Encoderless Flux Vector Oriented Control of Brushless Doubly-Fed Reluctance Generators

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Abstract—An angular position and velocity observer-based sensorless flux vector-oriented control scheme for a prominent doubly-fed reluctance generator has been presented and experimentally validated. This technology allows the use of the same partially rated power converter as the traditional slip-ring doubly-excited induction generator, while offering competitive performance with added cost benefits of high reliability and maintenance-free operation afforded by its brushless construction. The controller viability has been demonstrated on a laboratory machine prototype for emulated variable speed and loading conditions similar to those encountered in wind turbines.

NOMENCLATURE

BDFRG	Brushless Doubly-Fed Reluctance Generator
BDF(IG)	Brushless Doubly-Fed (Induction) Generator
DFIG	Wound Rotor Doubly-Fed Induction Generator
LVRT	Low-Voltage-Ride-Through
MPPT	Maximum Power Point Tracking
WECS	Wind Energy Conversion Systems
DT(P)C	Direct Torque (Power) Control
FOC	Flux (Field) Oriented Control
VOC	Primary Voltage Vector Oriented Control
$v_{p,s}$	Primary, secondary winding phase voltages [V]
$i_{p,s}$	Primary, secondary winding currents [A]
$R_{p,s}$	Primary, secondary winding resistances [Ω]
$L_{p,s}$	Primary, secondary 3-phase inductances [H]
$\lambda_{p,s}$	Primary, secondary winding flux linkages [Wb]
λ_m, L_m	Mutual flux [Wb] and 3-phase inductance [H]
$\omega_{p,s}$	Primary, secondary winding frequencies [rad/s]
p_p, p_s	Primary, secondary winding pole-pairs
p_r	Number of rotor poles ($= p_p + p_s$)
ω_{rm}, n_{rm}	Rotor angular velocity [rad/s], speed [rev/min]
ω_{syn}	Synchronous speed $= \omega_p / p_r$ [rad/s]
P_m	Mechanical (shaft) power [W]
$P_{p,s}$	Primary, secondary mechanical power [W]
T_e	Electro-magnetic torque [Nm]
P, Q	Primary real [W] and reactive [VAR] power
σ	Leakage factor $= 1 - L_m^2 / (L_p L_s)$

I. INTRODUCTION

A conventional DFIG [1] has been a widely adopted option of many large-scale wind turbine manufacturers [2], [3] by virtue of its good overall performance, low-cost fractional

power electronics, and absence of expensive rare earth permanent magnets with market supply chain volatilities [4]. In WECS, the operating speed ranges are relatively narrow (e.g. about 2:1 or so) allowing one to utterly exploit the DFIG slip-power recovery property through cost savings of the typically 70% derated bi-directional power converter [1]. The latter has also been reported to feature lower failure rates than its full-size counterpart used for synchronous generator based WECS [5]. However, these capital and running cost benefits may be offset by the DFIG main drawbacks, the compromised reliability and regular servicing of brush gear, as well as hardware protection complexities to satisfy the grid integration codes in making the small inverter LVRT compliant [1], [6].

The above DFIG limitations can be mostly eliminated with the BDFG technologies, while retaining all of the claimed advantages [6]–[8]. The BDFG can not only offer further cost cuts by the high reliability and maintenance-free operation of brushless construction [3], but the superior LVRT characteristics without a crowbar too due to the proportionally larger impedances, and hence reduced fault currents, than the equivalent DFIG [6], [9], [10]. Apart from the target WECS, stand-alone wind [11] and/or ship shaft power generation applications [12] are feasible.

The BDFG has a standard dual 3-phase winding stator structure with the primary (power) winding being directly connected to the utility grid, and the secondary (control) winding being usually fed via a double ‘back-to-back’ partially-rated bridge, as in the DFIG case. There are two quite distinct BDFG types, the one with a reluctance rotor, known as the BDFRG (Fig. 1) [13], and the other with a ‘nested’ cage rotor, called the BDFIG [14]. In order to achieve magnetic interaction between the two stator windings of different pole numbers and applied frequencies, the rotor poles must total the stator pole-pairs [13], [14]. This means that with the same number of rotor poles and winding frequencies, the BDFG would turn at around half the DFIG speed rendering it naturally a medium-speed machine and avoiding the need for the troublesome high-speed stage of the 3-stage gearbox in WECS [3]. A modern reluctance rotor can provide the higher efficiency prospects [15], less parameter dependent dynamic modeling [14], [16], and intrinsically decoupled control of torque and reactive power [17]–[19] unlike the BDFIG [20], [21]. The BDFRG control is thus the subject of this paper.

Fig. 1. A block-scheme of the sensorless speed FOC configuration for the space-vector-PWM inverter-fed BDFRG.

A VOC approach for the BDFRG was proposed and experimentally verified, albeit for the unloaded machine, in [17]. The follow-up theoretical preliminaries on this concept have not been practically validated [18], [19]. The same applies to the sliding mode control theory in [22]. On the other hand, DTC has been implemented in sensor [23] and sensor-less speed modes [19] but supported with unsatisfactory no-load test results as in [17]. A much improved response has been obtained by minimising the machine parameter dependence and sensitivity issues of this scheme in the alternative DTC, where the measurable primary reactive power is controlled instead of the estimated secondary flux [24], [25]. A complete parameter-freedom has been further achieved in its DPC concurrent [26]. However, the experiments discussed in [24]–[26] were done at fixed BDFRG loads of little interest to real WECS. Although they can be credited with ease of stator frame implementation without rotor position or velocity inputs, the hysteresis DT(P)C algorithms in [24]–[26] are all executed at variable switching rates and exhibit a generally worse harmonic content than the VOC, and especially its FOC correlative.

Significant contributions to the early VOC research [17] have been recently made with the comparative development and comprehensive practical studies of the two robust VOC and FOC techniques for the adjustable speed BDFRM (e.g. motoring) [27], [28] and the BDFRG [29], [30] under both constant [29] and variable loading conditions [27], [28], [30]. Similar realistic computer simulations of WECS using a 2 MW BDFRG design from [7] have been documented in [8].

The majority of the existing methods for the BDFRG(M) require a shaft position sensor for current and/or speed control

as well as to provide the drive train velocity information for the MPPT in WECS [1]. Sensorless operation would bring an extra degree of reliability and operation & maintenance cost reductions [5]. However, these conveniences often come with some performance trade-offs by introducing the inevitable machine model reliance necessary for the position or speed estimation as demonstrated in the DTC case [19]. An exception is the latest development of a viable speed sensorless FOC scheme in [31], which has been shown to possess good disturbance rejection capabilities and smooth response using the maximum torque per inverter ampere strategy [18]. This paper is an extension to the work in [31] by presenting the principles and experimental results of reactive power control implementation on a custom-built BDFRG for speed dependent WECS-alike loading profiles.

II. BDFRG OPERATING PRINCIPLES

The electro-mechanical energy conversion in the machine takes places under the following angular velocity and pole conditions with the mechanical power relationships reflecting the participation of each winding [16], [18]:

$$\omega_{rm} = \frac{\omega_p + \omega_s}{p_p + p_s} = \left(1 + \frac{\omega_s}{\omega_p}\right) \cdot \frac{\omega_p}{p_r} = \left(1 + \frac{\omega_s}{\omega_p}\right) \cdot \omega_{syn} \quad (1)$$

$$P_m = T_e \cdot \omega_{rm} = \frac{T_e \cdot \omega_p}{p_r} + \frac{T_e \cdot \omega_s}{p_r} = P_p + P_s \quad (2)$$

where $\omega_s > 0$ if $\omega_{rm} > \omega_{syn}$, else¹ $\omega_s < 0$ and $\omega_{rm} < \omega_{syn}$. Whereas, $\omega_s = 0$ (i.e. DC secondary) refers to the synchronous

¹The ‘negative’ frequency at sub-synchronous speeds means the opposite phase sequence of the secondary to the primary winding i.e. ‘clockwise’ rotation of the respective vectors in the $d_s - q_s$ frame as indicated in Fig. 2.

speed operation ($\omega_{rm} = \omega_{syn}$) as with a classical $2p_r$ -pole wound field turbo-machine.

It could be easily shown using (1) that if a variable speed range of 2:1 is required in WECS, the corresponding frequency ratio should be $\omega_p/\omega_s = 3$, which implies the inverter real power rating of circa $P_s \approx 0.21P_m$ according to (2).

III. FOC MODELLING ASPECTS

The space-vector equations for the primary and secondary windings in rotating reference frames (Fig. 2) using standard notation and *motoring* (BDFRM) convention are [16]:

$$\left. \begin{aligned} \underline{v}_p &= R_p \underline{i}_p + \frac{d\lambda_p}{dt} = R_p \underline{i}_p + j\omega_p \cdot \underbrace{(L_p \underline{i}_p + L_m \underline{i}_{sm}^*)}_{\lambda_p} \\ \underline{v}_s &= R_s \underline{i}_s + \frac{d\lambda_s}{dt} = R_s \underline{i}_s + j\omega_s \cdot \underbrace{(L_s \underline{i}_s + L_m \underline{i}_{pm}^*)}_{\lambda_s} \end{aligned} \right\} \quad (3)$$

where $\underline{i}_{pm}$ and $\underline{i}_{sm}$ are the magnetically coupled (magnetizing) current vectors of the actual primary ($\underline{i}_p$) and secondary ($\underline{i}_s$) current counterparts rotating at different velocities (Fig. 2). This peculiar frequency modulating action of the rotor manifests itself through the following important vector equalities for the sensorless controller design: $\underline{i}_{pm} = \underline{i}_p = \underline{i}_p e^{j\varepsilon}$ and $\underline{i}_{sm} = \underline{i}_s = \underline{i}_s e^{j\gamma}$ in the corresponding $d-q$ frames as illustrated in Fig. 2 [16]. Given (1), such frame choice facilitates control as the secondary dq currents are then DC.

The flux FOC forms of (3), and the respective torque and 3-phase reactive power expressions, now become [27]–[31]:

$$\lambda_p = \underbrace{L_p \underline{i}_{pd} + L_m \underline{i}_{sm_d}}_{\lambda_{pd}=\lambda_p} + j \cdot \underbrace{(L_p \underline{i}_{pq} - L_m \underline{i}_{sm_q})}_{\lambda_{pq}=0} \quad (4)$$

$$\lambda_s = \underbrace{\sigma L_s \underline{i}_{sd} + \lambda_{md}}_{\lambda_{sd}} + j \cdot \underbrace{(\sigma L_s \underline{i}_{sq} + \lambda_{mq})}_{\lambda_{sq}} \quad (5)$$

$$= \sigma L_s \underline{i}_s + \underbrace{\frac{L_m}{L_p} \lambda_p^*}_{\lambda_m} = \sigma L_s \underline{i}_s + \underbrace{\frac{L_m}{L_p} \lambda_p}_{\lambda_m} \quad (6)$$

$$T_e = \frac{P_p p_r}{\omega_p} = \frac{3p_r}{2} \lambda_p \underline{i}_{pq} = \frac{3p_r L_m}{2L_p} \lambda_p \underline{i}_{sq} = \frac{3p_r}{2} \lambda_m \underline{i}_{sq} \quad (7)$$

$$Q = \frac{3}{2} \omega_p \lambda_p \underline{i}_{pd} = \frac{3}{2} \frac{\omega_p \lambda_p}{L_p} (\lambda_p - L_m \underline{i}_{sd}) \quad (8)$$

Note that, with the grid-locked primary winding and approximately constant λ_p , and thus λ_m , the FOC frame-flux vector alignment in Fig. 2 leads to inherently decoupled control of T_e and Q through $\underline{i}_{sq}$ and $\underline{i}_{sd}$ by removing the problematic VOC cross-coupling terms from (7) and (8). If left uncompensated, the latter can severely deteriorate the VOC response quality, particularly with smaller machines, as experimentally verified in [27]–[30]. The FOC approach is therefore more generic and applicable to a range of machine sizes from this point of view. Furthermore, it is amenable to sensorless control as shown in the sequel. Another important control implication of $T_e \sim \underline{i}_{sq}$ and $Q \sim \underline{i}_{sd}$ is that $\underline{i}_{sq}$ and $\underline{i}_{sd}$ can serve as the reference outputs of the speed and Q loops avoiding the use of (7) and (8) and handling the associated parameter uncertainties

Fig. 2. A FOC phasor diagram with the key vectors and reference frames.

through PI tuning (Fig. 1). The controller robustness of the original implementation in [31] can be elevated in this way.

IV. SENSORLESS FOC DESIGN

The $\lambda_p = \lambda_p e^{j\theta_p}$ stationary frame components (Fig. 2) for the Y-connected primary winding with an isolated neutral point and ‘ABC’ phase sequence are calculated from the line voltage and current measurements using (3) as follows:

$$\left. \begin{aligned} \lambda_\alpha &= \int (v_\alpha - R_p i_\alpha) dt = \int \left(\frac{v_{AB} + v_{AC}}{3} - R_p i_\alpha \right) dt \\ \lambda_\beta &= \int (v_\beta - R_p i_\beta) dt = \int \left(\frac{v_{BC}}{\sqrt{3}} - R_p \frac{i_A + 2i_B}{\sqrt{3}} \right) dt \end{aligned} \right\} \quad (9)$$

The generated $\lambda_{\alpha\beta}$ estimates are input to a conventional PLL to filter out the transducer DC offset effects and noisy measurements in order to get the most accurate θ_p as the FOC foundation. This angle is then used to find the d_p-q_p currents, $\underline{i}_{pd}$ and $\underline{i}_{pq}$, and the rotor angle (θ_r) from the angular position version of (1) i.e. $\theta_r = p_r \theta_{rm} = \theta_p + \theta_s$, as shown in Fig. 1.

The d_s -axis position (θ_s) above is identified using (4) and the fact that $\underline{i}_{sm} = \underline{i}_s$ (Fig. 2):

$$\left. \begin{aligned} L_p \underline{i}_{pd} + L_m \underline{i}_{sm_d} &= \lambda_p \implies \underline{i}_{sd} = \underline{i}_{sm_d} = \frac{\lambda_p - L_p \underline{i}_{pd}}{L_m} \\ L_p \underline{i}_{pq} - L_m \underline{i}_{sm_q} &= 0 \implies \underline{i}_{sq} = \underline{i}_{sm_q} = \frac{L_p \underline{i}_{pq}}{L_m} \end{aligned} \right\} \quad (10)$$

Notice that the feedback currents in Fig. 1 are directly determined by (10) regardless of θ_s or θ_r errors. In addition, θ_s can now be obtained from the measured secondary currents by applying the obvious angular relationships from Fig. 2:

$$\theta_s = \tan^{-1} \frac{\underline{i}_{s\beta}}{\underline{i}_{s\alpha}} - \tan^{-1} \frac{\underline{i}_{sq}}{\underline{i}_{sd}} \quad (11)$$

$$= \tan^{-1} \frac{i_a + 2i_b}{\sqrt{3}i_a} - \tan^{-1} \frac{L_p \underline{i}_{pq}}{\lambda_p - L_p \underline{i}_{pd}} \quad (12)$$

Therefore, apart from R_p (through λ_p) with smaller machines, the use of (12) only needs the L_p knowledge. Yet, as θ_s can still be affected by measurement noise, the raw $\theta_r = \theta_p + \theta_s$ values are passed through a common load-model based PI observer [31], [32] to eradicate erroneous estimates for accurate $\hat{\omega}_{rm}$ predictions. The filtered $\hat{\theta}_r$ is fed back to further enhance the quality of $\hat{\theta}_s$ estimation, the emanating PWM signals, and in turn the controller response.

Fig. 3. Oscillograms of the steady-state currents in two phases of the shorted secondary winding for the unloaded BDFRG at ≈ 730 rev/min.

TABLE I
THE BDFRG PARAMETERS AND RATINGS

Rotor inertia $[J]$	0.2 kgm ²
Primary resistance $[R_p]$	11.1 Ω
Secondary resistance $[R_s]$	13.5 Ω
Primary inductance $[L_p]$	0.41 H
Secondary inductance $[L_s]$	0.57 H
Mutual inductance $[L_m]$	0.34 H
Rotor poles $[p_r]$	4
Primary power $[P_r]$	1.6 kW
Rated speed $[n_r]$	950 rev/min
Stator currents $[I_{p,s}]$	2.5 A rms
Stator voltage $[V_p]$	400 V rms
Stator frequency $[f_p]$	50 Hz
Winding connections	Y/Y
Stator poles $[p/q]$	6/2

V. LABORATORY TESTS

The BDFRG was started as a slip ring induction machine up to the steady no-load speed (Fig. 3), and the inverter was then enabled on the fly. The experimental results were generated by running the sensorless algorithm in Fig. 1 on a dSPACE® DS1103 platform at 2.5 kHz. The specifications of the BDFRG prototype are summarised in Table I. These data have been used to create the following shaft torque-speed profile to emulate the MPPT characteristic of WECS in the base speed region [1]:

$$T_L = -\frac{P_r}{\omega_r} \cdot \left(\frac{n_{rm}}{n_r}\right)^2 \approx -16 \cdot \left(\frac{n_{rm}}{950}\right)^2 \text{ Nm} \quad (13)$$

This expression is implemented using a commercial Parker® DC drive operated in torque mode. The other relevant details about the BDFRG test facility can be found in [24]–[31].

The results in Fig. 4 demonstrate an excellent tracking of the ramp speed reference trajectory in a limited range of [550–950] rev/min around the synchronous speed (750 rev/min). The effectiveness of the machine sensorless operation is clearly verified in either synchronous, super-synchronous or sub-synchronous speed modes and with marginal speed estimation

errors under both steady-state and transient conditions. The captured Q waveform properly follows the desired set-point showing overall immunity to the P variations and providing convincing evidence of the robustness afforded by the inherently decoupled FOC nature as expected from (7) and (8).

The observer low-pass filtering abilities are more than evident from the speed error plot in Fig. 4, which represents the deviations of the observer estimates ($\hat{\omega}_{rm}$) from the actual encoder measurements (ω_{rm}). Although the raw θ_r can be notably noisy, much the same as recorded in [31] but not in this paper for space reasons, a considerable improvement in accuracy is achieved by processing θ_r considering the virtually overlapping trend of the corresponding $\hat{\omega}_{rm}$ and ω_{rm} traces over the entire speed range. Such an impressive sensorless controller response essentially comes from the bulk of the quality estimates produced by the closed-loop configuration of both the position estimator itself and the high-performance observer as its central part.

The measured secondary current waveforms in Fig. 5 illustrate the phase sequence reversal while the BDFRG is riding through the synchronous speed from 950 rev/min to 550 rev/min. At super-synchronous speeds, the $d_s - q_s$ frame, and the accompanying secondary vectors including $\hat{i}_s$, rotate in a positive (anti-clockwise) direction indicated by $\omega_s > 0$ in (1). In sub-synchronous speed mode, however, the secondary phase sequence is opposite to the primary with $\hat{i}_s$ now rotating clock-wise when $\omega_s < 0$ in (1). Notice that $\hat{i}_s$ becomes stationary at synchronous speed (750 rev/min) as the secondary currents are then DC i.e. $\omega_s = 0$ in (1).

Finally, Fig. 6 is presented to reinforce the absence of cross-coupling not only in the Q response to ramp variations in P , but also the other way around in case of a sudden step change in Q reference, for the machine operating at 1000 rev/min. Both the P and Q responses are visibly smooth and show no apparent signs of distortion pointing out again the excellent disturbance rejection properties of the controller.

VI. CONCLUSIONS

A field-oriented speed and reactive power control algorithm for sensorless operation of the BDFRG has been validated by tests on a small machine prototype under wind turbine-alike loading conditions emulated in a laboratory environment. The scheme under consideration has been previously proposed by the author(s) in their latest work where the reactive power control has not been demonstrated. This gap has been filled by the experimental results presented in this paper.

The control system design is structured around the rotor position and speed estimation technique with a standard Luenberger-type load model based observer playing a pivotal role. This approach has allowed the controller to effectively substitute the encoder measurements by providing high-quality estimates of the angular position and velocity. A smooth and robust response has been consequently achieved in a narrow speed range where the variable secondary frequencies are low and down to zero at synchronous speed. Such good performance can be attributed to the rotor position independent

Fig. 4. Experimental verification of the BDFRG sensorless speed control.

Fig. 5. The secondary currents showing a phase sequence reversal from super to sub-synchronous speed mode as captured on the dSPACE® desktop.

current control feedback coupled with the relatively light reliance on the machine parameters of the entire estimation procedure.

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Fig. 6. Super-synchronous sensorless operation demonstrating excellent disturbance rejection abilities and smooth response of the decoupled FOC to P and/or Q variations at 1000 rpm.

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