Thermal analysis for the HTS stator consisting of HTS armature windings and an iron core for a 2.5 kW HTS generator

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Abstract
Most present demonstrations of high-temperature superconducting (HTS) synchronous motors/generators are partially superconducting, only installing HTS coils on the rotor as excitation windings. The possible applicability of HTS armature windings is an interesting research topic because these windings can certainly increase the power density attributed to a potentially high armature loading capacity. In this study, we analysed the thermal behaviours of a developed 2.5 kW–300 rpm synchronous generator prototype that consists of an HTS stator with Bi-2223–Ag armature windings on an iron core and a permanent magnet (PM) rotor. The entire HTS stator, including the iron core, is cooled with liquid nitrogen through conduction cooling. The rated frequency is set at 10 Hz to reduce AC loss. The properties of the HTS windings and the iron core are characterized, and the temperatures in the HTS stator under different operation conditions are measured. The estimated iron loss is 11.5 W under operation in 10 Hz at liquid nitrogen temperature. Conduction cooling through the silicon iron core is sufficient to cool the iron core and to compensate for the temperature increment caused by iron loss. The stable running capacity is limited to 1.6 kW when the armature current is 12.6 A (effective values) due to the increasing temperature in the slots as a result of the AC loss in the HTS coils. The thermal contact between the HTS coils and the cooling media should be improved in the future to take away the heat generated by AC loss.

Keywords: HTS armature winding, conduction cooling, iron core, AC loss, HTS generator

(Some figures may appear in colour only in the online journal)

1. Introduction

Much effort has been exerted to develop high-temperature superconducting (HTS) rotating machines since the commercialized production of HTS wires. Given the advantages of HTS materials, namely, a high current carrying capacity and a near-zero ohm loss, replacing the copper windings of rotating machines with HTS coils can increase power density, thereby reducing volume and weight \cite{1, 2}. The benefit of HTS rotating machines is prominent especially for applications that require volume and weight to be restricted, such as wind turbine generators \cite{3}, ship propulsion motors \cite{4} or aircraft power sources \cite{5}.

The power of a synchronous generator or motor can be expressed as follows \cite{6}:

\[ P = \frac{\pi^2}{\sqrt{2}} k_w n B_0 A D^2 L. \]  

(1)
Equation (1) shows that power $P$ is proportional to the product of rotating speed $n$, gap field $B_0$, armature loading $A$, winding factor $k_w$ and volume parameter $D^2L$ (diameter $D$ and length $L$). In general, $B_0$ and $A$ are related to the current capacity of DC excitation windings and AC armature windings, respectively. In most present demonstrations of HTS synchronous motors/generators, researchers have focused on the partial HTS configuration, installing HTS coils only on DC exciting windings to increase $B_0$ values over those of traditional copper ones [7–12].

Equation (1) also indicates that the power density can be further improved by combining HTS excitation windings and HTS armature windings. This improvement may enhance both $B_0$ and $A$, thereby producing fully HTS synchronous rotating machines. However, HTS armature windings cause AC loss, that can increase the heat load on cryogenic systems. Therefore, HTS armature windings have attracted minimal attention thus far. Several groups have conducted preliminary studies on HTS armature windings. A group from Cambridge University developed an HTS motor with YBCO armature coils and an YBCO bulk rotor [13]. At Kyoto University, Nakamura established a squirrel-cage-type HTS induction–synchronous motor and tried to replace the copper armature windings with Bi-2223–Ag coils [14]. At Fukui University, Sugimoto generated an axial-flux-type HTS motor with HTS armature windings and permanent magnets (PMs) [15]. Li proposed a radial-gap-type HTS generator design with HTS armature windings and PMs installed on the rotor [16]. Furthermore, groups have also initiated an investigation into fully HTS generators based on YBCO and MgB$_2$ wires [17–22].

To study the feasibility of HTS armature winding, we successfully developed a 2.5 kW synchronous generator prototype with an HTS stator and a PM rotor (HTS-PM type) [23]. The HTS stator mainly consists of Bi-2223–Ag HTS coils and an iron core made from silicon steel sheets. AC loss is the main obstacle for HTS armature windings; nonetheless, this loss may be reduced to an acceptable level if the HTS armature windings operate in a low-frequency situation, as in wind turbines [23]. For ordinary rotating machines, $B_0$ would be 0.8 T or higher. The critical current ($I_c$) of HTS coils (77 K) is small for such a field. The iron core can significantly reduce the magnetic field on HTS coils, thereby increasing the current carrying capacity.

In this particular generator configuration considering an HTS stator, the means of efficient cooling is one of the major concerns. Two approaches can be considered: the conduction cooling and immersion cooling methods. Most of the developed motors/generators using HTS armature windings in stators [13–15] were immersion cooled. In [13, 14], both the HTS stator and the rotor were entirely immersed in a liquid nitrogen bath. In [15], the HTS armature winding was separately cooled in a liquid nitrogen bath while the stator iron core and rotor were kept warm. The cryostat material was chosen as FRP to avoid eddy loss. Although the immersion cooling method is more efficient than the conduction cooling method, there are still some technical problems. For the total immersion situation, the rotor will stir the coolant, which leads to additional mechanical loss as well as troubles for the rotary sealing. If the HTS armature winding is separately cooled, the cryostat has to be built with non-metal materials to avoid eddy currents, and its structure will be quite complicated.

In our prototype machine [23], the HTS armature coils together with the iron core were cooled using the conduction cooling method, considering that it can be handled easily. The HTS armature coils were not cooled directly by liquid coolant but through the iron core or some other parts. The uncertainty is whether or not the thermal conduction through the iron is fast enough to remove the heat generated from the iron core and the AC loss. Otherwise the temperature increase would greatly influence the current carrying capacity of the HTS coils. Thus analysing and testing the temperature evolution of the HTS stator through the conduction cooling method during generator operation is necessary.

In [23], some preliminary electric properties of our generator in short-time running were tested. To test the long-term running properties and verify the feasibility of the HTS armature through conduction cooling, in the present study, the thermal behaviours of the HTS stator in our HTS-PM generator were investigated through three main aspects: conduction cooling efficiency, iron loss at liquid nitrogen temperatures, and the influence of AC loss on the thermal stability of the HTS stator.

### 2. General structure of the HTS-PM generator

The HTS-PM generator prototype contains a four-pole inner rotor composed of Nd–Fe–B PMs and a six-slot outer stator with HTS armature windings. Some of the basic parameters of this prototype are shown in table 1. All the Bi-2223/Ag racetrack coils were placed side by side in the slots to prevent coil end interference. The iron core was made of silicon steel sheets and was pressure mounted into the hollow cylinder cryostat. The outer surface of the iron core made close contact with the inner surface of the cryostat. To improve the contact condition, the outside surface of the iron core was painted with STYCAST 2850 FT before assembly. The structure is illustrated in figure 1(a).

As depicted in figure 1(b), HTS coils were placed into the iron core slots and fixed with different slot wedges. The slots

### Table 1. Basic parameters of the HTS generator.

| Parameter               | Value                                      |
|-------------------------|--------------------------------------------|
| Air gap flux density    | 0.8 T                                      |
| Stator inner radius     | 150 mm                                     |
| Stator outer radius     | 235 mm                                     |
| Air gap length          | 5 mm                                       |
| Iron core length        | 200 mm                                     |
| Cooling liquid          | LN$_2$                                     |
| Cooling method          | Conduction cooling                         |
| Frequency               | 10 Hz                                      |
| HTS material            | Bi-2223–Ag tape                            |
| Permanent magnet        | Nb–Fe–B                                    |


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were also filled with STYCAST 1266 mixed with aluminium oxide powder to enhance the cooling conditions. Two aluminium rings were fixed at the ends of the iron core to support the HTS coils and to generate additional cooling paths for the end parts of HTS coils. Both the HTS stator and the PM rotor were placed into a vacuum chamber. Liquid nitrogen was poured into the cryostat through the bottom pipe, and the vaporized nitrogen gas was released to the atmosphere through the top pipe. Additional structure details are provided in [23, 24].

3. Experimental details

Six PT-100 platinum temperature sensors were buried in the middle of each slot to measure the temperature evolution of the HTS coils and in the iron core during cooling and operation, as shown in figure 1(b). The measured slot temperature ($T_{slot}$) can approximate the iron core temperature at the same radial position, given that the slot area is smaller than the entire iron core cross section and the slots were filled with epoxy. The other PT-100 sensors were placed at the ends of HTS coils for safety monitoring. All the resistances of these PT-100 sensors were measured with a Keithley 2700 multi-meter. The vacuum degree was measured with a thermocouple gauge. Furthermore, the HTS-PM generator was driven by an 11 kW induction motor whose speed was controlled with a frequency converter. The rated working frequency of this prototype was 10 Hz.

To facilitate the thermal analysis of the HTS stator, the thermal conductivity $\lambda$ within the plane and the specific heat $C_p$ of the silicon steel sheet (grade 50DW400, according to the Chinese GB standard) were measured with a Physical Property Measurement System at various temperatures.

All the experiments were performed once the HTS stator was cooled to a stable temperature above 77 K. Before cooling, the pressure in the vacuum chamber was pumped to 3.1 Pa. After cooling, the HTS-PM generator was driven by the induction motor at the rated rotation speed of 300 rpm. The frequency of the rotating magnetic field was 10 Hz. First the temperature evolution in the slots ($T_{slot}$) was measured in the no-load running test, during which the HTS armature windings were not connected to an external circuit and there were no working alternating currents $I_{A,rms}$. Subsequently, $T_{slot}$ values were measured at different $I_{A,rms}$ during the with-load running tests. The $I_{A,rms}$ values were adjusted from 3.2 A to 23.7 A by reducing the load resistance from 13.7 $\Omega$ to 1.8 $\Omega$. Besides this, the $I_c$ values of a typical coil were also measured at various temperatures using the four-probe method.

4. Results

4.1. Cool down process

During the cooling process, the HTS stator was cooled to a stable temperature above 77 K. Before cooling, the pressure in the vacuum chamber was pumped to 3.1 Pa. After cooling, the HTS-PM generator was driven by the induction motor at the rated rotation speed of 300 rpm. The frequency of the rotating magnetic field was 10 Hz. First the temperature evolution in the slots ($T_{slot}$) was measured in the no-load running test, during which the HTS armature windings were not connected to an external circuit and there were no working alternating currents $I_{A,rms}$. Subsequently, $T_{slot}$ values were measured at different $I_{A,rms}$ during the with-load running tests. The $I_{A,rms}$ values were adjusted from 3.2 A to 23.7 A by reducing the load resistance from 13.7 $\Omega$ to 1.8 $\Omega$. Besides this, the $I_c$ values of a typical coil were also measured at various temperatures using the four-probe method.
4.2. No-load running test

$T_{\text{slot}}$ increased during the no-load running test due to the iron loss induced by the rotating magnetic field. Figure 3 exhibits a typical $T_{\text{slot}}$ as a function of the operation time during this test in both low-temperature and room-temperature situations. In cryogenic conditions, $T_{\text{slot}}$ increased rapidly from 82.1 K before 1000 s. After 5500 s, the increasing rate decelerated significantly. The maximum $T_{\text{slot}}$ increment in this test was approximately 1.3 K. In room-temperature conditions, $T_{\text{slot}}$ increased almost linearly, and the increment was about 1 K. It would be higher if the test were going on.

![Figure 2. Evolution of $T_{\text{slot}}$ at different positions during the cooling process. This process lasted for over 3.5 h until a stable temperature of 82.1 K was reached.](image)

4.3. $I_c$ of the HTS coil

$I_c$ of HTS coils were measured before and after assembly. The result is shown in [23]. Before assembly, $I_c$ of separate HTS coil was around 52 A to 58 A at 77 K (self-field). After assembly, it dropped to 30 A to 33 A at 82.1 K. The $I_c$ values were also measured at different temperatures during the cooling process, as shown in Figure 4. $I_c$ dropped linearly with increasing temperature, and the linear slope was $-1.67$ A K$^{-1}$.

The measurement results showed that all $T_{\text{slot}}$ curves in different slots displayed similar evolution behaviours, including the increase rate and final increment value, though the absolute temperature values differed slightly. In this study, we selected a typical curve to represent the most probable situation.

In [24], an aluminium foil was glued to the inner surface of the iron core to shield the radiative heat transfer from the rotor. However, the aluminium foil was proved to become an induction heat source in the air gap after the analysis. Thus, this foil was removed before the experiments were conducted as described in this paper; the resultant temperature change behaviours deviated from those explained in [24].

4.4. The temperature increment during the load test

Once the HTS armature winding was connected to the external circuit, the HTS armature coils carries the working alternating currents, then power was outputted. Figure 5 shows the relationship between the output power and the armature current $I_{\text{rms}}$ (effective value). The armature current induces AC loss and increases temperature further. Figure 6 depicts the evolution of the selected $T_{\text{slot}}$ measured at different $I_{\text{rms}}$. The frequency is maintained at 10 Hz. The Pt-100 sensors were stuck directly to the straight side of the racetrack coils. Thus, $T_{\text{slot}}$ can represent the coil temperature. According
to the previous discussions, $T_{\text{slot}}$ of a typical slot was selected to represent the typical condition. Given $I_{A,\text{rms}} = 3.2$ and 6.4 A, the HTS generator worked quite stably for 1 h without quench and the $T_{\text{slot}}$ curves were similar to those in the no-load condition. Nonetheless, at higher $I_{A,\text{rms}}$, $T_{\text{slot}}$ increased much faster. When $I_{A,\text{rms}} = 9.6$ A, the measurement lasted for 40 min and the $T_{\text{slot}}$ increment was approximately 2 K. The final $T_{\text{slot}}$ increment was roughly 2.5 K for $I_{A,\text{rms}} = 12.6$ A. This test lasted for only 27 min and did not reach a stable slot temperature due to the protection of the driven motor in case of overheating.

When $I_{A,\text{rms}} = 15.6$ and 18.2 A, $T_{\text{slot}}$ increased rapidly within a short period and showed no inclination of reaching the stable state before the protection. At $I_{A,\text{rms}} = 18.2$ A, $T_{\text{slot}}$ increased by 4.2 K within 500 s and continued to increase, whereas the temperature of another coil increased from 86 K to 92 K after 400 s and finally quenched, as shown in figure 7. The $I_c$ of the quenched coil at 92 K is less than 18 A, as per figure 4.

4.5. The thermal properties of silicon steel at cryogenic temperature

The measurement results of the thermal conductivity $\lambda$ and specific heat $C_p$ of silicon steel are shown in table 2. At an almost-stable cryogenic temperature, $\lambda$ is 9.6 W/(m $\cdot$ K) at 76.7 K, and $C_p$ is 160.5 J/(kg $\cdot$ K) at 79.5 K. These results can be applied to the calculation process as follows.

5. Discussion

5.1. Heat leakage from the rotor to the stator

In this HTS-PM generator, the rotor is expected to remain warm when the stator is cooled down; this expectation induces heat leakage from the rotor to the stator and stabilizes $T_{\text{slot}} > 77$ K, as shown in figure 2. A simple 2D model was built to analyse heat leakage and to estimate the temperature distribution in the stator, as indicated in figure 8. Rotors,
The heat conduction through air and radiation are the main sources of heat leakage. In principle, the quantity of heat leakage at the worst condition can be estimated.

According to [25], the radiation heat flow power \( \dot{Q}_{1,\text{rad}} \) from the rotor to the stator can be expressed as

\[
\dot{Q}_{1,\text{rad}} = \sigma E A_2 (T_1' - T_1^a). \tag{2}
\]

\( T_1 \) and \( T_1' \) represent the temperatures of the outer surface of the rotor and the inner surface of the stator respectively. In consideration of the worst-case condition that maximizes \( \dot{Q}_{1,\text{rad}} \), we set \( T_1 \approx 273K \) and \( T_1' \approx 82.1K \). \( \sigma \) is the Stefan–Boltzmann constant, that is, \( 5.67 \times 10^{-8} \text{ W m}^{-2} \text{ K}^{-4} \) [25]. \( E \) is determined by the emissivities of the two surfaces, and the equation is written as

\[
E = \frac{\varepsilon_1 \varepsilon_1'}{\varepsilon_1 + (A_1/A_1')(1 - \varepsilon_1 \varepsilon_1')}. \tag{3}
\]

\( \varepsilon_1 \) and \( \varepsilon_1' \) are the emissivities of the rotor outer surface and of the stator inner surface, respectively. \( A_1 \) and \( A_1' \) are the corresponding areas of these two surfaces. In this generator, \( A_1/A_1' = 0.93 \), and \( A_1' = 0.0942 \text{ m}^2 \). The outer surface of the rotor is composed of stainless steel, with the emissivity value \( \varepsilon_1 = 0.07 \). The unsmooth surface condition of the iron core as a result of the laminated and unpolished sheet structure leads to \( \varepsilon_1' = 0.6 \) in the worst case. Thus \( \dot{Q}_{1,\text{rad}} = 2.1 \text{ W} \) considering previous discussions.

The maximum heat leakage through air conduction can also be calculated. The pressure in the chamber is 1.6 Pa when the stator is cooled to a stable condition. The mean free path of air at this pressure between 3.9 and 0.68 mm, which is close to the air gap length of 5 mm. In this case, the heat transfer equation in the free-molecule regime can be used to estimate the heat leakage power through air conduction \( \dot{Q}_{1,\text{air}} \) [25]. The following equation is established:

\[
\dot{Q}_{1,\text{air}} = \text{kap} A_2 (T_1 - T_1'). \tag{4}
\]

In (4), \( k \) is a constant; the value for air is 1.2. \( a = 1 \) is a dimensionless factor depending on surface conditions for air [26], and \( p = 1.6 \text{ Pa} \) is the pressure. Thus \( \dot{Q}_{1,\text{air}} \) is estimated to be 34.5 W. The total heat leakage power \( \dot{Q}_1 = \dot{Q}_{1,\text{rad}} + \dot{Q}_{1,\text{air}} \) is 36.6 W. \( \dot{Q}_1 \) is overestimated because the temperature on the outer surface of the rotor should be lower than 273 K, thus \( \dot{Q}_1 \) should be lower than 36.6 W. The radiative heat transfer is rather small compared to the heat conduction through air. The vacuum degree is the key factor to limit heat leakage. If the vacuum can be improved to 0.1 Pa, total heat leakage can be reduced to 4.3 W, which is one order of magnitude lower than that at 1.6 Pa. Hence, the temperature of the iron core can approach 77 K.

### 5.2. The stable temperature of the stator after cooling

In the stable state, the temperature distribution equation applied to the stator and cryostat wall regions is expressed as

\[
\nabla^2 T = 0, \tag{5}
\]

with the boundary condition equation

\[
\dot{Q} = -\lambda A \nabla T. \tag{6}
\]

For the simplified 2D model in figure 8, the temperature distribution is derived as

\[
T = C_1 \ln r + C_2, \tag{7}
\]

which is suitable in the iron core and cryostat wall region.

The outer surface temperature of the stator \( T_2 \) can be calculated as 81.3 K by considering the input heat flow power \( \dot{Q}_3 = 36.6 \text{ W} \) and the measured \( T_{\text{slot}} = 82.1 \text{ K} \) and by applying the thermal property of silicon steel presented in table 2.

On the exterior surface of the cryostat, the heat flux is transferred by liquid nitrogen via the nucleate-boiling regime given that the heat leakage is slight. The heat transfer flux \( \dot{Q}_3 \) in nucleate-boiling mode is calculated by [25]

\[
\dot{Q}_3 = 5 \times 10^2 A_3 (T_3 - T_3')^{2.5}, \tag{8}
\]

where \( A_3 \) is the area of the outer surface of the cryostat, \( T_3 \) is the outer surface temperature of the cryostat wall, and \( T_3' = 77.3 \text{ K} \) is the boiling point of liquid nitrogen. \( T_3 = 78.0 \text{ K} \) is obtained when \( \dot{Q}_3 = \dot{Q}_1 \) in the stable state. The temperature of the inner surface of the cryostat wall \( T_2^i = 78.4 \text{ K} \) is obtained with (5) and by considering \( \lambda (7.9 \text{ W m}^{-1} \text{ K}^{-1}) \) at 77 K for the stainless steel [25].
The temperature differs across the interface between the stator and the cryostat wall, that is, $T_2 - T_1 = 2.9$ K. This variation is caused by the equivalent thermal conductance $C_{eq}$ across this interface, which can be expressed as

$$C_{eq} = \frac{\dot{Q}_2}{A_2(T_2 - T_1)}, \quad (9)$$

where $\dot{Q}_2 = \dot{q}_2$ is the heat transfer flux across that interface and $A_2$ is the area of that interface. Then, $C_{eq} = 85$ W m$^{-2}$ K$^{-1}$. The thermal conductance on the boundary is related to the pressure and surface conditions. At 77 K, $C_{eq}$ for a 4.45 MPa pressed steel–steel interface is $3 \times 10^4$ W m$^{-2}$ K$^{-1}$ [25], which is considerably larger than the $C_{eq}$ calculated previously. Increasing the contact pressure and polishing the iron core surface can increase the thermal conductance $C_{eq}$ further.

The stable $T_1'$ is determined by the air pressure $p$ and equivalent thermal conductance $C_{eq}$ for this machine. Considering equations (2)–(9), the relationship between $T_1'$, $p$ and $C_{eq}$ can be obtained, as shown in figure 9. Compared with $C_{eq}$, the air pressure is more controllable. Keeping a good vacuum degree, such as 0.1 Pa or lower, can guarantee the temperature of the iron core close to that of liquid nitrogen; $C_{eq}$ has a wide range at this air pressure, which is more convenient for the assembly process. In the case of $p = 1.6$ Pa, $C_{eq}$ has to be kept above 600 W m$^{-2}$ K$^{-1}$ in order to keep $T_1' < 80.02$ K. In the case of $p = 0.1$ Pa, $C_{eq}$ above 30 W m$^{-2}$ K$^{-1}$ is sufficient to keep $T_1' < 78.8$ K. It should be pointed out that the free mean path of air molecules is much small than the air gap length in the case of $p > 5$ Pa. Thus the heat transfer through air should be calculated by using heat convection equations, which is much larger than the result of (4).

To predict the behaviour of generators with larger power capacity, we may considering a model with larger iron according to figure 8. The inner radius of iron ($r_i$) varied from 0.075 m to 1 m. The thickness of iron was changed proportionally to $r_i$. The thickness of the air gap and the cryostat wall remained the same. For better cooling efficiency, the air pressure is fixed at 0.1 Pa, and the stable $T_1'$ at different $r_i$ and $C_{eq}$ is shown in figure 10. It is seen that for the case $r_i < 0.4$ m $T_1'$ can stay below 78.83 K with a wide range of $C_{eq}$. However, when $r_i > 0.5$ m, $T_1'$ is never lower than 78.83 K even for $C_{eq} = 10^4$ W m$^{-2}$ K$^{-1}$, which exceeds the equivalent thermal conductance of a steel–steel interface at 77 K and 4.5 MPa. A probable way is to put distributed LN$_2$ pipes across the stator iron, in addition to the present external cryostat. This helps to further reduce the distance between the cold source and the cooling target.

The conduction cooling method can cool the iron core effectively for the present HTS-PM generator. The heat leakage to the HTS stator and the equivalent thermal conductance $C_{eq}$ between the stator iron and the cryostat are two main factors that influence the cooling effect. If the vacuum and the thermal conductance on the boundary are improved, the stable cryogenic temperature of the iron core can approach 77 K after cooling, even for larger dimension of iron core.

### 5.3. Temperature increment caused by iron loss

When the HTS generator begins rotating, the temperature of the iron core increases due to iron loss. The gap field in this prototype is 0.8 T, and the maximum flux density in the iron core is approximately 1.6 T. The iron loss in the silicon steel sheet (50DW400) is 0.96 W kg$^{-1}$ at 0.8 T when $f = 50$ Hz and is 3.8 W kg$^{-1}$ at 1.6 T when $f = 50$ Hz, according to the data provided by the manufacturer. The iron loss is
approximately proportional to \( f^{1.3} \) (\( f \) represents the frequency) [27]; thus, the iron loss is roughly 0.118 W kg\(^{-1}\) at 0.8 T and 0.469 W kg\(^{-1}\) at 1.6 T when \( f = 10 \) Hz. Given an iron weight of 33.14 kg, the iron loss of the stator at room temperature when \( f = 10 \) Hz is expected to be between 3.91 and 15.54 W. If the distribution of magnetic flux density within the iron is considered, the total iron loss at room temperature when \( f = 10 \) Hz is estimated to be 11.5 W by ANSYS Maxwell; this value stays in the same range, as expected.

The resistance of silicon steel decreases with temperature, and the iron core is subject to compression stress due to the mismatch in thermal contraction between the cryostat and the iron core. Both effects enhance iron loss [28–32]. At 50 Hz, the iron loss of the silicon steel sheet increases by approximately 18% at 77 K compared with that at room temperature [29]. However, the working frequency of the present generator is 10 Hz, which is considerably lower than 50 Hz; thus, the enhancement of the iron loss caused by the reduced resistance is limited [28].

If the iron core is simplified to a hollow cylinder that generates heat homogeneously, then the heat transfer equation is expressed as

\[
\nabla^2 T + \frac{q_{\text{iron}}}{\lambda} = \frac{\rho C_p}{\lambda} \frac{\partial T}{\partial \tau},
\]

where \( q_{\text{iron}}, \lambda, \rho \) and \( C_p \) represent the iron loss per volume, thermal conductivity, material density and the specific heat per weight, respectively. \( \tau \) is the time. At the beginning of the no-load running process, the temperature distribution starts to shift from the stable state with \( \nabla^2 T \approx 0 \). Then, (10) can be expressed as

\[
q_{\text{iron}} = \rho C_p \frac{\partial T}{\partial \tau} = \rho C_p G_0,
\]

where \( G_0 = \frac{\partial T}{\partial \tau} \) is the initial temperature increase rate at \( \tau = 0 \). \( G_0 \) can be calculated as approximately \( 2.28 \times 10^3 \) K s\(^{-1}\) by setting the silicon steel density \( \rho = 7650 \) kg m\(^{-3}\) and the specific heat per weight \( C_p \) at cryogenic state as per table 2 and by applying the measured temperature data represented in figure 3. Then, \( q_{\text{iron}} \) can be calculated as \( 2.80 \) kW m\(^{-3}\). The total iron loss is approximately 12.6 W, which is similar to the previously estimated value.

When the HTS generator operates in the no-load running state and the temperature stops increasing, the temperature increase rate \( \partial T/\partial \tau \) is almost 0. Thus, (10) is written as follows for the iron core region:

\[
\nabla^2 T + \frac{q_{\text{iron}}}{\lambda} = 0.
\]

In the iron regime (between \( A_1^r \) and \( A_2^r \)) of the simple 2D model shown in figure 8, the temperature distribution is expressed as

\[
T = -\frac{1}{4} \frac{q_{\text{iron}}}{\lambda} r^2 + C_1 \ln r + C_4,
\]

where \( C_3 \) and \( C_4 \) are constant. The temperature distribution in the cryostat wall is the same as in (7). The heat leakage on the inner surface of the iron core, the equivalent thermal conductance \( C_{\text{eq}} \) across the interface between the iron core and the cryostat and the liquid nitrogen heat transfer regime are similar to those described in section 5.2. Thus, the temperature at the PT-100 position can be calculated as \( 83.3 \) K by setting an iron loss of 11.5 W (estimated via FEA software) and the \( \lambda \) of the silicon steel to that indicated in table 2. This temperature is \( 1.2 \) K higher than the stable temperature prior to no-load running. The experimental result (figure 3) shows that the temperature increment is approximately \( 1.3 \) K, which is close to the calculated results.

The iron loss could greatly influence the HTS stator temperature. Figure 11 shows the \( T^* \) evolution with different \( C_{\text{eq}} \) and iron loss in the case of air pressure 1.6 Pa and 0.1 Pa. It is clear that \( T^* \) increases with increasing iron loss and decreasing \( C_{\text{eq}} \). It is also seen that \( T^* \) is generally higher at

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**Figure 11.** \( T^* \) contour at different iron loss and \( C_{\text{eq}} \) in the case of air pressures (a) 1.6 Pa (present machine situation) and (b) 0.1 Pa (preferred situation). The horizontal axis denotes the iron loss and the vertical axis denotes the equivalent thermal conductance across the iron core and cryostat interface. The temperature rise with iron loss increasing in (a) is slower than that in (b).
1.6 Pa than at 0.1 Pa. In the case where $C_{eq} = 85$ W m$^{-2}$ K$^{-1}$, $p = 1.6$ Pa, $T_{1}'$ will increase to about 89 K if the iron loss is up to 60 W. With the same $C_{eq}$, $T_{1}'$ will be 85 K if $p$ decreases to 0.1 Pa. Lower air pressure leads to smaller heat leakage across the air gap, which would also contribute to the lower $T_{1}'$. Improving $C_{eq}$ can also sufficiently suppress the $T_{1}'$ increment when the iron loss becomes larger. If $C_{eq}$ were improved by one order of magnitude to 850 W m$^{-2}$ K$^{-1}$, $T_{1}'$ would be smaller than 83.09 K in the case of air pressure 1.6 Pa and iron loss 60 W, which is even lower than the experiment result of the present machine. For the case of air pressure 0.1 Pa, the evolution trend is nearly the same. Therefore, improving $C_{eq}$ is important for reducing the temperature increment caused by the iron loss.

From the above estimation, for our present HTS generator, the heat generated by iron loss at 10 Hz is rather small and the temperature increment could be kept less than 2 K by using conduction cooling through the iron core. Improving the equivalent thermal conductance on the interface is necessary for suppressing the temperature increase for future design of HTS rotating machines with similar structure.

### 5.4. Temperature increment caused by AC loss

The AC loss of the HTS coils is a key problem in HTS armature windings, especially when the conduction cooling method is used. When the HTS generator prototype begins rotating, the HTS coils produce heat that increases the temperature in the slot apart from the conducted heat and the iron loss.

Before assembly, the transport AC loss of a separate HTS coil without the iron core is measured at 11 Hz and 77 K (self-field) [23] via the method described in [33]. At 20 A (effective value), the AC loss is approximately 0.4 W with $I_e = 58$ A. It is difficult to measure the AC loss of assembled coils directly because these coils are coupled with the surrounding stator iron, and the measurement data include the contribution of iron losses. When $I_{A,rms} = 20$ A, $I_e = 32$ A and $f = 10$ Hz, the transport AC loss of a coil is estimated to be 0.75 W [23]. The real AC loss of an assembled coil should exceed the estimated value. Both the iron coupling [34] and the leakage alternating flux generated by the rotor increase AC losses.

Although we do not know the precise AC loss value, we can see its influences on slot temperatures. Figure 6 depicts the temperature increment of the HTS coils when different $I_{A,rms}$ are carried during the load test. When $I_{A,rms}$ is low (3.2 and 6.4 A), the temperature curves are close to those of the no-load running condition. This finding suggests that the heat generated by the AC loss in the case of low $I_{A,rms}$ can be conducted easily through conduction cooling; thus, temperature hardly increases any further and the HTS generator can run stably. Given $I_{A,rms} = 12.6$ A, the temperature increment after 1500 s is over 2.5 K; this increase is higher than 1.3 K in the no-load running test. The influence of AC loss at high $I_{A,rms}$ is therefore clearly important. In case of $I_{A,rms} > 12.6$ A, the AC loss produced too much heat, that exceeds the cooling capacity of the present method. Thus the temperature increase in the slot is prominent and stable running cannot be achieved.

As per figure 1(b), the AC loss in the HTS coils is a local heating source within slots. The absolute value of this loss may not be very high because of the low working frequency; however, the loss per volume is large because the volume of the HTS coils is considerably smaller than that of the iron core. The heat generated by the AC loss must be conducted across the epoxy, contacting interface and the stator iron in the present HTS-PM generator. Moreover, the cooling condition within the slots determines the stable temperature during the load running. Improving the cooling condition of the HTS coils is important for increasing the stable working current and, consequently, the power of the HTS generator.

### 6. Summary

A prototype HTS generator was developed to determine the feasibility of HTS armature windings. HTS coils can be cooled down to 82.1 K through conduction cooling. The maximum heat leakage from the rotor was estimated to be 34.6 W at a vacuum degree of 1.6 Pa. The iron loss was about 2.8 kW m$^{-3}$ at 10 Hz. The equivalent thermal conductance $C_{eq}$ between the iron core and cryostat was estimated to be 85 W m$^{-2}$ K$^{-1}$. The final temperature increment of the stator during the no-load running period was 1.3 K. This increment was very close to the estimated value. Given low alternating currents $I_{A,rms} = 3.2$ and 6.4 A, the AC loss of the HTS coil could hardly influence the slot temperature. At elevated working currents, however, the heat generated by the AC loss produced a rapid increase of $T_{slot}$, and even led to quench of one HTS coil in case of $I_{A,rms} = 18.2$ A.

It is suggested that the conduction cooling method is sufficient to cool down the iron core. This method can also compensate the temperature increment caused by the iron loss. The heat leakage to the HTS stator and the equivalent thermal conductance $C_{eq}$ between the stator iron and the cryostat are two main factors that influence the cooling effect. Using the analysis method, the thermal behaviours of HTS stators with similar structure can be predicted. For further reducing the stable temperature of the iron core and compensating temperature increase in a higher iron loss situation, the air pressure is preferred to be 0.1 Pa or lower, and $C_{eq}$ should be improved. Furthermore, the thermal conduction of the HTS windings inside the slots for this prototype may require improvement to take away the heat generated by the AC loss.

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