Induced currents and AC losses models for a butt-
joint with Rutherford’s shunts
A. Torre, T. Schild, E. Gaxiola, P. Decool, G. Jiolat

Abstract—The ITER Central Solenoid (CS) has terminal butt-
type joints called Coaxial joints. It was decided to study a design
of this joint with Rutherford shunts, and to build models for its
resistive and inductive behaviors. In particular, the behavior of
the joint under magnetic field transients is investigated with var-
ious analytical models that are compared with a FEM model. The
key point of the study was to verify that the induced currents
were reasonable and would not induce flux jumps in the ruth-
fereds. A prototype with simplified geometry was tested in the
CEA Josefa facility under various field ramps. The results are
presented and discussed.

Index Terms—Nuclear Fusion, Superconducting Joints.

I. INTRODUCTION

The ITER Central Solenoid (CS) is composed of six
modules, each one a stack of 6 hexa-pancakes and one
quad-pancake. A module has thus 6 internal joints, of butt-
type configuration, which are called splice-joints. The two
terminals of each module are connected to vertical supercon-
ducting extension lengths that are in turn connected to the
feeder system. The connection to the extension length is made
using another butt-joint configuration called “Coaxial Joint”.
Two designs are considered for this joint: The baseline Lanced-
Union Design (LUD) and the Parallel Rutherford’s Design
(PRD). The work presented here tries to summarize the ap-
proaches to model the AC losses and induced currents loops in
the two design, with the aim of evaluating whether the recent
design is still acceptable with regards to transient magnetic
field during operation.

II. COAXIAL JOINTS DESIGNS DESCRIPTION

A. Joint Specification and General Description

The CS coaxial joint must electrically connect two com-
pacted Nb:Sn CS cables facing each other in a butt-type ge-
ometry, while achieving a DC resistance below 4.1 nΩ. To
achieve this, the cables are initially compacted in a copper
tube (crimp tube) to form a terminal lead. Then, a supercon-
ducting shunt is added to bypass both leads positioned head-
to-head. In the two designs studied here, one uses a cylindrical
layer of twisted strands (LUD), while the other uses straight
rutherford’s (PRD). In both designs, the superconductors are
embedded in copper shells, compacted or soldered to the lead.

B. Original LUD Design

The LUD baseline design for this joint has been studied,
tested and analyzed in detail in the past years (see [1]-[4]). Its
design will not be detailed here, but its main components are
illustrated in Fig. 1. It should be noted that, in this design, the
laced union is soldered to the terminal lead.

Fig. 1. LUD joint design (courtesy David Everitt)

For the analyses lead hereafter, the useful characteristics of
this design are summarized below:

<table>
<thead>
<tr>
<th>TABLE I COAX-LUD PARAMETERS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Name</td>
</tr>
<tr>
<td>Joint total length</td>
</tr>
<tr>
<td>Laced Union strands twist pitch</td>
</tr>
<tr>
<td>Laced Union inner radius</td>
</tr>
<tr>
<td>Outer copper shell average thickness</td>
</tr>
</tbody>
</table>

C. PRD Design

A new design has been proposed to try to simplify part of
the assembly process by the use of straight superconducting
shunts (rutherford’s) and the use of indium wire compaction in-
stead of soldering. Again, the details of the design will not be
presented, but Fig. 2 and Table II give the important infor-
mation for our calculations. It should also be noted that the de-
sign illustrated is not the final one and is only indicative of the
concept used. In particular, the number of rutherford’s (2 or 4)
is still uncertain, and in the model proposed in section IV. C.,
we will consider only 2 rutherford’s shunts in parallel (one top
and one bottom in Fig. 2.).
\( B = \mu R \). In the case of the \( 1_1 \), see \( 2_2(\theta) = \mu S_2 \).

Where \( \rho_{\text{Cu}} \) is the external applied field, and \( \theta \) is the sum of all time constants contributing to the losses. In the case of the LUD joint, considering that the laced union shields the rest of the cable, we get \( \theta = 18 \) s. Furthermore, the shielding currents in this model have a cosine distribution and can be expressed as a linear current density:

\[
J_L = B_t \left( \frac{L_p}{2\pi} \right) \frac{1}{\rho_t} \cos(\phi) = \frac{2\theta_s B_i^2}{\mu_t} \cos(\phi) \quad [A/m]
\]

The figure above shows that losses calculated with \( B_t \) (around 0.25 W) are much lower than the approximation with \( B_s \) (around 90 W). The figure gives also the total screening current for one half of the laced union. Of course, the most critical part of the laced union is the one perpendicular to the changing field, in which the current will rise to around

### III. DC Resistance Considerations

#### A. Materials DC resistance

Although DC resistance is not the main goal of the models described in this paper, it is important to evaluate the resistive paths in order to include their influence in the inductive behavior. The resistance of a cylindrical shell defined by its inner radius \( r_1 \), outer radius \( r_2 \), length \( L \) and resistivity \( \rho \) is simply \( R_{\text{mat}} = \rho(2\pi L)\ln(r_2/r_1) \). Taking \( \rho_{\text{Cu}} = 2.5 \times 10^{-2} \Omega \cdot m \) and \( \rho_\ell = 1 \times 10^{-2} \Omega \cdot m \) for the copper and cable transverse resistivities respectively we find 0.28 n\( \Omega \) and 0.30 n\( \Omega \) for the LUD and PRD joints. The resistivities are only indicative (not measured), but give an estimate of the materials contribution to DC resistance.

#### B. Interfaces resistance

While estimating the interfaces (contact, solder etc...) resistance from the geometry is not relevant, we know from the experimental values (on the LUD joint, see [3]) that they are dominating the DC resistance. Therefore, we propose to add to the material resistance \( R_{\text{mat}} \) an interface resistance \( R_{\text{int}} \) in the form a resistive barrier homogeneous on the cylindrical surface of the joint. If we take the interface at a radial position of 18.7 mm (position of the crimp tube), the cylindrical surface of the joints amounts to \( S_1 = 16283 \) mm\(^2 \), and we get:

\[
\rho \Delta \varphi = R_{\text{int}} \times S_1
\]

Where \( \rho \Delta \varphi \) defines the interface resistance in \( \Omega \cdot m^2 \). In this case, for \( R_{\text{int}} = 1 \) n\( \Omega \), we have \( \rho \Delta \varphi = 8.14 \times 10^{-12} \Omega \cdot m^2 \).

### IV. AC Losses and Induced Currents

#### A. Introduction

It is important to note that while the PRD Design was proposed to simplify the joint assembly process and get a more reliable DC resistance, its inductive behavior was never assessed experimentally. In particular, this joint will be subject to magnetic field variations of about 0.1 T/s during one second in both transverse and parallel directions. Furthermore, analytical models to represent the inductive behavior of these joints are necessary to calculate the heat loads during operation, which is generally done by implementing AC losses models in a thermohydraulic code. Finally, one key point was to verify that the PRD design is not subject to high circulating currents which could lead to flux jumps during operation.

#### B. LUD Joint AC losses model in transverse field transient

Since the LUD joint is surrounded by a twisted superconducting layer, we propose for this joint an AC model relying on the composite (strand) model described partly in [5] and illustrated in the equations and figure below.

\[
\begin{align*}
\theta_{\text{Cu}} &= \frac{\mu_0}{8\pi \rho_{\text{Cu}}} \left( \frac{R_2^4}{R_2^2} - \frac{R_1^4}{R_1^2} \right) \\
\theta_{\text{Sc}} &= \frac{\mu_0}{2\pi \rho_{\text{Cu}}} \left[ \frac{L_p}{2\pi} \frac{1}{R_2^2} \right] \left( \frac{R_1^2}{R_2^2} - \frac{R_2^2}{R_1^2} \right) \\
\theta_{\text{Sc/Cu}} &= \frac{\mu_0}{2\pi \rho_{\text{Cu}}} \left[ \frac{L_p}{2\pi} \frac{1}{R_2^2} \right] \left( \frac{R_1^2}{R_2^2} + \frac{R_2^2}{R_1^2} \right)
\end{align*}
\]

Where \( \theta_{\text{Cu}} \) is linked with eddy currents, \( \theta_{\text{Sc}} \) is linked with coupling currents and \( \theta_{\text{Sc/Cu}} \) is linked with the induced currents in the outer copper shell. This model describe both the internal field \( B_t \) and the power \( P \) dissipated by the following equations:

\[
B_t = B_s - \theta_s \dot{B}_s; \quad P = \frac{2\theta_s B_i^2}{\mu_t}
\]

Where \( B_s \) is the external applied field, and \( \theta_s \) is the sum of all time constants contributing to the losses. In the case of the LUD joint, considering that the laced union shields the rest of the cable, we get \( \theta = 18 \) s. Furthermore, the shielding currents in this model have a cosine distribution and can be expressed as a linear current density:

\[
J_L = B_t \left( \frac{L_p}{2\pi} \right) \frac{1}{\rho_t} \cos(\phi) = \frac{2\theta_s B_i^2}{\mu_t} \cos(\phi) \quad [A/m]
\]
200 A/mm, which seems sustainable by the two layers of 0.8 mm diameter strands that compose the laced union.

C. PRD joint AC losses model under transverse field

It is trickier to estimate the losses for this design since we cannot rely on models related to composites (circular geometry, uniform internal field) as for LUD. Since the rutherford are parallel and define an equipotential on each side of the joint, we propose to represent the screening of the external field by a 1D diffusion equation of the field in the longitudinal direction of the joint, with in this case, linear time-dependent Dirichlet boundary conditions:

$$\Delta B - \frac{\mu_0}{\rho} \frac{\partial B}{\partial t} = 0$$

with $B(0,t) = B(L_j,t) = B_0 t$

Where $B_0$ is the constant time derivative of the external field (for our current case study, $B_0 = 0.1$ T/s). The solution, given in terms of eigenfunction expansion, takes the form:

$$B(x,t) = B_0 t + \frac{2B_0 L_j}{\pi^2 \alpha^2} \sum_{n=1}^{\infty} \frac{1}{n^2} \left( e^{-\alpha^2 \left( \frac{n\pi x}{L_j} \right)^2} - 1 \right) \sin \left( \frac{n\pi x}{L_j} \right)$$

Where $\alpha = (\rho' \mu_0)^{1/2}$. When $B(x,t)$ is known, the transverse current density between the two rutherford $J_i(x,t)$ is simply deduced by a Maxwell-Ampere law, and the circulating current in the rutherford is found by integration along $x$:

$$I(x,t) = \frac{2wB_0 L_j}{\mu_0 \pi^2 \alpha^2} \sum_{n=1}^{\infty} \frac{1}{n^2} \left( e^{-\alpha^2 \left( \frac{n\pi x}{L_j} \right)^2} - 1 \right) \sin \left( \frac{n\pi x}{L_j} \right)$$

Where $w$ is the width of the screened volume (here we will take the width of the rutherford). Since the equation is 1D, a demagnetization factor (taken equal to 2 between slab and rod geometry, see [9]) is needed to represent the 3D geometry. The equations above give the field and current distributions of Fig. 5 for the usual case of 0.1 T/s during 1 s followed by a plateau.

If we take a transverse resistivity inside the cable of $\rho_3 = 1 \times 10^{-9}$ $\Omega$ m, we can calculate the power dissipated in the joint during the field ramp.

D. PRD joint AC losses under axial field

The joint will also be subject to changing axial field of about 0.1 T/s. There are no pure analytical models for the axial losses, and to give an idea, one can calculate the magnetic energy for $\Delta B = 0.1$ T field variation by $E_{\text{mag}} = \Delta B^2 / \mu_0$ which gives about 1.9 J deposited in the full joint for a perfect shielding. Considering an adiabatic deposit, without considering the helium, this corresponds to a temperature increase to about 7 K. When considering helium, it becomes negligible. Now for the induced currents in this configuration, we had to rely on our COMSOL model (illustrated in Fig. 7). The integration of current density in the rutherford region gives less than 100 A carried by the rutherford as the shielding currents in the cable loop through these superconducting shunts.

V. EXPERIMENTAL CAMPAIGN & MODEL VALIDATION

A. Sample design and manufacture

Since no PRD joint had been manufactured or tested, we decided to build a simplified mockup, with only two straight rutherford soldered after heat treatment on a copper bar representative of the joint geometry (see Fig. 8).
Then, the mockup was instrumented with a pickup coil and tested in the JOSEFA facility, where a superconducting dipole can apply an external varying field up to around 1 T/s. The pickup has 21 turns ($n_s$) and a cross-sectional area of $1.63 \times 10^{-2}$ m$^2$ ($S_t$).

**B. Test Campaign**

There were two main interests in this test campaign. First, we would be able to cross-check our 1D analytical model and see if it is consistent with the more complex geometry of the joint. Then, we would also be able to increase the field-variation rate, and see if some instabilities (flux-jumps) arise. The test program included background fields $B_e$ of 0.1 T, 0.4 T, 0.6 T and 0.8 T with ramp-up times of 1 s, 2 s, 5 s and 10 s for each. The sample pickup coil voltage $V_s$ gives us access to an effective magnetization defined by:

$$M_{eff} = B_e - \int_0^t \frac{1}{n_s} \frac{dV}{dt} dt$$

The field ramps and measured magnetizations are shown in the Fig. 9.

![Graph showing field ramps and measured magnetizations](image)

Fig. 9. $B_e = 0.1$ T runs: B, plain lines / $M_{eff}$, dashed lines

The curves for other background fields are similar, and, just looking at $M_{eff}$, no thermal instabilities are visible. Nevertheless, when looking at the sample pickup voltage directly, the sensitivity is higher (before integration), and for 0.6 T runs, we start to see fluctuations of the signal which could indicate the onset of flux jumping phenomena (see Fig. 10).

![Graph showing pickup voltages for $B_e = 0.6$ T runs](image)

Fig. 10. Pickup voltages for $B_e = 0.6$ T runs

**C. Model-Experiment comparison**

Using the 1-D diffusion model above to calculate the flux, we can model the induced voltage in the experiment and compare with experimental values. The field inside the sample was considered homogeneous in the cross-section, and decreasing linearly in the rutherford thickness. With these assumptions, the model gives good agreement with the measured voltages as shown in Fig. 11 for 0.1 T runs.

This comparison validates the model, and gives confidence in the predicted induced currents $i_{ind}$ and losses values. Knowing the resistivity of the copper used for the sample, we can also deduce the power dissipated $P_{tot}$. We also made a fully adiabatic evaluation of the maximum copper temperature rise $T_{max}$, although without the helium enthalpy, this temperature is very pessimistic.

![Graph showing modeled and experimental pickup voltages for $B_e = 0.1$ T runs](image)

![Table III: Model Losses Calculation for T=1s Runs](image)

<table>
<thead>
<tr>
<th>Runs</th>
<th>$I_{ind}$ [kA]</th>
<th>$P_{tot max}$ [W]</th>
<th>$E_{tot 1s}$ [J]</th>
<th>$E_{tot 20s}$ [J]</th>
<th>$T_{max 1s}$ [K]</th>
<th>$T_{max 20s}$ [K]</th>
</tr>
</thead>
<tbody>
<tr>
<td>$B_e = 0.1$ T</td>
<td>3.19</td>
<td>0.084</td>
<td>0.037</td>
<td>0.50</td>
<td>5.65</td>
<td>8.13</td>
</tr>
<tr>
<td>$B_e = 0.4$ T</td>
<td>12.8</td>
<td>1.35</td>
<td>0.61</td>
<td>8.00</td>
<td>12.4</td>
<td>18.1</td>
</tr>
<tr>
<td>$B_e = 0.6$ T</td>
<td>19.9</td>
<td>3.26</td>
<td>1.52</td>
<td>19.4</td>
<td>15.9</td>
<td>22.3</td>
</tr>
<tr>
<td>$B_e = 0.8$ T</td>
<td>25.6</td>
<td>5.40</td>
<td>2.51</td>
<td>32.1</td>
<td>18.1</td>
<td>25.0</td>
</tr>
</tbody>
</table>

These values show that with ramp rates of around 0.1 T/s during 1 s, the induced currents are acceptable for this joint configuration, even including a maximum of 100 A induced by axial field variations. Therefore, there should be no instabilities arising in this design. The adiabatic temperature values are obviously pessimistic, but show that for higher ramp-rates, a badly cooled joint could be subject to high heat loads that might lead to flux jumping.

**VI. Conclusion**

The CS Coaxial joint is a critical sub-element of the crucial ITER Central Solenoid system. In the frame of the assembly preparation, an alternative design (PRD) is investigated, which makes use of parallel rutherford shunts. Since this design is not tested up to now, models had to be developed to check that circulating currents and AC losses were comparable to the previous design, and acceptable for the joint operation. A 1D diffusion model is proposed and validated on experimental data in the Josefa facility. Using this model, the currents and losses are calculated and show that the design is sound and should not suffer from thermal instabilities in the ramp-rates considered in operation.
REFERENCES