Superconducting generators for wind turbines: design considerations

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Abstract. The harmonic content of high temperature superconductors (HTS) field winding in air-core high temperature superconducting synchronous machine (HTS SM) has been addressed in order to investigate tendency of HTS SM towards mechanical oscillation and additional loss caused by higher flux harmonic. Both analytical expressions for flux distribution and current sheet distribution have been derived and analyzed. The two main contributors to the AC loss of HTS rotor winding are also identified and their influence addressed on general level.

1. Introduction

The electrical machine’s output power to volume ratio, as shown in Eq.1, is proportional to revolution speed \( n_s \), rotor produced magnetic flux density \( B_{\text{rotor}} \), stator electrical loading (stator’s magnetomotive force along its circumference) \( A_{\text{stator}} \), diameter of the stator winding \( D \) and length of the machine \( L \).

\[
P = \frac{\pi^2}{\sqrt{2}} k_w B_s A_{\text{stator}}^3 n_s D^2 \Rightarrow \frac{P}{n_s} = T = (2\sqrt{2}\pi k_w) B_s A_{\text{stator}}^3 V \tag{1}
\]

From Eq.1 it is obvious that the torque is volume dependent since \( D^2 L \) presents machine’s volume. For conventional Synchronous Machines (SM), Induction Machines (IM) and Permanent Magnet (PM) SM machine product \( B_{\text{rotor}} A_{\text{stator}} \) is limited. Saturation of steel in rotor and stator or properties of PM [1] are limiting the air gap flux density on 1.5\( T \)-2.2\( T \). Current capacity of the stator and rotor winding is set by maximal tolerable temperature increase caused by Joule dissipation. Maximal stator electrical loading is reported to be 350\( kA/m \) [2,3]. Thus in practice, the torque to volume ratio is constant for all electrical machines. On the other hand, from Eq.1 the power of the machine for a given torque can be increased by rising its operational speed. Consequently, using the gearbox together with the electrical machine (also referred as a drive) and adjusting machine’s product \( (T \times n_s = \text{const.}) \) to a lower torque - higher speed, is often very useful and justified, resulting in a lighter drive. Nevertheless, the gearbox as a solution is bound to low and mid-torque application and has significant influence on overall efficiency and maintenance requirements of the drive.
Wind turbine (WT) producers have in the past preferred the choice of geared drive with high speed generator. Still, rapid development of wind industry, especially aimed at offshore WT farms and WT up to 10 MVA is favoring direct drive (DD) generators in WT due to low maintenances and robustness towards torque transients. The conventional DD generator for powers and speeds of present WT would need higher torque (and volume) ≈ 60 times compared to the high speed generator in geared drive. This volume expansion can be easily calculated from the Eq.1 if we assume for speed of high speed generator \( \omega_{GBgen} \cong 1500 \text{ rpm} \) and speed of WT \( \omega_{DDgen} \cong 25 \text{ rpm} \). Obviously, in order to introduce DD generators in WT for high power range, the machine technology requires improvements of torque to volume ratio \( (T/V) \). High Temperature Superconductors (HTS) SM have become very interesting to researchers and industries enabling the machines with very high torque densities \( T/V[Nm/m^3] \). By increasing the current densities up to 100 times (and steadily rising) higher than copper, HTS SM is aiming to redesign the electrical machines, making conventional machine’s design obsolete. With power ratings from 100 kVA up to 5 MVA, the wind power industry is one where these advances would open new horizons for gearless WT and units up to 10 MVA, suitable for distant offshore wind farms.

One of the main ways how the HTS SM can offer advance of the DD train’s \( (T/V) \) is by increasing the excitation (rotors) magnetic field. According to the Eq.1, if the field is increased twofold, the volume of such machine would be half of the conventional machine, both rated with same torque. As the flux of HTS winding is much larger than in conventional machine, control of space harmonic of HTS winding as one of the main sources of mechanical vibrations, increased losses in the machine and induced harmonics of stator’s voltage and current are essential for ensuring good performance of machine. Actually, since HTS is produced as a rather stiff tape, especially for applications where mechanical properties are crucial to provide the necessary strength and protection for the brittle superconductor, only the simple geometry coils (racetrack coils) can be constructed with present HTS technology. Having these constraints for designing excitation winding for the SM can create a reservation towards the flux spatial distribution of such winding and its harmonic contents. The study of the HTS SM electromagnetic parameters and impact of construction factors and constraints on machine’s flux distribution and harmonic content have been performed in Sec.2.1, Sec.2.2.
HTS is a novel material in electrical machines and one of main concerns is the HTS losses which needs to be addressed. Other loss mechanisms in machine as eddy currents, iron loss, Jules loss, skin effect etc. will not be focus of this study. The two main contributors to the AC losses in HTS have been identified: pulse with modulation (PWM) caused by power electronics high frequency (HF) switching and wind turbine load changes caused by stochastic wind speed changes. The AC loss of HTS is function of the flux perturbations and we have evaluated amplitude of these oscillations in the case of air-core HTS SM wind turbine generator. The DC energy loss in HTS in HTS SM is also shown to be minor, using expected values of machine parameters.

2. HTS SM design layout

HTS SM are promising to double the torque densities $T/V \text{[Nm/m}^3\text{]}$ compared to conventional SM (double fed and PM), and doing so mainly by increasing the rotor excitation field above $2T$. As rotor magnetic parts become saturated on higher fields, substituting rotor iron with nonmagnetic composite materials will provide necessary mechanical support and additionally contribute to lighter machine design. It is reported in [2, 3] that high magnetomotive force (MMF) available with the HTS rotor winding would saturate the conventional slotted stator. With that respect, air gap stator winding, suggested by [2, 3] for turbo-generators, would be more suitable to SM with HTS rotor. Cross section of such configuration is illustrated on Fig.1.

The necessary low temperature cooling system for HTS-SM (in the range $20[K]-80[K]$) presents additional complexity to the machine system. Even so, with steady development of no maintenance or low-level maintenance industrial cryocoolers (like pulse-tube [4]) and growing HTS power applications (cable, FCL, SMES), it is believed that such cooling system can have the necessary reliability with minimum maintenance in due time and with the ‘off the shelf’ application strategy [5]. Depending of HTS design operating temperature, the cooling system and its performance will differ. Nevertheless, for this study it is assumed that in order to keep the rotor thermally well insulated, the thickness of the cryostat can be estimated to $50[mm]$ for temperatures between $50[K]-80[K]$, and to $100[mm]$ between $20[K]-50[K]$ where besides vacuum and ML insulation the chamber with $LN_2$ is desirable as a pre-cooling step. The pre cooling with $LN_2$ will increase cooling efficiency and lower cool-down time and price of it [6] but may also increases cooling system complexity and machine’s air gap. Thus, the tradeoff is necessary.
2.1. Analytical approximation of flux density of HTS SM

Absence of iron in rotor and air gap stator winding are making traditional approach of machine analysis inapplicable [7,8]. Assumption that the mutual flux between rotor and stator can be derived from calculation of magnetic permeance of overall magnetic circuit and machine’s MMF, in the air-core machine is flawed, mainly due to the fact that flux is not only radial across the air gap as in conventional machine. This presents obstacle in analysis of such machines since calculation of the electro-magnetic parameters is bound to the FEM modeling and derivation of spatial distribution of magnetic flux. While the exact values can be derived only using FEM analysis on specific generator geometry, analytical formulation of magnetic flux distribution for arbitrary machine geometry would be very useful for identification of inductances and flux density of air cored machines.

A convenient way of deriving analytical expression for the spatial flux distribution is to use the vector potential $A_z$ formulation of Maxwell equations as done by [7,8] and applied to arbitrary geometry. The Eq.2 present the radial magnetic flux density distribution of arbitrary current sheet enclosed by the stator back iron. In formulation of problem, authors have assumed infinitely thin current sheet at radius $r_{sheet}$ with harmonic current distribution along the circumference $A_z(\theta) = A_{sheet} \sin(p\bar{\theta})$. Derivation of $A_z(\theta)$ for general case of HTS winding will be presented in Sec.2.2.1. Neglecting saturation and 'end effects' and for $r_{sheet} < r < r_{sin}$, the radial flux distribution can be expressed as Eq.2 [7,8]

$$B_{r,\theta}^p = \frac{\mu_0}{2} A_{sheet} \left( \frac{r_{sheet}}{r} \right)^{\nu+1} \left( 1 + \eta_\lambda \frac{r_{sheet}}{r_{sin}} \right)^{2\nu} \cdot \cos(\nu p \theta)$$

(2)

where $\mu_0$, $\mu_s$ are magnetic permeability of vacuum and relative permeability of stator back iron, respectively, $\nu$-harmonic order, $p$-number of poles pairs, $A_{sheet}$ is electrical loading of the current sheet (see Sec.2.2.1) at its radius $r_{sheet}$ and $r_{sin}$ and $r_{out}$ are inner and outer radius of the stator back iron. The influence of the stator back iron on flux density of the rotor current sheet is seen throughout the parameters $\lambda_s$ and $\eta_\lambda$, defined by the Eq.3.

$$\lambda_s = \frac{\mu_s - 1}{\mu_s + 1}; \quad \eta_\lambda = \frac{1 - \left( \frac{r_{sin}}{r_{out}} \right)^{2\nu}}{1 - \lambda_s^2 \left( \frac{r_{sin}}{r_{out}} \right)^{2\nu}}$$

(3)

Interesting fact to illustrate is nature of synchronous inductances of air-cored machines. The tendency of inductances in full air core machine (no iron) and conventional machine (very narrow air gaps), where $g$ is radial air gap between stator iron and rotor iron, would go as [7,8]

$$\frac{L_{(conv)}}{L_{(air)}} = 2 \frac{1 + ((r_{sin} - g)/r_{sin})^{2p}}{1 - ((r_{sin} - g)/r_{sin})^{2p}}$$

(4)

If the air gap, $g$, is aspiring towards small values (few mm), ratio of inductances in Eq.4 can be as big as 100 [7,8]. Now, the value for the inductance of conventional machines (small air-gap machines) is $\approx 1.5$ p.u and if we take for the ratio of the air cored machine and conventional inductances value of 100, the air core design would yield only $\approx 0.015$ [p.u.]. In the case of HTS SM, since the back iron is present, the ratio of inductances will be smaller but even so it is expected to be 2 to 20 times lower than inductance in conventional machine. From electrical side of characterization of electrical machines, the very low value of synchronous inductances is the biggest difference between conventional and air cored HTS SM suited for WT.
2.2. Consideration of the HTS coils and field winding

In order to investigate the flux distribution of the simple HTS rotor, as general as possible, the space current sheet approximation for the proposed HTS rotor layout must be established first.

2.2.1. Current sheet approximation of HTS rotor winding

The Fig.4 is illustrating the angular distribution of ampere-turns along the sheet circumference. In the radial cross section of the machine, HTS windings can be presented by a ring with constant turns density \( n_\rho \), shown on the Fig.1 with a orange color. Current fed coils (light and dark purple color on Fig.2) or angular segments of orange ring are defined by the angles \( \theta_1 \) and \( \theta_2 \).

Fig.3 is illustrating the corresponding angles between the ring presentation and actual stacks of racetrack coils for two pole pitch rotor segment (2\( \pi/p \)).

The HTS turns density \( n_\rho \) expressed in \( \text{turns/m}^2 \) of the rotor’s cross section, is considered constant in the coil angular segments. Expansion of the ampere turns distribution into Fourier series \( A(\theta) [A/m] = \sum A_\nu^\text{sheet} \cdot \sin(\nu p \theta) \) would yield Eq.5 and Eq.6 as expression for the amplitude of each harmonic of the current sheet, where the \( r_{\text{sheet}} = 0.5(\text{r}_{\text{out}} + \text{r}_{\text{in}}) \).

\[
A_\nu^\text{sheet} = \frac{I_{\text{sheet}}}{\pi} \int_{-\pi}^{\pi} \int_{r_{\text{in}}}^{r_{\text{out}}} n_\rho(r,\theta) \cdot \sin(\nu p \theta) d\theta dr =
\]

\[
= \frac{I_{\text{sheet}}}{\pi} \frac{2p}{r_{\text{sheet}}} \frac{\text{r}_{\text{out}}^2 - \text{r}_{\text{in}}^2}{2} \left[ \frac{-\cos(\nu p \theta)}{\nu p} \left( \frac{\pi - \theta}{\nu p} \right) \right] + \left[ \frac{-\cos(\nu p \theta)}{\nu p} \left( \frac{\pi + \theta}{\nu p} \right) \right] = k_{A_\nu^p} r_{\text{sheet}}
\]

where \( k_{A_\nu^p} \) can be interpreted as the winding coefficient for HTS rotor winding, \( r_{\text{out}} \) and \( r_{\text{in}} \) are outer and inner diameter of HTS ring (orange ring on Fig.1) and \( I_{\text{sheet}} \) would be current of the HTS winding. The \( n_\rho(r,\theta) \) is spatial distribution of HTS conductors in radial cross section.

2.2.2. Harmonic content of HTS winding

All radii will be normalize to the radius of current sheet \( r_{\text{sheet}} \). The normalization of radial dimension of machine is desirable in order elevate the discussion on more general level. For example, if \( r_{\text{sheet}} \) is the base value (1p.u.), all radial dimensions (calculated as \( r/r_{\text{base}} \) and expressed in p.u.) will lie in few percentage of the base value. If the rotor winding is at radius of 1.5m, cryostat is 100mm and stator iron \( r_{\text{sm}}=2.1m \), the radii from interest would be in the range \( 1.067p_{\text{pu}} - 1.4p_{\text{pu}} \), expressed in relative units and would corresponds to the space between outer radius of rotor’s cryostat and inner radius of the stator back iron.

- The dependence of \( K_{(r,p)}^\nu \) from Eq.2 and amplification of the flux spatial harmonics inherited from current sheet harmonics can be observed on Fig.5. The tendency of the main flux harmonic is illustrated in the inset figure on Fig.5 while varying \( p \) and normalized \( r \). All higher harmonics are referred to its main and the black plane is plotted as a indicator to 5% of higher harmonics content.

The flux harmonics content are decreasing approximately as \( r^{-\nu(p+1)} \). It can be observed that \( \nu \) harmonic is falling with increasing radius and number of poles faster than the first harmonic and for a factor \( r^{(1-p\nu)} \). Hence, the content of flux spatial harmonics is decreasing with increasing number of poles, favoring the high pole machines and with increasing \( r \) distance from excitation.
Figure 5. Dependence of rotor’s flux higher harmonics referred to its first harmonic, calculated as a ratio of $K_{\nu=3,5,7}/K_{1^{st}}$ from Eq.2, from $(r,p)$, illustrating the tendency for harmonics content of rotor’s flux for different numbers of poles and stator radial position. The 0.05pu of the main flux harmonic is presented with black plain. Inset: Illustration of the tendency of the first harmonic of rotor’s flux in the air core machine, seen throughout the $K_{1^{st}}$, while varying the normalized radial and number of poles.

sheet, favoring machines with low synchronous inductance.

It is also noteworthy to state that for fixed value of $A_{sheet}$, the main flux harmonic is decreasing as $\approx r^{-p}$, as illustrated in inset figure on Fig.5.

- Concerning the HTS current sheet and harmonic content of rotor MMF, if the available space for the HTS coil is known ($r_{in}$ and $r_{out}$), the sector angles $\theta_1$ and $\theta_2$ (between 0 and $\frac{\pi}{2}$) are having significant influence on current sheet harmonics. The $k_{A}^{1st}$ from Eq.6, if referred to its first $k_{A}^{1st}$, would be the measure of the sheet harmonics ($\nu$ harmonic as a % of first harmonic). We can observe the first harmonic of HTS current sheet, $k_{A}^{1st}$ and tendency of higher harmonics referred to $k_{A}^{1st}$ on Fig.6 where the 20% is marked with black plane. For the $\theta_1 \approx 30[deg]$ and $\theta_2 \approx 5[deg]$, lower order harmonics are less than 20% of the main harmonic. Thus, it is prudent to set the coils sector angles on these values. By doing so, the lower order spatial harmonics (3rd and so on) of the current sheet and flux they make will be lowest. Higher order harmonics of current sheet and the flux they create will be attenuated more than for lower harmonics as concluded previously. Thus, construction parameters of HTS winding have significant influence on flux distortion and should be adjusted accordingly.

As the result of previous analysis, the overall harmonic content in the rotor’s flux, donated by $k_{A}^{\nu} \cdot K_{\nu}$ from Eq.2 and Eq.6, can be adjusted by stator and rotor winding radii and HTS winding angles $\theta_1$, $\theta_2$ to negligible values. The black planes in Fig.5 and Fig.6 are presenting 5% from the main flux harmonic and 20% from the main current sheet harmonic, respectively. Values for $k_{A}^{\nu} \cdot K_{\nu}$ from planes would yield high harmonic with amplitude of 1% of the main harmonic.
3. The HTS SM for wind power application

With power ratings up to 4[MVA] and the rotation speed 20[rpm], today’s WT with the gearbox (GB) and IM have been the more favorable option by wind turbine producers. This is mainly due to the smaller weight and cost of the high speed generator and gearbox compared to the direct driven (DD) generator [9]. GB equipped WT units have been very successful on the ‘on shore’ market, yet inherent maintenance requirements for GB and call for larger units (up to 10MVA) is one of the main technical issues and the reason for unexploited offshore wind potential. One of the biggest challenges of DD WT generator is its size and weight. As previously said the HTS can generate high magnetic field and with it increase the \(T/V\) of electrical machines significantly and hence the motivation towards DD HTS SM wind turbine generator.

3.1. Topologies of the drive train

As it is well established in WT by now, the variable speed DD WT will have a full power frequency converter (voltage source /current source or more advanced topology) as an interface between WT generator and the power grid ensuring variable speed operation. As power electronic and PWM will be unavoidable to achieve full control of the HTS generator, it is crucial to predict the behavior of such machine under WT like conditions. The losses in HTS rotor which are amplified by the cooling efficiency of rotor cryogenic cooling (ranging from 50 up to 1000 \([6]\) for temperatures ranging in \(80[K]\) to \(20[K]\), will be function of WT operation conditions. We will address the biggest two contributors to AC loss of HTS in wind turbine generator.

4. Contributors to energy loss in HTS

Thermal load of the cooling system (cryocoolers) of HTS SM would be the heat load thru the insulation and heat load generated by HTS, both DC and AC. To be able to estimate energy loss we have to know how to characterize the HTS. The electro-magnetic behavior of all superconductors can be described with the Power low shown in Eq. 7 and used to derive voltage.
of the tape/coil.

\[ E(L) = E_0 \left( \frac{I}{I_{c}(B,\alpha,T)} \right)^{n(B,\alpha,T)} \] (7)

The \( I_c(B,\alpha,T) \) is HTS critical current and it is a local tape attribute. It depends from intensity of magnetic flux density, B, the angle between B vector and wide face of the tape, \( \alpha \), and local temperature (accounting for thermal gradients in coils). The \( I \) is the electrical current of HTS and \( n(B,\alpha,T) \) is a measure of the transition abruptness between superconducting and normal state and is a also local tape attribute. The \( n(B,\alpha,T) \) has tendency to decrease logarithmically with increasing B and/or T [10]. The \( E_0 \) is the voltage seen when the tape is carrying critical current and the value of \( 10^{-4}[V/m] \) is wildly used.

4.1. DC energy loss in HTS winding

The energy loss of the HTS winding is seen as a DC voltage drop (generated by the movement of unpinned flux vortices) along tape and across the HTS field winding when loaded with DC current. The voltage of HTS coil, shown in Eq.8 would be line integral of the Power low along the tape’s length.

\[ U = \int_0^L E(L) dL \] (8)

The DC loss is calculated as a product of tape’s current and tape’s voltage drop. For the time constant electrical and magnetic loading of the HTS winding, the expressions for the power losses can be written as Eq.9

\[ P = \int_0^L I \cdot E(L) dL \] (9)

Since \( I_c \) and \( n \) are both functions of flux distribution, the equations Eq.9 requires FEM simulations in order to derive exact voltage along the HTS. It is much more desirable to have high \( I/I_c \) which is minimizing amount of expensive HTS but usually some parts of the HTS coils are experiencing higher flux and are very close to \( I_c \) while other parts are in much lower flux and have higher \( I_c \). Thus, for the purpose of this discussion we can define average \( I_c \) and \( n \) as averages values for one coil. If the average ratio \( I/I_c \) is relatively low (0.5), and assuming the average \( n = 15 \), the power dissipation due to this loss will be minimal i.e. the voltage drop along a km of tape would be \( 10^{-5}[V/m](0.5)^{15} \times 1000[m] \approx 3 \times 10^{-6}V/km \). Under hypthesizes that 10MVA generator for WT would need 500km of standard 4mm HTS tape, operating at 20K and carrying 50A, the approximate amount for static losses would be \( 500[km] \times 50A \times 3 \times 10^{-6}V/km \approx 0.075W \) at low temperature. Multiplying that loss with cooling efficiency (\( 10^3 \) for \( T \approx 20K \)) would result in \( \approx 100[W] \) at room temperature what is negligible value compared with 10MVA.

4.2. The contributors to AC loss in HTS winding

The AC loss of HTS windings would result from high frequency (1[kHz] – 5[kHz]) field modulation caused by PWM flux and WT torque fluctuation caused by stochastic wind speed profiles (WT operating frequency is in the range 0.1Hz – 1Hz). This loss depends primarily of the magnetic and electric loading of HTS and the intensity and frequency of oscillations. The HTS coils current ripple [11] can also have the big influence on power loss, \( I_c \) and \( n \) value but due to the large self inductance of HTS winding, it is expected that excitation system can maintain HTS current very smooth (negligible ripple).

To address the wind caused torque fluctuations, we can calculate the armature reaction using the value for synchronous reactance of HTS SM. The intensity of the flux perturbations due to the wind load oscillations will be proportional to the stator current change, donated with \( \Delta I_{(d)}wind \). For the value of \( L_d \) of the HTS SM, referring to the Sec.2.1, we can assume 0.1 p.u
measure of the stator’s magnetic influence on the rotor is the of stator and field winding, respectively. The resistance of the stator is donated with can be argued that with a lower value for the rotor HTS would be less the 10% of main flux, accounting for a stator leakage flux. Thus, it that for current change of 1 pu (from no load to full load), the maximum flux perturbation at frequency is in the range 2

\[
\begin{align*}
\left[ \begin{array}{c}
\psi_a \\
\psi_q \\
\psi_f
\end{array} \right] = 
\left[ \begin{array}{c}
-L_{aq} + L_l \psi_a + L_{ad} \psi_q \\
-L_{aq} + L_l \psi_q + L_{ad} \psi_f
\end{array} \right] = 
\left[ \begin{array}{c}
-(L_{ad} + L_l) \psi_d + L_{ad} i_d \\
-(L_{aq} + L_l) \psi_q + L_{ad} \psi_f
\end{array} \right]
\end{align*}
\]

where the \( L_{ad/aq}, L_l \) and \( L_{d/q} = L_l + L_{ad/aq} \) are the mutual, leakage and self inductances of the \( dq \) stator representation and \( L_{ff} \) is the self inductance of the field winding (all referred to the stator). The \( u_{d/q/f}, i_{d/q/f} \) and \( \psi_{d/q/f} \) are presenting voltages, currents and fluxes of \( d \) and \( q \) axis of stator and field winding, respectively. The resistance of the stator is donated with \( R \). The measure of the stator’s magnetic influence on the rotor is the \( L_{ad/aq} \) and can be seen in Eq.10.

The mutual inductance \( L_{ad/aq} \) will be smaller than synchronous inductance \( L_{d/q} \). This means that for current change of 1 pu (from no load to full load), the maximum flux perturbation at the rotor HTS would be less the 10% of main flux, accounting for a stator leakage flux. Thus, it can be argued that with a lower value for the \( L_{d/q} \), the rotor - stator magnetic coupling is weaker and with it the losses of HTS, caused by change of stator current. It is noteworthy at this point to remind that such tendency goes together with the increasing ratio of \( T/V \), both favoring high values for rotor flux density. As the WT torque oscillations (unsteady wind speeds, blades effects - 3P, etc) would be a portion of rated torque and reciprocal to the its frequency [13], the \( \Delta I_{(d)\text{wind}} \) will also be a portion of rated current. It is realistic to assume that stator current will behave similar to \( I_0 = 0.75\text{[p.u.]} + 0.25\text{[p.u.]} \sin(p\omega \cdot \text{load})\), where \( \Delta I_{(d)\text{wind}} \approx 0.5\text{p.u.} \). Thus, flux perturbation due to the wind load change can be estimated with mutual flux perturbation calculated with \( L_{ad/aq} \cdot \Delta I_{(d)\text{wind}} < 0.1 \cdot 0.5 \).

The HF PWM magnetic field ripples, and its effect on HTS excitation winding, are caused by armature current on PWM frequency. Again using the \( L_{ad/aq} \), we can estimate the amplitude of armature ripple. Let us assume the non excited steady state, i.e \( [u_d, u_q] = [0, 1]\text{[p.u.]} \), \( i_f = 0\text{p.u.} \), where the \( dq \) frame is rotating with \( \omega_{\text{pwm}} = 500\text{p.u.} - 2000\text{p.u.} \). The base speed is \( \text{p}\omega_{\text{WT}} \). By assuming 10 pole generator and WT revolution 1\text{Hz}, the \( \text{p}\omega_{\text{WT}} \) would be 5\text{Hz}. If the PWM frequency is in the range \( 2.5 \cdot 10^3 - 10 \cdot 10^3\text{Hz} \) the \( \omega_{\text{pwm}} \) will be in the range 500\text{p.u.} - 2000\text{p.u.} . If we neglect the \( R_s \), from Eq.10 the armature current will be

\[
i_d = \frac{u_d}{\omega_{\text{pwm}} L_{ad}} \Rightarrow \Psi_{f_{\text{pwm}}} = L_{ad} \frac{1\text{[p.u.]} (500\text{[p.u.]})}{500\text{[p.u.]} L_{ad}} < 0.002
\]

and associated HF flux perturbations will have less than 0.2% of main harmonic. It is recommended to use additional rotor circuit (dumpers) to effectively screen out HF armature flux and thus minimize the AC loss of HTS. Yet, in the case of HTS SM suited for WT with imperative on low weight of machine and with it low synchronous inductance too (the \( T/V \) increase is going together with lowering \( L_{ad} \), the additional rotor circuit will decrease the transient and sub transient parameters of machine far lower than already low synchronous inductance. The additional complexity of cryostat, energy loss and transient parameters of machine all need to be optimized if the rotor dumper circuits are to be used to minimize HTS AC loss due to the armature flux oscillations.

Returning to the rotor winding and the HTS tapes, the intensity of the magnetic flux oscillations caused by armature current, in the case of slow changing wind load \( (f_{\text{load}} < 10^{-1}\text{[Hz]}) \) [13] and for a realistic values for inductances, \( L_d = L_l + L_{ad} = 0.025\text{[p.u.]} + 0.075\text{[p.u.]} \) of the air core HTS SM and mentioned WT load oscillation, \( \Delta I_{(d)\text{wind}} \) the amplitude of oscillation of armature reaction will be \( L_{ad} \Delta I_{(d)\text{wind}} = 0.075 \cdot 0.5 \). Thus, for having the flux density at rotor HTS winding for example 8T, the wind load change would cause 3.75% flux oscillation,
In order to calculate HTS loss, the tapes parameters $I_{c}(B,\alpha,T)$ and $n(B,\alpha,T)$ must be obtained experimentally and since the AC power loss will depend on the local distribution of magnetic field and current density in HTS, it can not be discussed on generally level. The influence of the ratio $I/I_{c}$ and expected flux perturbations, derived in this article, on AC loss of HTS will be part of the future work.

5. Conclusion
This paper has briefly shown the nature of improving the output capabilities (T/V) of electrical machines. The content of higher harmonics in the spatial flux distribution was derived proving that when using the HTS with all its mechanical constraints as a rotor field winding, the amount of harmonics can be minimal. Also, we have addressed two main contributors to AC loss of HTS and using realistic values for HTS SM parameters, we have estimated the extent of their influence. It is noticed that when increasing the machine’s flux density (resulting in the tendency towards the low $L_{d/q}$ SM and increasing the (T/V)), the HTS winding will be less susceptible to stator flux distortions and will have lower AC loss.

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References
[1] E P Furlani 2001 Permanent Magnet & Electromechanical Devices: Materials, Analysis, and Applications (Electromagnetism) (Academic Press)
[2] E Davies 1971 IEE Proceedings 118 529–535
[3] E Spooner 1973 IEE Proceedings 120 1507–1518
[4] Z H Gan, et al 2009 ScienceDirect Cryogenics 49 198–201
[5] J Frauenhofer, J Grundmann, G Klaus, W Nick 2007 IEEE Transactions on Applied Superconductivity 17 1568–1570
[6] T M Flynn 2005 Cryogenic engineering (CRC Press)
[7] A Hughes, T Miller 1977 IEE Proceedings 124 121–126
[8] T Miller, A Hughes 1977 IEE Proceedings 124 127–132
[9] L H Hansen et al 2001 Conceptual survey of Generators and power electronics for wind turbines (RISØ National Laboratory)
[10] E Martinez, et al 2007 IEEE Transactions on Applied Superconductivity 17 2738–2741
[11] L F Goodrich, J D Splett 2007 IEEE Transactions on Applied Superconductivity 17 2603–2606
[12] P Kundur 1994 Power System Stability and Control (McGraw Hill)
[13] T Burton, D Sharpe, N Jenkins, E Bossanyi 2001 Wind energy handbook (Wiley Press)