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Research paper

# AC loss simulation in a HTS 3-Phase 1 MVA transformer using *H* formulation

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ABSTRACT

One of critical issues for HTS transformers is achieving sufficiently low AC loss in the windings. Therefore, accurate prediction of AC loss is critical for the HTS transformer applications. In this work, we present AC loss simulation results employing the *H*-formulation for a 1 MVA 3-Phase HTS transformer. The high voltage (HV) windings are composed of 24 double pancakes per phase wound with 4 mm – wide YBCO wire. Each double pancake coil has 38 ¼ turns. The low voltage (LV) windings are 20 turn single-layer solenoid windings wound with 15/5 (15 strands of 5 mm width) Roebel cable per phase. The numerical method was first verified by comparing the numerical and experimental AC loss results for two coil assemblies composed of two and six double pancake coils (DPCs). The numerical AC loss calculated for the transformer was compared with the measured AC loss as well as the numerical result obtained using the minimum magnetic energy variation (MMEV) method. The numerical AC loss result in this work and experimental result as well as the numerical result using MMEV at the rated current agree to within 20%. Further simulations were carried out to explore the dependence of the AC loss on the gap between the turns of the LV winding. The minimum AC loss at rated current in the 1 MVA HTS transformer appears when the gap between turns is approximately 2.1 mm turn gap in the LV winding. This is due to the change of relative heights between the HV and LV windings which results in optimal radial magnetic field cancellation. The same numerical method can be applied to calculate AC loss in larger rating HTS transformers.

## 1. Introduction

Large scale HTS (high temperature superconducting) applications, such as generators, transformers, fault current limiters and superconducting magnetic energy storage, are composed of HTS coils/windings with large turn numbers [1–5]. AC loss in HTS windings is one of the key issues, because it adds heat load to the cooling system and hence reduces the efficiency of the devices. Therefore, it is critical to predict the AC loss in these windings with large turn numbers using numerical methods to be able to optimise the design of the windings and cooling system.

Numerical modelling of AC loss for single tapes and stacks composed of large number of conductors has been done by many methods, such as the *H* formulation [6,7], *A-V* formulation [8], *T*-Ω formulation [9], *T-A* formulation [10] and MMEV method [11]. The *H* formulation method is promising with many advantages: small number of variables, direct computation of magnetic field components, easy application of boundary conditions, and automatic satisfaction of continuity of the tangential component of variables [12].

Recently, a HTS 1 MVA 3-Phase transformer was demonstrated by Robinson Research Institute, Victoria University of Wellington, in New Zealand. The high voltage (HV) windings are composed of 24 double pancakes per phase wound with 4 mm–wide YBCO wire with each double pancake coil having 38¼ turns [1]. The low voltage (LV) windings are 20-turn single-layer solenoid windings wound with 15/5 (15 5 mm strand) Roebel cable per phase. AC loss of a single phase of the 1 MVA transformer without ferromagnetic core was measured and calculated using MMEV [13].

In this paper, we presents modelling results for the 1 MVA HTS transformer with approximately one thousand turns in the HV winding and 20 turn 15/5 Roebel cable solenoid in the LV winding, as well as a stand-alone solenoid coil which has the same geometry as the LV winding, using *H* formulation, for which there has been no report of simulations using this method for large turn number transformers. A

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structured rectangular mesh [7], edge element method [6], and homogenization method [14] were used to optimise the simulation accuracy and calculation speed.

The numerical method was first verified with smaller scale HTS windings by comparing the numerical and experimental AC loss results in two coil assemblies composed of a stack of two and six double pancake coils (DPCs) [15,16]. The simulated AC loss results for the transformer were then compared with the measured result as well as the one obtained using the MMEV method [13]. We also carried out simulations by changing the gap between turns in the LV winding to investigate the dependence of the AC loss in the transformer on this parameter. The gap was varied in the range of 1 mm–4 mm. The result can be explained by considering the magnetic field distributions around the LV and the HV windings.

## 2. Numerical method

Calculations were carried out using the *H* formulation. A combination of structured mesh, edge element [6], and homogenization methods [14] was used. In the following we recap some details of the *H* formulation and homogenization method. The model was implemented using COMSOL Multiphysics 5.2 and a computer equipped with Intel(R) Core(TM) i5-4570 CPU @3.2 GHz and a RAM of 16 GB.

### 2.1. H formulation

A 2D axial symmetrical *H* formulation was applied in the calculation. The variables in the model were defined as $\boldsymbol{H} = [H_r, H_z]^T$, where $H_r$ and $H_z$ are the radial and axial magnetic field components, respectively. The injected or induced current $\boldsymbol{I}$ flows in the $\varnothing$ direction as shown in Fig. 1. The relationship between local electric field $E_\varnothing$ and local current density $J_\varnothing$ is expressed as, $E_\varnothing = \rho J_\varnothing$, where ρ is the resistivity of the material.

The Maxwell equations used in the model are expressed as follows,

$$\boldsymbol{J} = \nabla \times \boldsymbol{H} \tag{1}$$

$$\nabla \times \boldsymbol{E} = -\frac{\partial \boldsymbol{B}}{\partial t} = -\mu_0\mu_{re}\frac{\partial \boldsymbol{H}}{\partial t} \tag{2}$$

As $\boldsymbol{E}$ has the same direction as $\boldsymbol{J}$, namely $E_\varnothing$ only, by substituting Eq. (1) and $E_\varnothing = \rho J_\varnothing$ into Eq. (2), we can get

$$\begin{cases} \mu_0\mu_{re}\frac{\partial H_r}{\partial t} - \frac{\partial\left(\rho\left(\frac{\partial H_z}{\partial r} - \frac{\partial H_r}{\partial z}\right)\right)}{\partial z} = 0 \\ \mu_0\mu_{re}\frac{\partial H_z}{\partial t} + \frac{1}{r}\frac{\partial\left(r\rho\left(\frac{\partial H_z}{\partial r} - \frac{\partial H_r}{\partial z}\right)\right)}{\partial r} = 0 \end{cases} \tag{3}$$

where $\mu_0$ is vacuum permeability and $\mu_{re}$ is relative permeability with the value of one in this work because there is no magnetic substrate or any other ferromagnetic materials. It is worth noting that different materials have different ρ values. E.g., for air we use $\rho_{air} = 1\ \Omega$m, but

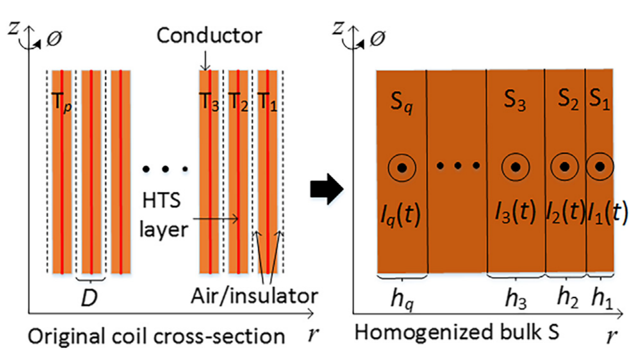


**Fig. 1.** Schematic of the homogenized bulk.

**Table 1**
Magnetic field dependence parameters.

| | $J_{c0}$ (A/m$^2$) | $B_0$ (mT) | $\alpha$ |
|---|---|---|---|
| Two-DPC stack | $2.6 \times 10^{10}$ | 125 | 1 |
| CWs in six-DPC stack | $2.15 \times 10^{10}$ | 170 | 1 |
| EWs in six-DPC stack | $5.95 \times 10^{10}$ | 150 | 1 |
| LV winding in transformer | $3.55 \times 10^{10}$ | 149 | 0.6 |
| HV winding in transformer | $2.12 \times 10^{10}$ | 149 | 0.6 |

for superconductors, we define $\rho_{HTS}$ based on a power-law for *E* as a function of *J*:

$$\rho_{HTS} = \frac{E_c}{J_c(B)}\left|\frac{J_\phi}{J_c(B)}\right|^{(n-1)} \tag{4}$$

where $J_c(B)$ is the critical current density as a function of magnetic field; $E_c = 10^{-4}$ V/m; *n* is the power-law exponent of the *E-J* curve with its value assumed to be 30 in this work. For the $J_c(B)$ relationship, we have used a modified Kim model [17]:

$$J_c(B) = J_{c0}\left(1 + \frac{|B_\perp|}{B_0}\right)^{-\alpha} \tag{5}$$

where $J_{c0}$ and $B_0$ are constants determined from the measured *E-J* curve of coated conductors under perpendicular magnetic field, $B_\perp$. Table 1 shows the specifications for magnetic field dependence in all simulations in the paper. The details of the two-DPC and six-DPC stacks can be found in Section 3.

### 2.2. Homogenization method

The homogenization method was introduced in [14] for simulating AC loss in stacks comprising up to 64 HTS coated conductors. In this paper, we extend it to calculate AC loss in the large-turn-number HV and LV windings of the 1 MVA air-core transformer. It is worth noting each turn of Roebel-cable LV winding was modelled as two parallel stacks with a total of either 14 or 16 conductors, carrying the same current in each conductor [18,19]. In the model constraints were applied to the current to impose equally distributed current in all parallel conductors [20,21]. The homogenization method adopts an equivalent anisotropic homogeneous bulk S for the cross-section of stacks/coils such that the geometrical layout of the internal structures of metallic, substrate layer, and superconducting layer are merged together while keeping its original overall electromagnetic properties. In a tightly piled stack composed of infinitely thin conductors, we have the condition as follows [14],

$$K(z, t) = \int_S J(r, z, t)dr \tag{6}$$

where $K(z, t)$ is the sheet current density at specific height *z* in S; $J(r, z, t)$ is the current density and *t* is the specific time during one AC cycle.

Fig. 1 gives the schematics of subdivided sub-blocks. The original coil/stack is composed of *p* tapes $T_1, T_2, T_3, \ldots, T_p$. There are $q$ ($q < p$) sub-blocks $S_1, S_2, S_3, \ldots, S_q$ in the corresponding homogenized bulk S, carrying current $I_1(t), I_2(t), I_3(t), \ldots, I_q(t)$, and having the heights of $h_1, h_2, h_3, \ldots, h_q$, respectively. Then the current constraints can be expressed as,

$$K(z, t) = \frac{I_1(t)}{h_1} = \frac{I_2(t)}{h_2} = \frac{I_3(t)}{h_3} = \ldots = \frac{I_q(t)}{h_q} \tag{7}$$

For the homogenous bulk, the equivalent engineering critical current density, $J_{c,\ eng}$, is defined as,

$$J_{c,\ eng} = J_c f_{HTS} \tag{8}$$

where $f_{HTS}$ is the volume fraction of superconductor, the ratio of the thickness of the superconducting layer to the tape thickness D, as shown in Fig. 1.