifier must be considered in a practical design.

#### A. Design Equations Considering Generator Nonideal Characteristics

Assuming a low dc-side filter capacitor ripple current and a constant dc-bus current, each diode of the passive rectifier carries a constant current during noncommutation period. The passive rectifier may operate in Mode I, II, or III depending on the dc-bus current, synchronous impedance, and the generator back emf [21]. In comparison to Mode II or Mode III, Mode I is preferable due to a lower regulating characteristic. The synchronous impedance of the generator must be less than 50% of the base impedance to achieve a current commutation angle less than $\pi/3$ for the passive rectifier to stay in Mode I.

The passive-rectifier average dc-side voltage, given by (2), is modified to include the effects of the generator impedance. At an operating speed $\omega$ and dc-side load current $I_{dc}$, the average dc-side voltage is given by

$$V_{\text{passive}} = \sqrt{3}E_0 - \frac{3}{\pi}I_{dc}\omega_0 L - 2RI_{dc}. \quad (17)$$

Terms $\frac{3}{2}I_{dc}\omega_0 L$ and $2RI_{dc}$ correspond to the voltage drop due to the diode current commutation and the stator resistance, respectively. To regulate the dc-bus voltage at the value $V_{\text{dc,nom}}$, the active rectifier must generate a dc voltage

$$V_{\text{active}} = V_{\text{dc,nom}} - (k - 1)V_{\text{passive}} \quad (18)$$

where $V_{\text{dc,nom}} = (1 + \alpha)\sqrt{3}E_0$, $\alpha$ is the practical design margin of the dc-bus voltage. Normalizing both sides of (18) by the lower bound of the dc-bus voltage, $\sqrt{3}E_0$, the per-unit voltage supported by the active rectifier is

$$v_a = 1 + \alpha - \frac{k - 1}{k} \left(\omega - \frac{3}{\pi}I_{dc}\omega_0 L - 2RI_{dc}\sqrt{3}E_0 \right). \quad (19)$$

Equations (14) and (19) are used to calculate the active-rectifier VA rating as presented in Section II to determine the optimum number of ac ports for a specific application.

#### B. Practical Design Examples

Consider a 3-MW wind-turbine driven multiport PMSG that generates ac voltage at 20 Hz at the rated operating speed [4]. Connecting the PMSG to a 4.16-kV rms line-to-line voltage ac grid requires a dc bus of at least 5.88 kV. Allowing tolerances, $V_{\text{dc,nom}}$ is selected to be 6 kV. Selecting $\alpha$ to be 5%, $kE_0$ for the PMSG is 3.3 kV. The synchronous inductance and resistance for the PMSG are chosen as 2.16 mH and 90 mΩ, respectively. These parameters corresponds to an equivalent 0.05 p.u. synchronous impedance and 0.017 p.u. stator resistance, which can be achieved through appropriate generator design [24].

Fig. 4(a) shows the VA rating of the active rectifier as a function of the number of ac ports. A three-ac-port PMSG is the best choice for minimizing the active rectifier VA rating considering practical design.
the effects of the generator nonideal characteristics. Compared to the conventional single-port configuration, the active rectifier VA rating is reduced by 23.6%. Similarly, the proposed architecture shows promising system-level improvements when used in gas-turbine driven PMSGs. A 3-MW, 3.3-kV, 60-Hz gas-turbine driven PMSG is used as an example. The generator synchronous inductance and stator resistance are chosen as 0.7 mH and 90 mΩ, respectively. In this application, a three-port PMSG leads to a minimum VA requirement of the active rectifier, as shown in Fig. 4(b). The VA rating of the active rectifier is reduced by 38%.

From a practical design point of view, a low-inductance generator is beneficial. As shown by (19), a low inductance $L$ reduces the voltage requirement for the active rectifier, and consequently, the VA rating. However, low inductance may lead to challenges in the protection system. Special care in machine design and system protection must be considered for a safe operation.

V. STEADY-STATE PERFORMANCE OF INTEGRATED GENERATOR–RECTIFIER SYSTEM

This section discusses the key performance metric of the proposed architecture. First, power processed by the active rectifier is presented. A substantial reduction in the power processed by the active rectifier is achieved compared to the conventional single-port PMSG system. This reduction leads to a higher conversion efficiency or, alternatively, a lower converter loss. From a thermal design perspective, the ac–dc power converter size, weight, and cost can be reduced. The improvements are illustrated using the 3-MW, 6-kV design examples in Section IV.

A. Power Processed by Active Rectifier

Section II-C calculated the active rectifier VA rating, which is dependent on the maximum voltage and current rating. However, the power processed by the active rectifier is dependent on actual operating voltage and current at a specific operating speed. At any operating speed $\omega$, power processed by the active rectifier is the product of the output dc voltage and the output dc current

$$p_a(\omega) = v_a(\omega)i_{dc}(\omega).$$  \hfill (20)

While $v_a(\omega)$ is determined by (19), $i_{dc}(\omega)$ is decided by the application. For example, for a wind turbine, $i_{dc}$ equals $\omega^3$ and the optimum $k$ is three. Using these design choices, (20) is computed and plotted in Fig. 5(a) by the solid-orange line. The proposed system processes only a maximum of 1.2 MW out of 3 MW rated power, compared to 3 MW of the conventional single-port–full-power-electronics design. Similarly, for the gas turbine, $i_{dc}$ is computed using the look-up table and the optimum $k$ is four. The active rectifier processes only 1.2 MW throughout the operating speed range. A low VA rating leads to higher conversion efficiency, as illustrated in Section V-B.

B. Efficiency Improvement Due to Integrated Design

This section illustrates the improvement on conversion efficiency. Power switches are selected to support the required voltage and current rating of the active rectifier, as calculated in Section II. For the two-level active rectifier topology, each switch must be rated for $V_{a,\text{rated}} = v_a,\text{rated} V_{dc,\text{nom}}$ and $I_{a,\text{rated}} = i_a,\text{rated} I_{dc,\text{nom}}$. The rated values for different numbers of ac ports are summarized in Table I for both wind-turbine and gas-turbine applications.
The active switches are selected to have a 50% higher voltage and current rating than required to ensure a safe operation. For example, with wind-turbine system, for the case $k = 3$, the IGBTs rated for (1000 A, 6500 V) are selected to meet the requirement of (776.4 A, 3947.2 V). For $k = 1$, because none of the market-available IGBTs could meet the requirement, two (1000 A, 6500 V) IGBTs are connected in series to replace a single device to support the (606.1 A, 6000 V) requirement.

A lower voltage margin of 10% is sufficient for diodes on the passive rectifiers, as they switch at ac source frequency. Vishay VS-SD1100 C 1400-A, 2000-V rectifier diodes are chosen for the system.

The conversion losses include passive and active rectifier losses. For a passive rectifier, the conduction loss is the dominant component as these devices are switching at the ac source frequency. The power loss on each passive rectifier is

$$P_{\text{loss, passive}} = 2V_{\text{passive, FM}}I_{\text{dc,max}}$$

where $V_{\text{passive, FM}}$ is the forward-voltage drop of the diodes used in the passive rectifiers.

For the active rectifier, the power losses include that in the IGBTs (conduction and switching) and free-wheeling diodes (conduction and reverse recovery). The losses are estimated using [25, eqs. (18), (21), (27)] and [26, eqs. (10), (11)], with an assumption of unity power factor. Therefore, the total loss on the series-stacked ac–dc conversion system is

$$P_{\text{loss}} = (k - 1)P_{\text{loss, passive}} + 6(P_{\text{cond, IGBT}} + P_{\text{on, IGBT}} + P_{\text{off, IGBT}} + P_{\text{cond, diode}} + P_{\text{rr, diode}})$$

where $P_{\text{cond, IGBT}}$, $P_{\text{on, IGBT}}$, and $P_{\text{off, IGBT}}$ represent the conduction, turn-on, and turn-off losses on the IGBT, respectively. $P_{\text{cond, diode}}$ and $P_{\text{rr, diode}}$ stand for conduction loss and reverse-recovery loss of the free-wheeling diode. The normalized conversion loss is given by

$$p_{\text{loss}} = \frac{P_{\text{loss}}}{P_0} \%.$$ 

Table II summarizes the device data required to compute this efficiency. The passive diode voltage drop $V_{\text{passive, FM}}$ is 1.44 V. Fig. 6 shows the estimated conversion efficiency for the 6-kV, 3-MW dc bus system. For the wind-turbine case, Fig. 6(a) shows that three-port PMSG has the lower conversion loss for the entire operating speed range compared to the single-port PMSG. The reduction is 48% at the rated speed. This corroborates the optimum design presented in Section III-A. Similarly, for the gas-turbine system, Fig. 6(b) shows a loss reduction of 49% at the rated operating condition. Thanks to the loss reduction, cooling system size could be significantly reduced. Consequently, the system power density is increased.

The proposed integrated generator–rectifier system is compared to conventional architectures used for high-power ac–dc conversion such as a six-pulse diode rectifier, a two-level converter, and a NPC converter. Table III summarizes the results. Among the converters that provide dc-bus voltage control capability, the proposed system has the lowest switch-voltage rating, lowest conversion loss, and smallest amount of power processed on the active switches.

Recently, multiport PMSGs have been employed in several wind turbines. Each port powers an active converter and the converter outputs are connected in parallel [28]–[31]. This topology has the advantage of redundancy, i.e., the turbine can generate partially rated power in case some converters...
TABLE III
COMPARISONS OF DIFFERENT ARCHITECTURES FOR HIGH POWER
AC–DC CONVERSION

| Number of active switches | Active switch voltage rating | Power processed on active switches | Conversion loss | De-bus voltage control |
|--------------------------|-------------------------------|-----------------------------------|----------------|-----------------------|
| Integrated generator rectifier | Six-pulse diode rectifier | Two-level converter | Neutral-point clamped converter |
| 6                        | 0                             | 6                                 | 12             |
| Active switch            | < 0.4V<sub>dc</sub>           | V<sub>dc</sub>                     | 0.5 V<sub>dc</sub> |
| Voltage rating           |                               | P<sub>dc</sub>                     | P<sub>dc</sub> |
| Power processed on       |                               | 40% P<sub>dc</sub>                | 0              |
| active switches          |                               | P<sub>dc</sub>                     | 3.2%           |
| Conversion loss          |                               | 1.7%                              | 3%             |
| De-bus voltage control   | Yes                           | No                                | Yes            |

rectifier, respectively. I<sub>sd</sub> and I<sub>sq</sub> are the d–axis and q–axis currents, respectively. The power drawn by the active rectifier from the generator is given by

\[ P_{ac} = \frac{3}{2} \epsilon \omega I_{sd} - \frac{3}{2} I_{sd}^2 R. \]  

Similarly, the power delivered by the active rectifier to the dc bus and to the capacitor is given by

\[ P_{dc} = V_{active} (I_C + I_{load}) \]  

where I<sub>load</sub> is the current supplied to the dc bus, and I<sub>C</sub> is the capacitor current \( I_C = \frac{dV_{active}}{dt} \). Equating (26) to (27) for power balance, the active rectifier dc-side voltage dynamic is expressed as

\[ V_{active} \frac{dV_{active}}{dt} = -V_{active} I_{load} + \frac{3}{2} \epsilon \omega I_{sd} - \frac{3}{2} I_{sd}^2 R. \]  

Equations (24), (25), and (28) describe the dynamics of the active rectifier.

B. Control Architecture

Fig. 7 shows the cascaded control architecture. The inner loop is a current controller that regulates the active rectifier d– and q–axis currents. The d–axis current dynamics are governed by (24). The applied d–axis voltage is output of a proportional-integral (PI) controller with feed-forward terms as follows:

\[ V_{rd}^* = K_{pi} (I_{sd}^* - I_{sd}) + \int K_{ii} (I_{sd}^* - I_{sd}) dt + E + \omega_0 LI_{sq} \]  

where \( I_{sd}^* \) is the reference d–axis current. The proportional \( K_{pi} \) and the integral gain \( K_{ii} \) of the controller are chosen to ensure the loop stability and the desired transient response. A similar control law is applied to (25) for controlling the q–axis current

\[ V_{rq}^* = K_{pi} (I_{sq}^* - I_{sq}) + \int K_{ii} (I_{sq}^* - I_{sq}) dt - \omega_0 LI_{sd} \]  

where \( V_{rq}^* \) is the applied q–axis voltage and \( I_{sq}^* \) is the reference q–axis current. The reference value is set to zero for a unity displacement power-factor operation. A voltage controller generates the d–axis current command to regulate the active-rectifier dc-side voltage at the reference value \( V_{active} = V_{dc,nom} - V_{passive} \). Similar to the current controller, output of the voltage controller comprises output of a PI controller and feed-forward terms

\[ I_{sd}^* = K_{pu} (V_{active} - V_{active}) + \int K_{iv} (V_{active}^* - V_{active}) dt + \frac{2V_{active}}{3 \epsilon \omega} I_{load} + \frac{I_{sd}^2 R}{\epsilon \omega} \]  

where \( K_{pu} \) and \( K_{iv} \) are the proportional and integral gains of the voltage controller, respectively.

C. Controller Gain Selection

The current-controller gains are selected using the pole-zero cancellation technique. Specifically, the proportional gain \( K_{pi} \) and
Fig. 7. Cascaded control architecture for regulating the dc-bus voltage. Inner loop contains the $d-$ and $q-$ axis current controllers. Outer loop is a PI controller with feed-forward terms. The controller makes the active-rectifier dc-output voltage equal to the difference between the desired dc-bus voltage $V_{dc,\text{nom}}$ and the passive rectifier dc-side voltage $V_{\text{passive}}$.

and the integral gain $K_{ii}$ are chosen to be $-\frac{L}{\tau}$ and $-\frac{R}{\tau}$, respectively. This selection will allow the closed-loop current dynamic to be a first-order system with time constant $\tau$. The voltage controller gains are selected using the pole placement technique.

Consider the generator for gas-turbine application in Section IV-B with three-port configuration. Each port generates a back emf of 1.1 kV peak line-to-neutral at 60 Hz rated electrical frequency. The per-phase equivalent series resistance $R$ is 30 mΩ and the synchronous inductance $L$ is 233 µH. The switching frequency is selected to be 2 kHz to represent a realistic MW system. The time constant for the current controllers is chosen to be 1 ms, leading to the proportional and integral gains of $K_{pi} = -0.233$ and $K_{ii} = -30$, respectively. Placing the poles of the voltage controller at $-50$ and $-100$ leads to satisfactory dynamic performance, resulting in $K_{pv} = 1.25$ and $K_{iv} = 41.71$.

Stability and performance of the proposed architecture is illustrated using simulation results, as shown in Fig. 8. Initially, the dc-bus voltage is maintained at 6 kV while the dc-bus current is 500 A, equivalent to 3 MW power delivered to the load. The dc-bus current steps up to 650 A at 0.033 s, as shown in the middle plot. The active rectifier increases the amplitude of its ac-side current, as shown in the bottom plot. The dc-bus voltage has a small dip, equivalent to 1.1% of the nominal voltage value, and then, recovers to the nominal value of 6 kV.

VII. EXPERIMENTAL RESULTS

A three-phase, 48-pole, 350 r/min, 160 watt PMSG is used to demonstrate the functionality of the proposed architecture. Each phase comprises 12 coils in series. The stator is customized to externally access all the coil terminals, as shown by Fig. 9(a) and (b). The external access allows different number-of-ac-port configurations for the PMSG. Each coil has an inductance and resistance of 2.5 mH and 0.5 Ω, respectively. The back emf constant for each winding is 0.03125 V/r/min. The PMSG can be configured as one port, two port, three port, and four port through external connections. The one-port configuration is the conventional architecture, because all the power is processed by the active rectifier. Other configurations are realizations of the proposed architecture. The setup is shown in Fig. 9(c). For the multiport configuration, one ac port feeds a Texas Instrument High Voltage Motor Control and PFC Developer’s Kit, named Rec-1, which operates as the active rectifier and switches at 10 kHz. The rest of the ac ports are connected to passive rectifiers constructed from MT3516A-BP three-phase diode bridges. Each diode has a forward voltage drop of 1.2 V. DC outputs of all the rectifiers are serially connected to form the dc bus. A resistor load bank is used to draw power from the generator. A speed-controllable prime mover drives the PMSG. The dc-bus voltage is maintained at 260 V by a feedback controller implemented on the active rectifier for all operating speeds and loading conditions. The experimental results are generated using a wind
load profile, which is proportional to the cube of the generator operating speed.

As Rec-1 switches at 10 kHz, the time constant $\tau$ is chosen to be five times the switching period, $\tau = 0.5$ ms. The corresponding current-controller gains are $K_{pi} = -30$ and $K_{ii} = -6000$. These gains are converted to the discrete time domain using the Tustin transformation for implementation on the digital controller. With filter capacitors totaling 1650 $\mu$F on Rec-1, placing the voltage controller poles at $-5$ and $-10$ gives a satisfactory performance. This placement leads to $K_{pv} = 0.0297$ and $K_{iv} = 0.099$ in the continuous time domain.

A. VA Rating of the Active Rectifier

For each PMSG configuration, the active rectifier VA rating is calculated from the experimental setup by multiplying the maximum ac-line current and the maximum dc-side voltage of the active rectifier for all operating conditions, as shown in Fig. 10(a). Theoretically, the VA rating for the PMSG is calculated using (18) and (19). For this PMSG, a two-port configuration minimizes the active rectifier VA rating. With one-port configuration, the VA rating is 233 VA while for a two-port configuration, the VA rating reduces to 209 VA. Theoretical analysis shows that the VA ratings are 232 and 210 VA, respectively, for the two configurations. As the number of ac ports increases, the active rectifier VA rating increases significantly.

B. Power Processed by the Active Rectifier

Fig. 10(b) shows that the power processed by the active rectifier is accurately predicted by the theoretical analysis. The measured values are marked by blue-star and orange-circle markers while the theoretical calculations are presented by solid-blue and dashed-orange curves. Using a multiport PMSG, power processed by the active rectifier is reduced across the whole operating speed range. At the rated speed, 100 out of 166 watts are processed by the active rectifier, equivalent to 60%. This value matches well with the theoretical analysis, which is 59%.

C. Conversion Efficiency Improvement

Fig. 10(c) compares the conversion loss in the two configurations. The loss is measured by subtracting the measured...
Fig. 11. Bumpless dc-bus voltage is achieved under different load transient conditions. (a) Reducing dc-bus current. (b) Increasing dc-bus current.

Fig. 12. DC-bus voltage is maintained constant when the generator changes the operating speed. (a) Generator slows down. (b) Generator speeds up.

dc-bus power from the total generator output power. The experimental results present a couple of trends that align well with the theoretical calculations used to generate Fig. 6(a). First, the conversion system is more efficient at the rated operating condition compared to a low-speed operation. At the rated speed, majority of the power is processed by the passive rectifiers. At a lower operating speed, the active rectifier shares a dominant portion of the total power, and hence, the overall loss percentage increases. Second, the conversion loss is reduced across the whole operating speed range. The reduction ranges between 33% and 12% based on the operating speed.

D. Dynamic Performance

Fig. 11 shows the dynamic performance of the controller for keeping a constant dc-bus voltage under load-change conditions. The load initially draws 160 W at a dc-bus voltage of 260 V and then, drops to 130 W using mechanical switches, as shown in Fig. 11(a). Accordingly, the active rectifier ac-side current is reduced to lower the converted power, shown by the a-phase current waveform. As a result, the dc-bus voltage remains constant. The dynamic response is fast enough that the dc-bus voltage is maintained at 260 V, regardless of the power level. Fig. 11(b) shows a similar experiment but in an increasing loading scenario, the load is increased from 130 to 160 W. The controller increases the ac-side currents, as observed by the phase-a current waveform, to meet the new loading condition while holding a constant-voltage dc bus.

Fig. 12 shows the dc-bus voltage is kept constant when the generator varies its speed. The generator initially rotates at a speed of 350 r/min and ramps down to 320 r/min, shown in Fig. 12(a). The passive rectifier dc-bus voltage is reduced due to a lower generator speed. The active rectifier automatically increases its dc-side voltage to compensate for the drop, resulting in a bumpless-voltage dc bus. The same capability is shown for the ramping up case, Fig. 12(b).

E. Discussion on the Low-Power Prototype and the High-Power Simulation System

The experimental results have verified the feasibility of the proposed integrated generator-rectifier system and the sizing equations, however, at a low-power level. The simulation results based on realistic parameters have illustrated the capability of the
system at a megawatt-power level. The differences in the critical parameters and performance metrics of the two are summarized in Table IV. The switching frequency is significantly higher at the low-power level, leading to smoother ac currents. In addition, the experimental system has higher per-unit inductance and resistance. This deviation changes the optimal number of ac ports—two-port for the prototype versus three-port for the simulation, as shown in Figs. 4 and 10(a). Consequently, the VA rating of the active rectifier increases. Nevertheless, conversion losses still reduce significantly, as shown in the last row of Table IV.

| Table IV | Comparison Between the Low-Power Experimental System and the High-Power Simulation System |
|----------|------------------------------------------------------------------------------------------|
|          | Exp. low-power system | Sim. high-power system | Unit |
| P.E.     |                         |                         |      |
| IGBT     | PS21765                 | FZ800R45KL3B5NOSA2      | –    |
| $P_{sw}$ | 10                      | 2                       | kHz  |
| Voltage  | 600                     | 4500                    | V    |
| Current  | 20                      | 1600                    | A    |
| Generator |                         |                         |      |
| Rated power | 160                    | 3000000                 | W    |
| Rated voltage | 130                    | 3300                    | V    |
| $L$      | 0.16                    | 0.05                    | p.u. |
| $R$      | 0.037                   | 0.016                   | p.u. |
| $K_{pl}$ | -30                     | -0.233                  | V/A  |
| $K_{il}$ | -6000                   | -30                     | V/(A.s) |
| $\tau$  | 0.5                     | 1                       | ms   |
| $K_{pr}$ | 0.0297                  | 1.25                    | A/V  |
| $K_{ye}$ | 0.099                   | 41.71                   | A/(V.s) |
| Performance |                         |                         |      |
| $k_{opt}$ | 2                      | 3                       | –    |
| $\eta_{VA}$ | 0.89                    | 0.63                    | p.u. |
| Conv. losses reduction | 41.7                    | 54                      | %    |

VIII. CONCLUSION

This article presents a series-stacked ac–dc conversion architecture employing a multiphase PMSG with integrated power electronics. Exploiting the limited operating speed range, relevant in many applications such as wind-turbine and gas-turbine-driven systems, a regulated dc bus is created using multiple passive rectifiers and one active rectifier. The VA rating of the active rectifier is minimized by choosing an optimum number of the PMSG ac ports. This number depends on the application power-speed profile. Under ideal conditions, the optimum designs require a four-port for wind-turbine and a six-port for gas-turbine-driven generators. Nonideal characteristics of generators also influence the selection of the optimum number of ac ports. Conversion efficiency significantly improves by processing a portion of power on the highly efficient passive rectifiers. Experimental results from a laboratory-scale system based on a customized three-phase PMSG verify the theoretical analysis and the proposed design methodology. These results also validate that the proposed architecture reduces the conversion loss across the whole operating range and has satisfactory dynamic response. The integrated generator–rectifier system opens up multiple opportunities for future energy production by making dc-grid viable through a low-cost, high-efficiency, and high-reliability ac–dc conversion solution.

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