Dual stator winding variable speed asynchronous generator: magnetic equivalent circuit with saturation, FEM analysis and experiments

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Abstract. The authors carried out a theoretical and experimental study of dual stator winding squirrel cage asynchronous generator (DSWA) behaviour in the presence of saturation regime (non-sinusoidal) due to the variable speed operation. The main aims are the determination of the relations of calculating the equivalent parameters of the machine windings, FEM validation of parameters and characteristics with free FEMM 4.2 computing software and the practice experimental tests for verifying them. Issue is limited to three phase range of double stator winding cage-asynchronous generator of small sized powers, the most currently used in the small adjustable speed wind or hydro power plants. The tests were carried out using three-phase asynchronous generator having rated power of 6 [kVA].

1. Introduction
Despite its simple and robust construction, the motion control for this type of generators should take into account the complexity of the dynamic model which is nonlinear and variable in time and that the physical parameters of the machine are not always known with great precision [1-3]. Usually, the electric machines are designed to be supplied in sinusoidal regime. The presence of higher harmonics will have as result the appearance of a deforming and saturation regime in the machine, with adverse effects in general in its operation [4], [5]. The appearance of the deforming regime in the machine is inevitable because any static frequency converter based on semiconductor technique produces voltages or currents, which contain, in addition to fundamental and harmonic, higher odd time harmonics.

The introduction of distributed generation through renewable sources of energy has opened a challenging area for power engineers. As these sources are intermittent in nature, variable speed electric generators are employed for harnessing electrical energy from these sources. However, power electronic control is required to connect these sources to the existing grid [6]. A wind turbine can be designed for a constant speed or variable speed operation. Variable speed wind turbines can produce 8% to 15% more energy output as compared to their constant speed counterparts, however, they necessitate power electronic converters to provide a fixed frequency and fixed voltage power to their loads. Most turbine manufacturers have opted for reduction gears between the low speed turbine rotor and the high speed three-phase generators. Direct drive configuration, where a generator is coupled to the rotor of a wind turbine directly, offers high reliability, low maintenance, and possibly low cost for certain turbines [6-11].
For small to medium power wind or hydro turbines, permanent magnet generators and squirrel cage asynchronous generators are often used because of their reliability and cost advantages. For certain high power wind or hydro turbines, effective power control can be achieved with double PWM (pulse width modulation) converters which provide a bidirectional power flow between the turbine generator and the utility grid [12-17].

2. Equivalent Circuit with Considering the Saturation

One of the main goals is the reduction of the necessary reactive excitation power by the arrangement of the load windings versus the excitation windings. Some arrangements of the windings can establish an internal positive load current reaction and thus can determine the desired effect. Such arrangements can be obtained with the excitation winding and load winding displaced one from another by a certain space angle $\alpha$, where the rotor winding and the consumer impedance are also shown. Because of this complexity, a reconsideration of the main flux linkage saturation effect is called for in the design of the machine and in the development and practical implementation of speed/torque control algorithms. To avoid deep magnetic saturation in the stator and rotor cores and rotor and stator teeth, magnetic design methodologies have been suggested both for the dual stator-winding and brushless doubly fed asynchronous machines [9], [10].

In the literature there are known various mathematical models associated to asynchronous machines fed by static frequency and voltage converters.

In the case of magnetic saturation, the mmf of the iron $U_{Hfe}$ is no longer negligible with respect to the mmf of the air-gap $U_{Hg}$, a deformation of the magnetic curvature takes place, and the polar coverage factor depends on the saturation factor $k_s$ [17]:

$$k_{st} = \frac{U_{Hs} + U_{Hts} + U_{Htr}}{U_{Hs}},$$ \hspace{1cm} (1)

for the teeth saturation and a new proposal:

$$k_{st,sp} = \frac{U_{Hs} + U_{Hts} + U_{Htr} - U_{Hys} - U_{Hyr}}{U_{Hs}},$$ \hspace{1cm} (2)

for the teeth and yoke saturation, where $U_{Hts}$ and $U_{Htr}$ represent the mmf in the stator and rotor teeth, and $U_{Hys}$ and $U_{Hyr}$ represent the mmf in the stator and rotor yoke.

In the references [17], [18] it was shown that if there is yokes saturation (both stator and rotor), this influences in the opposite way the distribution of the flux density in the air-gap. The teeth saturation leads to a bending of the flux density curvature ($B_{1,flat}$), and the yoke saturation leads to a sharper peak ($B_{1,peak}$) (Figure 1).

![Figure 1. Sinusoidal, flat and peak air-gap flux density](image)

If the level of teeth and yoke saturation is identical, then the distribution of the flux density stays sinusoidal ($B_1$).
Considering the air-gap distribution of the flux density sinusoidal, sometimes with the $3^{rd}$ harmonic included, the yoke flux density will be computed in 4-5 points, and the corresponding mmf is calculated through digital integration using the Simpson formula:

$$\int H_y dl = \frac{D_y \cdot \pi}{2p} \cdot \frac{1}{c_x} ,$$  \hspace{1cm} (3)

with

$$c_x = f(B_y) \right) .$$  \hspace{1cm} (4)

Usually, the magnetization curvatures $H = f(B)$ are given by the producer up to a certain value of the flux density $B$ (for instance 1, 8 - 2T). There are areas in which the flux density is higher than the maximum value obtained in the magnetization curvature very often when designing modern electrical machines, which require high torque densities with respect to the mass and the volume of the machine. A method of extrapolation of the magnetization curvature is proposed, and this method is based on the property of relative magnetic permeability of the magnetic material $\mu_r$ to tend to 1 and is also based on the property of differential permeability $\mu_d$ to tend to 0 when the flux density becomes infinity:

$$\mu_r = \frac{B}{\mu_0 \cdot H} ,$$  \hspace{1cm} (5)

$$\mu_d = \frac{dB}{\mu_0 \cdot dH} .$$  \hspace{1cm} (6)

The continuity of both the permeability $\mu_r$ and $\mu_d$ is imposed, in the last points of the given magnetization curvature. The intensity of the magnetic field $H_x$ in a random point may be written as:

$$H_x = H_{ref end} + \left[ (B_x - B_{ref end}) + B_0 \left( e^{a_1 B_x} - e^{a_1 B_{ref end}} \right) \right] \cdot \frac{1}{\mu_0} ,$$  \hspace{1cm} (7)

where $H_{ref end}$ and $B_{ref end}$ are the intensity of the magnetic field, and the flux density respectively in the last point from the characteristic, and $a_1$ and $B_0$ are variables calculated so that the function is continuous and convergent. They have the expressions:

$$a_1 = \ln[(1-\mu_d)(1-\mu_d^2)] \cdot \frac{1}{x_1 - x_2} ,$$  \hspace{1cm} (8)

$$x_1 = \frac{B_{n-2} + B_{n-1}}{2} ,$$  \hspace{1cm} (9)

$$x_2 = \frac{B_{n-1} + B_n}{2} ,$$  \hspace{1cm} (10)

$$\mu_d = \mu_0 \cdot \frac{dH_{ref}}{dB_{ref}} ,$$  \hspace{1cm} (11)

$$\mu_{d1} = \mu_0 \cdot \frac{H_{ref n-1} - H_{ref n-2}}{B_{ref n-1} - B_{ref n-2}} ,$$  \hspace{1cm} (12)

$$B_0 = (\mu_{d2} - 1) \cdot e^{-a_1 x_1} \cdot \frac{1}{a_1} .$$  \hspace{1cm} (13)

In Figure 2 the dependence between the flux density and intensity of the magnetic field (extended magnetization curvature) is presented.
Figure 2. Magnetization curves: Cref – references points, C0 – interpolation and extrapolation on original curve \( w_{2b}=0 \), \( k_{Fe}=1 \), C1 – interpolation and extrapolation on modified curve with \( w_{2b}=1 \), \( k_{Fe}=0.97 \), C2 – interpolation and extrapolation on modified curve considering \( w_{2b}=2 \), \( k_{Fe}=0.97 \)

In the case of yokes saturation at the nominal frequency, losses are very high. Talking about the low frequency generating electrical generators, at which the shaft is coupled directly to the wind turbine, there is a possibility of yoke saturation without any significant increase of iron losses. Flux density in the yoke is calculated with the relation:

\[
B_{ref} I = B_{ref} + \mu_0 \cdot H_{ref} \cdot \left( \frac{w_{2b}}{k_{Fe}} \right),
\]

where

\[
w_{2b} = \frac{h_{st}}{h_y},
\]

the depth of the slot is denoted by \( h_{st} \), and the length of the yoke where the flux density is computed is denoted by \( h_y \). For the case of the teeth:

\[
w_{2b} = \frac{w}{b},
\]

\( w \) being the length of the slot and \( b \) the length of the teeth.

An iterative solving of the circuit is made, starting from the linear inductance around the nominal load, the current is computed and then the curvatures of the inductances are being iteratively computed.

3. FEM Analysis

An approach that includes the influence of magnetic saturation and iron loss using finite-element analysis in the performance prediction of the twin stator winding asynchronous machine was set. The proposed finite-element model provides very good steady-state predictions and can be used for the sizing and design optimization of the machine.
The asynchronous machine saturation could be increased at low frequency (required in direct driving) without notably iron losses increases in order to have a larger slot area required in two stator winding or for aluminium winding which could replace the copper winding in low cost machines.

The analytical model should be improved for high saturation magnetic core. Increasing the stator slot with 40% will increase the saturation of the stator core (Figure 3) and will change the line field distribution. The leakage inductances computed analytically could be validated through a virtual short circuit using ac finite element analysis where the stator winding currents are given (rated or higher values) at rated frequency and the rotor bar currents are computed as eddy currents (Figure 4). The end coil leakage inductances are not considered in 2D FEM approach.

![Figure 3. The magnetic field distribution: original 6 kVA, 8 poles, 15 Hz rated frequency induction generator](image1)

![Figure 4. Virtual short circuit test using AC finite element analyses (f=fN=15Hz, ImA = 7A peak current in main winding, ImB=ImC=-3.5A)](image2)
No load (zero rotor current) FEM investigations at standstill, with dc currents for star connection lead to $I_{Ac} = I = -2I_{Be} = -2I_{Ce}$. Figure 5 presents the air-gap flux density (produced by main and excitation currents) and Figure 6 shows the normal air-gap magnetic field strength.

4. Experimental Results
To prove the above design methods, a 6kVA 400V/415 rpm prototype of the DSWA generating system has been developed (Figure 7). The prime mover is simulated by a three-phase cage-type asynchronous machine driven by an inverter of ABB ACS 800-11. In experiments, the value of the auxiliary excitation capacitors is 35 μF, and the value of the filter inductances is 23 mH.
Through the experimental tryouts it is desired, in a first step, the computation of the DSWA parameters and characteristics for the stationary regime and their comparison with the values obtained through finite element analysis. In Figure 8 the equivalent phase scheme of the machine is presented. Resistances $R_m$ and $R_e$ are measured in DC current and the inductances will be computed through no load (real and ideal) and short circuit methods.

![Figure 8. Equivalent phase scheme of the DSWA](image)

### 3.1. No load probe

The equivalent phase scheme for the no load methods is presented in Figure 9. The equations which characterize the functioning in these conditions are given as follows:

$$V_{mo} - (R_m + jX_{cm}) \cdot L_{1mo} = V_{eo}', k_e' \cdot V_{eo}' = V_{m}', \tag{17}$$

$$V_e' - (R_e' + jX_{ce}) \cdot L_{le}' = V_{2m}', \tag{18}$$

$$k_e \cdot V_e' - (R_e' + jX_{ce}) \cdot k_e' \cdot L_{le}' = V_{2m}' \tag{19}$$

$$k_e [V_e' - (R_e' + jX_{ce}) \cdot L_{le}'] = V_{2m}' \tag{20}$$

### 3.2. Short circuit probe

Equivalent phase scheme in short circuit (Figure 10) and the equations are given as follows:

$$V_{msc} - (R_m + jX_{cm}) \cdot L_{1msc} = V_{esc}', k_e \cdot V_{esc}' = V_{m}', \tag{21}$$

$$V_{mo} - (R_m + jX_{cm}) \cdot L_{1mo} = V_{esc}', \tag{22}$$

$$V_{msc} - (R_m + jX_{cm}) \cdot L_{1msc} = V_{esc}', \tag{23}$$

$$V_m - (R_m + jX_{cm}) \cdot L_{1sc} = \frac{V_m}{V_{esc}} \cdot [V_m - (R_m + jX_{cm}) \cdot L_{1sc}], \tag{24}$$
\[ V_{m} - V_{e} = (R_{m} + jX_{m})(I_{lmo} - I_{lmsc}), \]  
(24)

\[ R_{m} + jX_{m} = \frac{V_{m} - V_{e} - V_{msc}}{I_{lmo} - I_{lmsc}}, \]  
(25)

Figure 10. Equivalent phase scheme of the DSWA in short circuit probe

A ratio is made between relations (21) and (25) and the result is:

\[ X_{e} = \omega L'_{e}, \]  
(26)

\[ \frac{V_{m} - V_{e} - V_{msc}}{(R_{m} + jX_{m})I_{lmsc}} = \frac{R'_{e} + jX'_{e}}{I_{esc}}. \]  
(27)

with the help of the previous measurements and equations the stationary regime parameters are computed for the DSWA.

The magnetizing effect of the main current and that the best arrangement of excitation and main windings are at \( \alpha = -\pi/2 \) (Figures 11, 12, and 13).

Figure 11. Main voltage and main current: \( I_{DSWA} = 5.82 \) A, power to network \( P_{net} = -40\% \), \( f = 24 \) Hz, prime mover current \( I_{PM} = 13.3 \) A, reference torque 32.8 \%, speed \( n = 365.5 \) rpm.
Figure 12. Main voltage and main current: $I_{DSWA} = 5.12$ A, power to network $P_{net} = -14.62\%$, $f = 24$ Hz, prime mover current $I_{PM} = 12.12$ A, reference torque 16.5 %, speed $n = 362.4$ rpm

Figure 13. No load excitation voltage

5. Conclusions
In this paper was presented a new type of dual stator windings asynchronous machine operating in generator mode at variable low speed. A mathematical perfectly saturated model should be implemented in flux rotating frame coordinate and then the transients and steady state magnetization inductance could be considered in the model. The FEM validation results are still accurate from despite of large difference between steady state inductance and transient inductances, because the magnetization current magnitude is rather constant during the fundamental currents and voltages. In the experimental tests are prove quantitatively and qualitatively the good correlations with simulations, and the practicality of the proposed system for low power variable speed wind or hydro power plants applications.
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