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Mechanical behavior of a Nb₃Sn accelerator magnet during a quench

THÈSE

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Résumé

La prochaine génération d'aimants supraconducteurs pour des accélérateurs de particules permettra d'atteindre l'ambitieux objectif de produire régulièrement des niveaux de champs magnétiques supérieurs à 10 T. Cette recherche de hautes performances magnétiques se traduit toutefois par de nouveaux défis lors de la conception des aimants. En particulier, l'augmentation incessante des densités de courant dans les conducteurs, l'accroissement des énergies stockées dans les aimants et la présence d'importantes forces électromagnétiques ont repoussé les frontières du design des aimants et de leur protection vers de nouvelles limites.

Une récente avancée cruciale pour atteindre des nouveaux horizons de champ magnétique en utilisant des aimants supraconducteurs a été, sans aucun doute, le développement de supraconducteurs à haute performance capables de fonctionner au-delà des limites de la technologie Nb-Ti. Les nouveaux aimants à champ magnétique élevé sont principalement fondés sur des conducteurs en Nb₃Sn (niobium-étain), qui présentent des paramètres critiques supérieurs à ceux du Nb-Ti. Par exemple, les dernières configurations des fils en Nb₃Sn permettent le transport de densités de courant dans la zone hors cuivre (J) supérieures à la valeur de $J = 1000 \text{ A mm}^{-2}$ à 16 T et 4.2 K. Une telle remarquable performance ouvre la voie à la réalisation de champs records au sein d'unités magnétiques compactes.

L'une des particularités marquantes de la technologie Nb₃Sn est cependant la faible résistance aux charges mécaniques et la grande fragilité du matériau. Il est aujourd'hui bien connu que les propriétés supraconductrices du Nb₃Sn présentent une forte dépendance à la déformation mécanique, comme le prouve une grande variété d'expériences. En termes pratiques, la performance du conducteur est susceptible de souffrir une réduction indésirable si les contraintes mécaniques dans les bobines supraconductrices dépassent certaines valeurs tolérées. Cette diminution de la capacité de transport du courant peut se produire de manière réversible ou irréversible. Bien qu'en termes stricts l'une ou l'autre de ces réductions doivent être évitées, la diminution permanente est particulièrement dangereuse pour le fonctionnement de

l'aimant et doit être soigneusement éludée.

En raison de l'importance de cet aspect, la conception mécanique devient un facteur clé dans la production de nouveaux aimants Nb_3Sn . À l'heure actuelle, une grande majorité des designs proposés entraînent des contraintes mécaniques maximales dans les bobines de l'ordre de 150 MPa à 200 MPa pendant l'assemblage et le fonctionnement de l'aimant. Ces valeurs de contrainte approchent, et dans certains cas dépassent, les limites du conducteur actuellement comprises. Il reste donc une marge réduite pour absorber d'éventuelles contraintes supplémentaires qui pourraient soudainement apparaître émanant d'autres sources.

Si la mécanique de l'aimant peut être considérée comme un sujet crucial pour la nouvelle technologie Nb_3Sn , le second défi le plus important est peut-être lié à la protection de l'aimant lors d'une transition résistive (quench). Les grandes énergies stockées et les densités de courant mentionnées ci-dessus, poussent les exigences en matière de protection vers des objectifs très stricts, où des hautes températures dans les bobines (limitées à 350 K dans les nouveaux designs) et d'importants gradients thermiques peuvent être atteints pendant la phase transitoire lors d'un quench.

La combinaison de toutes les caractéristiques présentées dans les deux derniers paragraphes fait apparaître une nouvelle préoccupation fondamentale pour les aimants en niobium-étain: la possible apparition de charges mécaniques maximales pendant un quench, pouvant mettre en premier lieu en danger l'intégrité du supraconducteur (sensible à la contrainte), ainsi que la structure de l'aimant en général. Les origines de ces nouvelles charges sont notamment 1) la dilatation thermique localisée, qui génère des contraintes thermiques dans le système et 2) le transitoire électro-magnétique (e.m.), qui crée au contraire un changement brusque des forces e.m..

L'analyse de la mécanique de l'aimant pendant le quench devient, par conséquent, essentielle et est traitée dans la première partie de cette thèse. En utilisant une nouvelle approche, nous fournissons une analyse tridimensionnelle exhaustive du sujet sur la base de l'étude de cas liés au nouvel aimant Nb_3Sn , connu sous le nom de MQXF. L'analyse est réalisée via les techniques de modélisation par éléments finis (FEM), dont l'utilisation lors de la conception de systèmes supraconducteurs est bien

établie. Dans un premier temps, un modèle numérique 3D reproduisant la propagation du quench et la décharge de l'aimant a été développé en ANSYS APDL. Ce modèle a été validé grâce à un grand nombre de signaux expérimentaux enregistrés lors de tests réels. Lors d'une étape ultérieure, nous profitons des résultats de ce calcul pour transférer les charges thermiques et électromagnétiques nécessaires (à l'aide d'un calcul électromagnétique standard) au modèle mécanique de l'aimant, également construit dans ANSYS APDL. Le lien entre les simulations permet l'étude du comportement mécanique de l'aimant fondée sur une approximation quasi-statique.

La dernière partie de la thèse concerne la caractérisation électromécanique des supraconducteurs Nb_3Sn . A ce sujet, une campagne expérimentale complète a été mise en place pour étudier les principaux effets des charges mécaniques (appliquées axialement et transversalement) affectant les performances du conducteur. Les expériences ont été réalisées sur des fils supraconducteurs, permettant une analyse plus rapide et plus approfondie de la phénoménologie. Les échantillons pour la campagne de tests ont été extraits du fil utilisé pour la production des aimants quadripolaires MQXF. Ainsi, l'étude permet une comparaison directe entre les limites électromécaniques trouvées et les charges prédites pendant un quench par les modèles d'éléments finis.

Summary

The next generation of superconducting accelerator magnets will turn into a reality the ambitious objective of routinely producing magnetic field levels beyond 10 T for the interaction with particle beams. This pursuit of high magnetic performances translates, however, into novel challenges for the magnet design. In particular, the unceasing increase in the conductor current densities, the growing magnet stored energies, and the presence of large electro-magnetic forces have pushed the boundaries of the magnet conception and its quench protection towards new limits.

A crucial breakthrough for the recent attainment of new magnetic field horizons in accelerator magnets has been, undoubtedly, the development of high-performance superconductors capable of operating beyond the limits of Nb-Ti technology. New high-field magnets are mostly based on Nb₃Sn (niobium-tin) conductors, which present superior critical parameters when compared to Nb-Ti. For instance, state-of-the-art Nb₃Sn wires enable the transport of current densities in the non-copper area (J) that are above the value of $J = 1000 \text{ A mm}^{-2}$ at 16 T and 4.2 K. Such a remarkable performance ultimately paves the way towards the achievement of record fields in compact magnet units.

One of the most relevant particularities of Nb₃Sn technology is, on the contrary, the reduced tolerance to mechanical loads and the brittleness of the material. It is today well-known that the superconducting properties of Nb₃Sn present a strong dependence in mechanical strain, as proven by a large variety of experiments. In practical terms, the conductor performance is prone to experience an undesired reduction, if the loads in the superconducting coils exceed given tolerable values. This decrease in the current transport capability may happen both in a reversible and an irreversible manner. Although in strict terms either of the reductions should be prevented from happening, the permanent decrease is specially dangerous for magnet operation and must be carefully avoided.

As a result of the importance of this aspect, the mechanical design becomes a key factor in the production of new Nb₃Sn magnets. In the present days, a large

majority of the proposed designs entail peak stresses in the coils that are in the range of 150 MPa - 200 MPa during magnet assembly and operation. These stress values approach, and in some cases surpass, the currently understood limits of the conductor. There exists, hence, a reduced margin to additional loads that could promptly appear from other sources.

If the magnet mechanics can be seen as a crucial topic for new Nb₃Sn technology, perhaps the second most important challenge is related to the magnet protection during a quench. The aforementioned large stored energies and current densities push the requirements on the quench protection towards very stringent targets, where large coil temperatures (limited to 350 K in novel designs) and severe thermal gradients can be reached during a quench transient.

From the combination of all the features presented in the two last paragraphs, a new fundamental concern arises for niobium-tin magnets: the possible appearance of peak mechanical loads during a quench, which can put in serious risk the integrity of the strain sensitive superconductor first, and of the magnet structure in general. The origins for these new loads are namely: 1) the localized thermal expansion of the warm parts, which generates thermal stresses in the system and 2) the fast electro-magnetic (e.m.) transient, which creates instead an abrupt change in the e.m. forces.

The analysis of the magnet mechanics during a quench becomes, by implication, essential and is treated in the first part of this thesis. Using a novel approach, we provide an exhaustive three-dimensional analysis on the topic based on the case study of the new Nb₃Sn MQXF accelerator magnet. The analysis is performed via Finite Element Modelling (FEM) techniques, whose use in the design of superconducting systems is well-established. On first instance, a 3D numerical model reproducing the quench propagation and the magnet quench discharge has been developed using ANSYS APDL. This model has been validated using a large amount of experimental signals recorded during real tests. In a next step, we profit from the output of this computation to transfer the necessary thermal and electro-magnetic loads (with the help of a standard electro-magnetic calculation) to the magnet mechanical model, also built in ANSYS APDL. The link between the simulations enables the study of

the magnet quench mechanics based on a quasi-static approximation.

The last part of the thesis deals, however, with the electro-mechanical characterization of Nb_3Sn superconductors. On this subject, a complete experimental campaign has been carried out to study the main effects of mechanical loads (both axially and transversely applied) in the conductor performance. The experiments were performed on single superconducting wires, allowing for a faster and more in-depth analysis of the phenomenology. The samples for the test campaign were extracted from the wire used in the production of the MQXF quadrupole magnets. Thus, the study allows for a direct comparison between the found electro-mechanical limits and the peak loads predicted during a quench by the Finite Element models.

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Contents

1	Introduction	1
1.1	A general overview on applied superconductivity	1
1.2	Applications: practical superconductors	5
1.3	Nb ₃ Sn: a strain sensitive superconductor	10
1.4	Superconducting accelerator magnets	12
1.5	Quench	16
1.6	Quench protection	19
1.7	Scope and structure of the work	24
1.8	Contents of the thesis	26
2	MQXF: The $low - \beta$ quadrupole magnet for the High Luminosity upgrade of the LHC	29
2.1	Particle accelerators: LHC, the Large Hadron Collider at CERN . . .	29
2.2	HL-LHC: The high luminosity upgrade	32
2.3	The MQXF $low - \beta$ quadrupole	34
2.3.1	Superconducting strand and cable	36
2.3.2	Magnet design	38
2.3.3	Magnet instrumentation	50
2.3.4	Quench protection	51
2.3.5	Available test results	55
3	Three-dimensional Finite Element modelling of quench transients: Multi-physics simulation	59

3.1	Introduction to quench modelling	59
3.1.1	Numerical simulation and main challenges	59
3.1.2	The quench simulation concept	63
3.1.3	References on available codes	66
3.1.4	The Finite Element Method (FEM)	67
3.2	A 3D FEM quench simulation built in ANSYS APDL	69
3.2.1	The quench simulation algorithm	71
3.2.2	The 3D coupled thermal-electric model	73
3.2.3	The superconductor model	77
3.2.4	The decoupled magnetic field model	78
3.2.5	The circuit model	82
3.2.6	The external losses model	83
3.3	Model Validation	85
3.3.1	The cable experiment for quench propagation	86
3.3.2	The MQXF short model magnet test campaign	95
3.4	The reference nominal current quench case	107
4	Magnet mechanical behavior during a quench	111
4.1	Load transfer	111
4.2	MQXF - Mechanical models description	113
4.2.1	Theory basis	114
4.2.2	Model geometries	119
4.2.3	2D Mechanical models	120
4.2.4	3D Mechanical models	121
4.2.5	Material properties	121
4.2.6	The MQXF standard modelling steps	122
4.2.7	MQXF - Mechanical models validation	124
4.3	Magnet mechanical behavior during assembly and operation	126
4.3.1	Simulation parameters	127
4.3.2	2D Mechanical analysis	130

4.3.3	3D Mechanical analysis	131
4.3.4	The pole unloading mechanism	134
4.4	Modelling of the mechanical response during a quench	136
4.4.1	References to similar analyses	138
4.5	Mechanical behavior during a quench: Model validation	138
4.5.1	Experimental mechanical readings during a quench	139
4.5.2	Comparison with modelling results	146
4.5.3	Coil stresses during the QI tests	152
4.5.4	Conclusions	154
4.6	Reference quench simulation	156
4.6.1	The symmetric simulation approach	158
4.6.2	2D Results	159
4.6.3	3D Results	165
4.7	The average temperature simplification	172
4.8	A deeper insight into the link with the magnet stored energy	173
4.9	Application to High MIITs tests	175
4.10	Conclusions	178
5	Electro-mechanical characterization of Nb₃Sn superconducting wires	179
5.1	The importance of mechanical loads	179
5.2	The strain dependence of the superconducting properties	181
5.3	A closer look to Nb ₃ Sn	182
5.3.1	The scaling law for J_c in practical Nb ₃ Sn superconductors	183
5.3.2	Distinguishing the sources of performance degradation	188
5.4	Measurement strategies for practical superconductors	189
5.5	Experimental campaign description	190
5.5.1	Investigated wires	191
5.5.2	Measurement setup: I_c vs. axial strain	191
5.5.3	Measurement setup: Stress vs. strain	196
5.5.4	Measurement setup: I_c vs. transverse stress	196

5.5.5	Background: The transverse tests	198
5.6	Results	200
5.6.1	I_c vs. axial strain	200
5.6.2	I_c vs. transverse compressive stress	203
5.6.3	An insight into the degradation mechanisms of RRP Nb ₃ Sn wires	212
5.7	Application of the test results to the MQXF magnet	214
5.8	Conclusions	216
6	Conclusions and outlook	219
6.1	Conclusions	219
6.2	Outlook	224
A	A quick note on the fundamentals of accelerator physics used in this thesis	227
B	Application of the Finite Element Method	231
C	Coupling methods available in ANSYS APDL	239

Chapter 1

Introduction

The objective of this chapter is to present a succinct introduction to the fields of applied superconductivity and superconducting accelerator magnets. We will begin the discussion by reviewing the main particularities of useful superconductors, putting special emphasis in the description of Nb₃Sn technology. The main challenges in the design of accelerator magnets will be treated next, in order to provide a solid basis for our future studies. To conclude, an overview on quench processes and their necessary protection will be tackled at the end of the chapter.

1.1 A general overview on applied superconductivity

In 1911, Heike Kamerlingh Onnes observed for the first time the existence of a superconducting state associated with the transport of electrical current in matter. Attributed to the group of Prof. Onnes at the University of Leiden on the 8th of April 1911, the discovery took place during a series of experiments devoted to measure the electrical resistivity of pure metals at low temperature [1]. Tests done in a high purity Mercury wire showed the absence of resistivity for the material when the sample was cooled down below its (later called) critical temperature ($T_c = 4.2\text{ K}$). This finding, whose significance was immediately realized¹ by H. K. Onnes, received the name of

¹Although not perceived by the team at University of Leiden, the same experiment constitutes as well the first time that the superfluid transition of liquid Helium at 2.2 K was observed. The laboratory notes recorded during the experimental campaign unveiled years later all the details

superconductivity. An historic plot reproducing the tests at University of Leiden is shown in Figure 1-1.

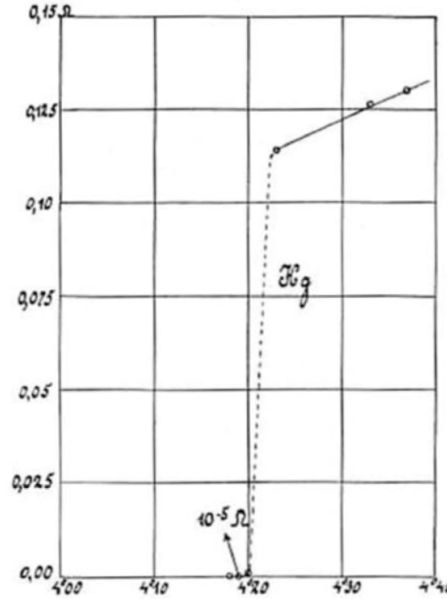


Figure 1-1: Historic plot of the resistance [Ω] (vertical axis) versus the temperature [K] (horizontal axis) for a Mercury sample. The superconducting transition is clearly visible at 4.2 K. Figure extracted from [1] and dated on the 26th of October 1911.

Superconductivity can be therefore defined as the phenomenon by which certain materials exhibit a vanishing electrical resistivity under specific conditions. From a practical standpoint, the superconducting state is preserved below critical values for the temperature (T_c [K]), magnetic field (B_c [T] for Type I superconductors and B_{c2} [T] for Type II, see next paragraphs.) and current density (J_c [A mm⁻²]). These parameters define the so-called critical surface², which governs the transition from superconducting to normal state [2]-[3]. Mechanical strain (ϵ) has also a direct impact on the superconducting properties, and constitutes a fourth parameter that can modify the previously mentioned surface. Nevertheless, the effect of mechanical loads on the superconducting performance varies remarkably depending on the material considered. Some superconductors show a large strain sensitivity, while others are

behind that extraordinary day.

²More precisely, superconductors do not revert to the normal state above J_c . In reality, in this regime, the so-called fluxoid movement (explained later in the text) translates into energy dissipation while the material remains in the superconducting state.

almost not affected by it.

The resistance-less flow of an electrical current through a superconductor is explained in terms of quantum mechanics, and attributed to the pairing of electrons into Cooper pairs due to electron-phonon interactions [4]. Following their most elemental classification, superconductors are grouped in two classes: Type I and Type II [3]. The distinction between both classes is made based on the intimate link that exists between the superconducting state and the magnetic field. It is therefore useful to define, first, the magnetic flux density in the superconductor as

$$B = \mu_0(H + M) \quad [\text{T}] \quad (1.1)$$

where M [A m^{-1}] is the magnetization field, μ_0 [T m A^{-1}] is the magnetic permeability and H [A m^{-1}] is the applied magnetic field strength.

Elemental superconductors (as lead, tin, mercury or others) are Type I superconductors. In their superconducting state, these materials show a perfect diamagnetic behavior, expelling completely any magnetic flux from their interior. Such a particular response is known as the Meissner-Ochsenfeld effect, in honor to the two scientist who discovered it in 1933 [5]. Type I superconductors remain in their superconducting state provided that the applied field is kept below a limit value named as B_c . At that critical value, usually lower than 0.1 T, it results more energetically favorable for the superconductor to be in the normal state and the material transits to the resistive state.

The second type of superconductors, Type II, shows a totally different behavior at high magnetic fields. Industrial superconducting alloys, intermetallic compounds or oxides belong to this group. Type II materials are characterized by two critical fields, B_{c1} and B_{c2} . Below B_{c1} , they behave in the same way as their Type I counterparts, expelling any magnetic flux from their interior (Meissner state). In the range between B_{c1} and B_{c2} the superconductor is said to be in the mixed state, and the magnetic field pierces the material in the form of flux tubes. Contrary to the first type, in this case it is energetically more favorable to have a sequence of normal and superconducting

zones, where the magnetic flux penetrates the superconductor in quantized fluxoids [3]. Ultimately, the superconductor transits to the normal state at a much larger applied magnetic field, B_{c2} , usually well beyond the value of 10 T.

In view of their properties, Type II superconductors are the only ones well-suited for applications that require their operation in presence of magnetic fields. There is, however, an important consideration that needs to be taken into account when a practical use of Type II superconductors is envisaged. In their mixed state, $B_{c1} < B < B_{c2}$, the presence of a transport current in the material will exert a force in the flux lines that will tend to cause their displacement. As a result, the movement of the flux lines will provoke a net energy dissipation within the superconductor and will suppress the advantages of the resistance-less flow of current. The cure to this effect, which is behind the so-called flux-flow resistance of superconductors [3], is found in the inclusion of pinning centres. These centres are typically point defects in the material, dislocations or interfaces like grain boundaries, and act as a retaining mechanism against fluxoid movement. The influence of pinning in the superconductor performance is quantified through the pinning force ($F_p = I_c \times B$) [6], and defines the critical current density available. Hence, it is important to emphasize here that while the critical temperature and magnetic field are two properties of the material and its composition, the critical current density is defined by the pinning processes. Type II superconductors exhibiting strong pinning are called hard superconductors, being the materials used in practical applications.

A second important classification for superconductors is conventionally defined as function of the critical temperature. Those superconductors who need to be cooled with LHe are called Low-Temperature Superconductors (LTS). On the other hand, the materials that are found in the superconducting state already at the liquid nitrogen temperature are classified into High-Temperature Superconductors³ (HTS). The theory developed in this work is only related to low-temperature superconductors. Their high-temperature counterparts present some particularities that require a spe-

³High temperature superconductivity was discovered in 1986 by Berdnorz and Muller, opening a new door towards a completely novel class of superconducting materials.

cial treatment, both for the physical theory behind their characteristics and for their use in superconducting systems.

1.2 Applications: practical superconductors

Superconducting materials are employed today both in small and large scale applications. Due to the lack of electrical resistivity, superconductors are perfectly suited for those uses that imply the transport of large electrical currents in small conductor volumes. In this regard, one domain of utilization specially emerges above the rest: the field of superconducting magnets. These are magnets capable of generating high magnetic fields by transporting large electrical current densities in their coil windings. Magnetic Resonance Imaging (MRI) scanners or Nuclear Magnetic Resonance (NMR) spectrometers, are perhaps two of the most popular examples with a direct impact in our society. Their core is constituted by a magnetic system built using superconducting materials, which provides the precise magnetic fields to be used for investigation and industrial purposes. In industry, superconductors are also used in power system equipment like transformers, rotating machines, current fault limiters or superconducting magnetic energy storage systems (SMES).

There exists a second major domain of application for superconductors, which has not been yet mentioned and that is indeed the focus of this thesis. We refer to the field of superconducting magnets designed for their operation in particle accelerators or newly-conceived fusion reactors. Suchlike machines rely on superconducting magnetic systems for the study of high energy physics (HEP) and for the control of thermonuclear fusion reactions respectively. It is the case of the Large Hadron Collider (LHC) at CERN [7] or the International Thermonuclear Reactor (ITER) [8]-[9], both in Europe. Of great importance, the scientific community behind these two large-scale instruments has been a key actor in the recent development of the superconducting technology.

Due to the inherent presence of high magnetic fields in the applications mentioned in last paragraph, Type II hard superconductors are the only superconducting

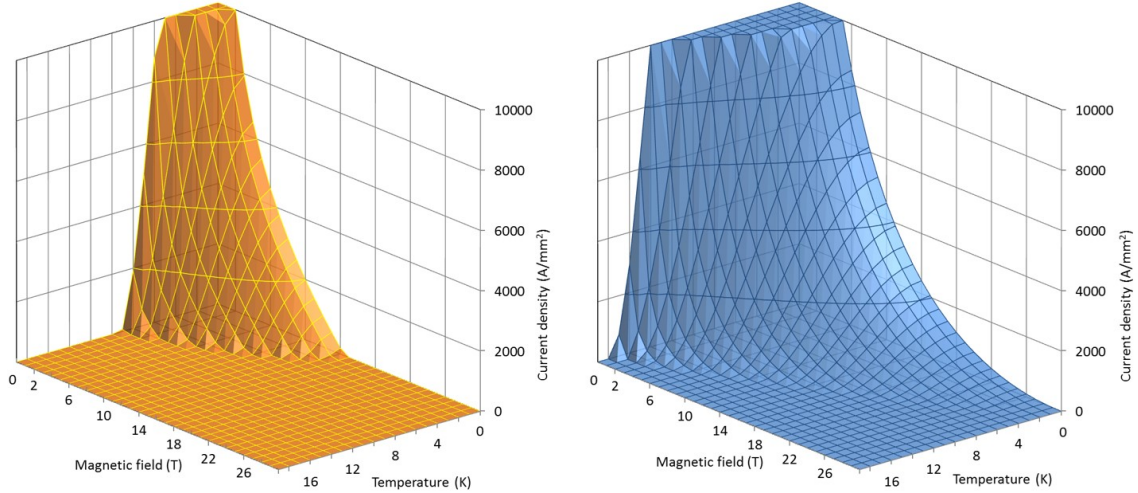


Figure 1-2: Illustration of the critical surface for Nb-Ti (left) and Nb₃Sn (right) [10]. The larger critical values of Nb₃Sn materials allow the preservation of the superconducting state in a larger parameter space compared to Nb-Ti.

materials that can be exploited for the construction of these systems. The main low-temperature superconductors belonging to the Type II class are Nb-Ti, Nb₃Sn and Nb₃Al. Table 1.1 summarizes the most important physical properties of the three materials. Figure 1-2 shows instead a comparison between two exemplary critical surfaces for Nb-Ti and Nb₃Sn [10]. Despite the larger critical values of Nb₃Al [11]-[12] for the temperature and magnetic field, the applications of superconductors based on this technology are nowadays reduced. This is partially explained by their lower critical current densities when compared to Nb₃Sn, and to the complicated production process. The use of Nb₃Al will not be discussed further in this work.

Table 1.1: Properties of main industrial LTS Type II superconductors

	Crystal structure	Critical temperature (T_c [K] at B=0 T)	Upper critical field (B_{c2} [T] at T=0 K)
Nb-Ti	BCC	9.8 K	~ 15 T
Nb ₃ Sn	A15	18 K	~ 30 T
Nb ₃ Al	A15	18.8 K	~ 33 T

Nb-Ti is considered the workhorse of superconducting materials, remaining undoubtedly as the reference superconductor when the operation conditions lie below its critical surface. It is a ductile intermetallic alloy, on which the applied supercon-

ductivity community accumulates a large experience. Resulting from many years of research and development activities, Nb-Ti is today a mature technology of proven successful performance. It has been extensively used for the construction of MRI and NMR magnets, fusion and accelerator magnets. Belonging to the last group, the long series of Nb-Ti magnets produced for the Large Hadron Collider at CERN [13] constitute a clear example of its successful performance.

On the other hand, Nb₃Sn is an A15 intermetallic compound considered in the present days as the preferred alternative for higher field applications. The larger critical values of Nb₃Sn allow the design of superconducting systems able to operate beyond the capabilities of Nb-Ti. However, one of the main particularities of Nb₃Sn is its remarkable strain sensitivity and its brittle behavior. Furthermore, the fabrication of Nb₃Sn-based superconducting systems features a long conductor thermal heat treatment at high temperature, which is needed to form the final superconducting phase as from its precursors. These two characteristics pose new important challenges for the design and construction of Nb₃Sn systems that were not present in the case of Nb-Ti. It is also important to note that even though superconductivity was observed before in Nb₃Sn than in Nb-Ti, the experience of the accelerator magnet community on its use is more recent. Today, research activities are still on-going with the objective of exploiting the full potential of the material. Nb₃Sn superconductors are currently being employed, for instance, in the winding of the new dipole and quadrupole magnets developed for the High Luminosity upgrade of the LHC (HL-LHC) [14], in the construction of the ITER Toroidal Field (TF) coils and Central Solenoid (CS), or in NMR magnets aimed at operating at fields of about 20 T.

We will focus on our research in the use of Nb₃Sn superconductors for high field applications. Both in this case, and in the case of Nb-Ti, it is necessary to distinguish between the superconducting material itself and a practical superconductor. For a superconductor to be employed in any of the applications described, it must be produced ensuring a series of crucial requirements. In what follows, the most important of those characteristics are briefly enumerated:

- **Geometry:** Perhaps trivial, the first important requirement for a useful super-

conductor is its availability in the form of wires or tapes that mimic the more conventional normal conductors. The production of practical superconductors following such geometries enables the addition of various features that are essential for their correct operation, in particular: 1) mechanical reinforcement, aimed at ensuring a safe response of the conductor to mechanical loads, including during the winding of complex coil shapes and 2) thermal stabilization, whose details will be presented in next paragraph.

- **Multifilamentary wires:**

Stability: As just mentioned, the second requirement is tightly linked with the first one and deals with the stability of superconductors [2]-[3], [15]. Despite the fact that the principle of a hard superconductor is to provide pinning centres that avoid fluxoid movements, thermal activation can still generate spontaneous local flux line motions resulting in energy dissipation. This phenomenon is known as "flux jump". Eventually, the flux line movements can enter into a cascade regime (leading to the loss of the superconducting state) if the superconductor does not provide a mechanism for stabilization. The solution to it is encountered, on first instance, in the sub-division of the superconductor area in very small filaments of the order of tenths of μm , which are more stable against flux jumps [2]. The second strategy to deal with the so-called conductor instabilities relies in the fundamental concept of thermal stabilization, which is instead provided by embedding the filaments in a high purity matrix of a normal-conducting material (the ensemble constituting a multifilamentary conductor). The presence of the stabilizing matrix seeks essentially to conduct away the heat generated by instabilities [2], [16]-[21], hence avoiding cascade effects and preventing new energy dissipation. Moreover, thermal stabilization results also essential in the general case of a transition of the superconductor to the normal state. The detailed explanation for the latter case will be treated in Section 1.5.

Magnetization: The third requisite is related with the presence of hysteresis

losses in the form of superconductor magnetization [25]. Also referred in many works as persistent currents, magnetization is a consequence of the strong pinning mechanism. The "Critical State Model" proposed by C.P. Bean provides a clear explanation on this effect [26]-[27]. It can be proven that the amount of energy dissipated in the hysteresis loop of the superconductor is proportional to the filament diameter. Thus, small filament diameters are used in practical wires to reduce magnetization losses. Interestingly, the already mentioned strategy of filament twisting is also beneficial here, due to the removal of the electrical coupling between filaments. For some applications, as the case of superconducting magnets, persistent currents are not only important from the point of view of the energy dissipated. They are as well a topic of extreme importance that determines the quality of the magnetic field generated when the conductor operates at a small fraction of its critical current density [2]-[3].

- **Minimization of transitory losses:** Finally, the last need deals with the minimization of the losses caused by transitory effects in the superconductor [2], [22]-[24]. When the magnetic field varies rapidly in a multifilamentary wire, coupling currents are generated in between the superconducting filaments. These currents, which are induced in an attempt to counteract the magnetic field change, circulate in the normal matrix and dissipate energy by Joule effect. It becomes therefore essential to minimize them. The main⁴ solution to limit interfilament losses relies on decreasing the loop of the coupling currents by filament twisting. The geometry of a multifilamentary wire is then defined by its characteristic filament twist-pitch parameter, which depends on each wire's architecture.

The practical superconductors that successfully accomplish these requirements allow the design of effective superconducting systems. Figure 1-3 shows the example of a Nb₃Sn multifilamentary wire used for the production of the new MQXF quadrupole magnet [28]-[29] designed for the HL-LHC upgrade.

⁴Other strategies, such as the inclusion of high resistivity barriers in the normal matrix, have been also tested in superconducting wires.

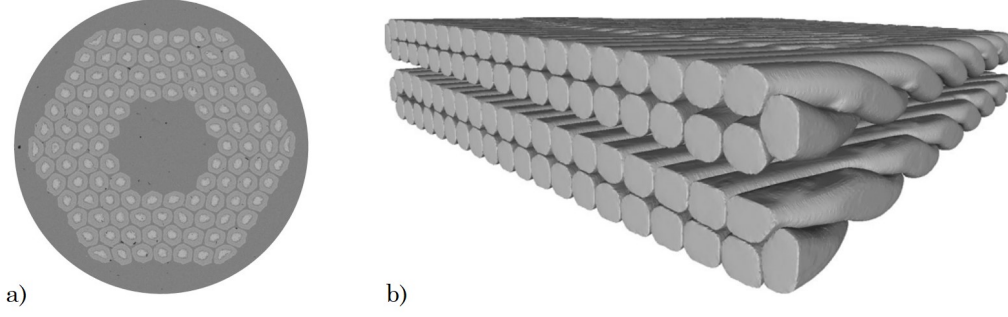


Figure 1-3: Nb_3Sn superconducting wire and Rutherford cable examples. a) Cross sectional view of the unreacted Nb_3Sn RRP superconducting wire used for the new MQXF quadrupole, produced following the 108/127 sub-element layout. b) Rendering of a Rutherford cable composed of different wires transposed together. Courtesy of F. Wolf [30].

1.3 Nb_3Sn : a strain sensitive superconductor

It has already been discussed that mechanical strain has a direct impact on the critical surface of superconducting materials, and that the sensitivity to strain varies depending on the material considered. A case of paramount relevance is the one of Nb_3Sn .

Niobium-Tin (Nb_3Sn) superconductors are primarily defined by large critical values for the temperature and upper magnetic field, well beyond the values attained in Nb-Ti. Nowadays these wires have demonstrated, with large margin, the capability of surpassing the target of $J_c(\text{non} - \text{Cu}) > 1000 \text{ A mm}^{-2}$ at 16 T and 4.2 K while maintaining the necessary properties for stable operation and system protection [31]–[36]. Such a remarkable performance has been obtained through exhaustive development programs, and through the optimization of the production techniques during the last decades. At present, three production methods are available for Nb_3Sn technology: the "Bronze Route", the "Powder In Tube" (PIT) technique, and the "Internal Tin" process. The Redstack Rod Process (RRP), belonging to the internal tin method, together with PIT offer currently the largest critical current values for high field and low temperature applications [37].

An equally important aspect of Nb_3Sn superconductors is, as already highlighted, its reduced mechanical strain tolerance and its noteworthy propensity to fracture. As

shown in numerous experiments [38]-[59], the critical surface of Nb₃Sn superconductors is strongly dependent on the applied strain state. In other words, for a fixed operation temperature and magnetic field, their critical current density highly depends on the mechanical strain seen by the superconducting phase. In particular, the strain state can cause a reduction in the available current density happening in two well-distinguished regimes. On one side, there is a reversible component that is fully recovered when the external load is removed. On the other side, there exists an irreversible and thus permanent component (degradation), which occurs when the loads exceed a critical threshold. In the case of the latter type of decrease, it can be ascribed to residual plastic strain coming from the high-purity stabilizing matrix, and/or to superconducting filament breakage.

We will develop further the study of the electro-mechanical properties of Nb₃Sn superconductors in Chapter 5. Nevertheless, and considering that in a superconducting (SC) system the conductor may experience a large spectrum of mechanical loads coming from different sources, it becomes already clear that the performance degradation due to mechanical strain turns into a new crucial parameter for Nb₃Sn technology (of much less importance in Nb-Ti). The reversible effect of mechanical strain in the critical surface should be minimized, whereas the permanent decrease of the superconductor performance must be always avoided.

As in the case of other superconducting materials, the mathematical description of the critical surface is a necessary tool for the design of superconducting systems based on Nb₃Sn. Several scaling laws are found in literature for this purpose, including the strain dependence [60]-[77]. The parametrization published by Bottura in [77] reads:

$$J_c(B, T, \varepsilon) = \frac{C}{B} s(\varepsilon) (1 - t^{1.52}) (1 - t^2) b^p (1 - b)^q \quad (1.2)$$

$$B_{c2}^*(T, \varepsilon) = B_{c2}^*(0, 0) s(\varepsilon) (1 - t^{1.52}) \quad (1.3)$$

$$T_c^*(B, \varepsilon) = T_{c0}^*(0, 0) [s(\varepsilon)]^{\frac{1}{3}} \left(1 - \frac{B}{B_{c2}^*(0, \varepsilon)} \right)^{\frac{1}{1.52}} \quad (1.4)$$

$$b = B/B_{c2}^*(T, \varepsilon) \quad (1.5)$$

$$t = T/T_c^*(0, \varepsilon) \quad (1.6)$$

where C is a scaling constant, $B_{c2}^*(0, 0)$ is the upper critical field at zero temperature and strain, $T_{c0}^*(0, 0)$ is the critical temperature at zero temperature and strain, p is the low field exponent of the pinning force ($p \approx 0.5$), q is the high field exponent of the pinning force ($q \approx 2$) and $s(\varepsilon)$ is the so-called strain function, treated in detail in [76]-[77]. We will use these expressions in subsequent chapters of the thesis.

1.4 Superconducting accelerator magnets

Superconducting magnets [2]-[3], [78]-[80] are used to produce magnetic fields beyond of those that can be reached with conventional electro-magnets [81]. The last ones employ normal-conducting materials, where the field shape is normally defined by the iron geometry and their peak field is usually limited by the iron saturation level (in the order of 2 T). When the production of larger magnetic fields by small magnet volumes is needed, it is generally more beneficial to use superconducting materials for the coil windings. The working principle of a superconducting magnet is the same than for a conventional electro-magnet, i.e. a flow of electric current generating a desired magnetic field. However, superconducting magnet designers need to face new challenges compared to the ones of normal systems. Namely, those associated to the particularities of superconducting materials. In addition, SC magnets usually operate above the iron saturation level, and thus, the shape of the magnetic field is not dominated by the iron geometry. Instead, it is defined by the coil geometry⁵.

In the present thesis, our work will be centered exclusively in superconducting magnets for particle accelerators [83]-[87]. The objective of these magnets is to produce a certain magnetic field aimed at interacting with a beam of charged particles. Since particle accelerators [88] require different magnetic field shapes depending on the desired effect on the particle beam, they rely on so-called multipole magnets. The latter can be designed using different coil architectures, where the most recur-

⁵Those superconducting magnets on which the field shape is still governed by the iron geometry are known as "Superferric" magnets [82].

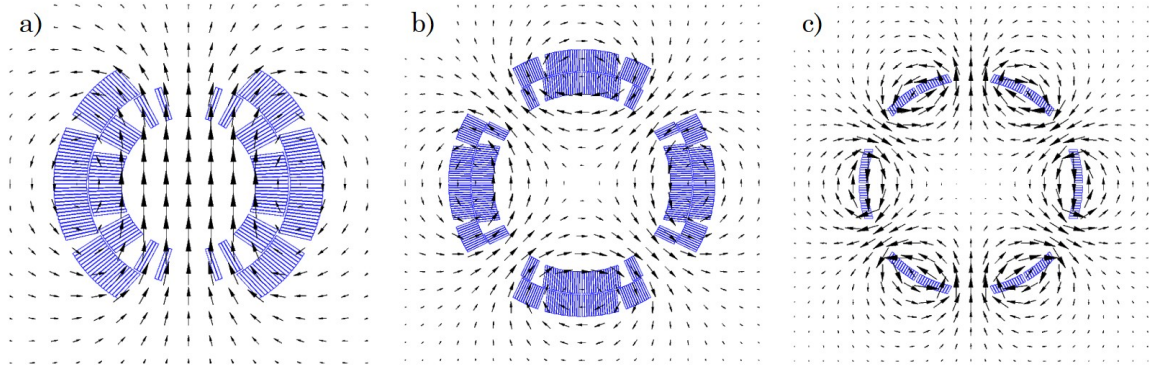


Figure 1-4: Two-dimensional magnetic field vectors and conductor cross section for three multipole accelerator magnet examples ($\cos \theta$ - type): a) Dipole, b) Quadrupole and c) Sextupole.

rent solution for the superconducting accelerator magnets installed so far (worldwide) consists in the so-called $\cos \theta$ coil geometry⁶.

Dipole magnets generate an unidirectional magnetic field in space, which is used to deflect the particles. Quadrupole magnets produce instead a magnetic field whose magnitude has a linear dependence with the position, in the plane orthogonal to the beam travelling direction (being the field null at the magnet center). They are used to focus or de-focus the particle beam, receiving the name of magnetic lenses. Higher order magnets (sextupoles, octupoles, etc.) are used to correct the magnetic field errors coming from the imperfections of the former magnets. These imperfections are linked to the impossibility of constructing ideal magnets and also to the inherent particularities of the superconductor itself, as for example the presence of field distortions due to persistent currents. A two dimensional representation of the magnetic field produced by three magnet examples is shown in Figure 1-4.

The necessity of producing large magnetic fields, and therefore of using superconducting magnets, is evident when considering the case of synchrotron accelerators. Synchrotrons keep a circular orbit for a bunched particle beam, where the beam is accelerated repetitively by radiofrequency cavities located in the straight parts of the

⁶As extensively treated in [2]-[3], [78]-[80], [82], [83]-[87], a $\cos \theta$ current distribution generates a pure dipole field in the space. In practice, such a current pattern is approximated by the accurate positioning of the conductor. The generation of a perfect quadrupole field is instead obtained with a $\cos 2\theta$ disposition. Higher order multipoles are also obtained with a proper conductor arrangement.

machine. Consequently, the beam energy is increased progressively on each passage through the cavities. In order to keep the closed orbit, the field level in the magnets must be ramped synchronously. It can be proven that in this case, the energy of the beam can be approximated by the following equation [89]:

$$E [TeV] \approx 0.3 \cdot R \cdot B_d \quad (1.7)$$

where R [Km] is the radius of the accelerator and B_d [T] is the magnetic field strength of the dipole magnets used for the particle deflection. It follows straightforwardly from the equation that the energy of the machine is directly proportional to the magnetic field produced by the magnets, and thus it becomes interesting to bring the value to its upper limit. High energy synchrotrons exploit in this way the advantages of superconductivity to explore the origins of the universe [90].

To achieve the largest magnetic field, accelerator magnets usually employ insulated cables in their coil windings rather than single wires. By doing so, higher currents can be transported in the superconducting cable while reducing the total number of turns and wire piece length. On parallel, the use of a cable composed of many wires decreases the magnet inductance⁷ and allows a certain current redistribution between them in the case of damage. There exist several types of superconducting cables that have been proposed along time. The most commonly used in accelerator magnets is the Rutherford cable (see Figure 1-3). It is composed of a certain number of single superconducting wires (also called strands when they are part of a cable) that are fully transposed together. This type of cable, which can be keystone⁸ to allow its easier winding in arc shapes, ensures that each strand exchange position with the rest along the cable length, and thereby creates a homogenous distribution of the current density for all the strands. It also reduces the loop of coupling currents between

⁷Reducing the magnet inductance decreases the inductive voltages in the system and allows a faster charge/discharge of the magnet. In addition, the magnet stored energy is directly proportional to this magnitude. The mentioned aspects play a crucial role in the magnet protection and operation, as it will be shown later in the Chapter.

⁸A keystone cable presents a trapezoidal geometry, provided by the mechanical compaction of the conductor during its production. During winding, the cable thin edge faces the inner radius of the coil and facilitates the creation of the desired arc shape.

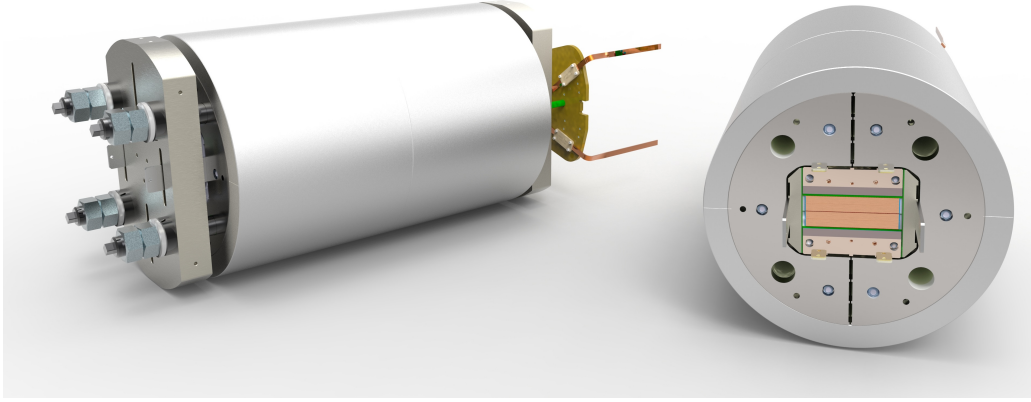


Figure 1-5: Mechanical support structure concept for a short high-field Nb₃Sn dipole magnet. The structure provides both radial and axial support to the superconducting coils, which are subjected to large electromagnetic forces during operation.

strands, and provides a very high packing factor allowing for the achievement of large current densities in the coils.

Moving forward in scale from the insulated cable level, the next essential entity in a superconducting magnet is the superconducting coil. The coil comprises all the cable turns and the necessary structural parts for fabrication and support. Eventually, they can be resin-impregnated to form a solid block of material. In fact, and in order to deal with the brittleness of the material and its strain sensitivity, the coil impregnation becomes essential for Nb₃Sn coils. The presence of the resin as an homogeneous medium between all coil parts, seeks for a smooth distribution of the mechanical loads inside the conductor, and guarantees a robust geometry for magnet assembly and coil manipulation.

Surrounding the superconducting coils of accelerator magnets, a mechanical structure supports the large mechanical loads seen during magnet operation. Though some particular coil geometries and configurations for other types of magnets may create a self-sustaining condition for the loads, in the case of superconducting accelerator magnets the presence of the mechanical structure is essential. Its purpose is twofold: first, it is conceived to act as a force-restraining structure that manages the large electromagnetic forces (resulting from the vectorial product of the current density circulating in the coils and the magnetic field on which they are immersed). Second,

the structure has also the essential role of minimizing the conductor displacement, ensuring the field shape for which the magnet has been designed. The minimization of the conductor displacement responds as well to the objective of avoiding the release of energy by frictional movements in the system. This type of heat deposition mechanism may increase locally the temperature, causing the loss of the superconducting state. Figure 1-5 shows the example of a mechanical concept for a high field magnet. The structure is ultimately enclosed in a He containment vessel, conforming the so-called magnet cold-mass. To conclude, the complete magnet cryostat adds the necessary insulation layers, support systems and auxiliary lines.

The last important aspect to review about accelerator magnets, concerns the presence of energy losses during their operation regime. Low-temperature superconducting systems operate in cryogenic baths of He, ranging from 1.9 K (superfluid He) to 4.2 K (liquid He). During the powering of the magnets, apart from the already discussed interfilament coupling losses or the superconductor hysteresis, other dynamic losses may be induced in the system as a result of fast electromagnetic transients. They can be particularly negative for the correct magnet performance due to the thermal properties of the magnet materials at low temperatures. Inside the coils, when cables are used, so called inter-strand coupling currents develop in between strands. These currents are ultimately characterized by the inter-strand contact resistance, and play an important role during magnet ramping and during the magnet discharge [24]. Similarly, outside the superconducting coils, hysteresis losses can be generated in the ferromagnetic parts of the magnet, and eddy currents may develop in any of the normal conducting materials present in the system. The whole of these energy dissipation mechanisms determine the so-called magnet AC losses, which need to be accounted for in the design of the cryogenic system.

1.5 Quench

The irredeemable and sudden transition of part of a superconductor from its superconducting state to the normal state receives the name of a quench [2]-[3],[91]-[92].

By definition, this irreversible transition happens when the superconductor crosses its critical surface due to a variation, or a combination of changes, on its temperature, magnetic field or current density. Within the three critical parameters, variations in temperature are usually the most common cause for a quench, due to the low specific heat of the magnet materials at cryogenic temperatures. In this regime, small energy inputs (in the order of $\mu\text{J mm}^{-3}$) translate into large temperature increments, thus triggering premature quenches.

Superconducting magnets are designed to ensure a safe margin on their operation parameters, trying to avoid the crossing of the critical surface in case of a sudden change on the local conditions. Still, a large variety of disturbances can lead to the quench of a magnet. For instance: mechanical events (local release of energy due to frictional movement or cracks), flux-jumps, radiation or beam deposited energy losses, dynamic losses from electromagnetic transients, heat leaks, loss of insulation, and many others. On this basis, the presence of quenches cannot be excluded when considering the operation of healthy superconducting magnets.

The condition of irreversibility that is implied in a quench event connote that after its transition to normal state, the conductor is not able to recover the superconducting state. In this regard, the thermal stabilizing matrix plays a crucial role. Superconducting materials have an electrical resistivity (when normal-conducting) which is order of magnitudes bigger than the one of normal conductors. Henceforth, the matrix provides a low resistance path for current transport, reducing the ohmic losses in case of a transition to the normal state and decreasing the final peak temperature. Additionally, the matrix acts as well as an essential heat removal mechanism, conducting away the generated heat in the superconductor. All these stabilizing features provide the necessary margin to avoid the damage of the system. In certain occasions, the presence of the matrix can have the extremely beneficial effect of allowing the recovery of the superconducting state quickly after a local transition, hence avoiding the quench. However, when the recovery is not possible, the quench will develop along the length of the superconducting coil. This phenomenon is known as quench propagation [2]-[3],[78], and results in the abnormal termination of the system

operation.

The appearance of the growing electrical resistance that propagates within the coil windings, translates straightforwardly into the dissipation of the stored magnetic energy through the normal-conducting zone. A fast thermal and electromagnetic transient is generated both at the coil and magnet level. To avoid any damage, once a quench is detected, the stored energy needs to be safely discharged without compromising the system integrity. Quench protection systems are normally used and designed to carry out this task while fulfilling given design margins.

It has been historically considered that the two main aspects that can endanger the magnet integrity during a quench are the temperature increase and the induced voltages:

- **Hot spot temperature:** The temperature increase in the superconducting coils corresponds to the conversion of the magnetic stored energy, or a part of it, into released heat in the normal zones. In this regard, the most important concern is the highest temperature reached at any point of the coil, called the "Hot spot temperature". Due to the combination of large stored energies and current densities, very high temperature values can be reached in superconducting magnets in very small time frames. Usually, a conventional design limit for the maximum hot spot (normally located in the position of the coil where the quench started) relies on limiting its value below room temperature, i.e. 300 K. This threshold seeks basically to avoid the damage of the most common conductor insulation systems. For recent high performance Nb₃Sn impregnated magnets, 350 K is thought to be a new acceptable maximum limit [93]. The idea behind the new value is to safely remain below the glass transition temperature of the epoxy resin used for the coil impregnation. In corrector magnets, where the margin is much less expensive and lower current densities can be used, the hot spot temperature is normally limited to 200 K - 250 K [87].
- **Maximum voltages:** Large voltages can be induced in the coils, and between the coils and the ground, due to the presence of the normal zones and due to

the unbalance between inductive and resistive loads. The voltage differences can cause arcing and short circuits if the insulation is not able to withstand the generated levels. In this case, the maximum values allowed depend on the insulation system used and on the conditions at which the threshold is considered. It is important to take into account that the properties of the coolant may play a big role in the system insulation. As an example, the test threshold for the voltage to ground during manufacturing of the new Nb₃Sn MQXF quadrupole magnets is set to 3680 V at 300 K and 1840 V at 1.9 K [94].

Whilst temperature and voltage are two essential concerns that are always taken into account in the design of superconducting magnets, the mechanical consequences of quench events become a concept of paramount importance in new high field magnets. For these, the use of strain sensitive conductors as Nb₃Sn, in combination with the increase in the magnetic stored energies, current densities and electromagnetic forces, may create a large risk of degrading the conductor or surpassing the magnet structural limits during a quench. The study of the transitory mechanical loads seen by the magnet in case of a quench becomes, therefore, an indispensable topic that needs to be addressed in detail.

1.6 Quench protection

A single superconducting magnet during a quench can be approximated by a pure RL circuit. The total magnet resistance is the result of the propagation of the normal zones within the superconducting coils, and the magnet differential inductance is a non-linear function due to the iron saturation (when present), magnetization and coupling current effects [95]. Under an adiabatic approximation, the coil hot spot temperature can be estimated using a local heat balance equation for the ohmic losses generated in the stabilizing matrix along the normal zone [87],[91],[96]:

$$A \int \bar{C}_p(T) dT = \rho_{stab}(T, B) \int f_{pitch} \frac{I^2(t)}{f_{stab} A} dt \quad [J] \quad (1.8)$$

where A [m^2] is the conductor area, l [m] is the normal conductor length, $\bar{C}_p$ [$\text{J K}^{-1} \text{m}^{-3}$] is the weighted volumetric specific heat of the composite conductor (that may take into account the transposed geometry), ρ_{stab} [Ωm^{-1}] is the electrical resistivity of the stabilizing matrix, f_{pitch} (≥ 1) is a factor that allows the scaling of the stabilizer's length for the calculation of the resistance (to account for the transposition pitch in multi-strand transposed cables), I [A] is the magnet current and f_{stab} is the fraction of stabilizer in the conductor cross section.

Equation 1.8 can be integrated over time from the start of the quench, to obtain the final hot spot temperature. Rearranging the terms, the equation reads:

$$f_{stab} A^2 f_{pitch}^{-1} \int_{T_0}^{T_{max}} \frac{\bar{C}_p(T)}{\rho_{stab}(T, B)} dT = \int_0^\infty I^2(t) dt \quad [\text{A}^2 \text{s}] \quad (1.9)$$

with T_0 [K] being the temperature in the instant prior to the quench and T_{max} [K] the maximum temperature attained at the end of the discharge.

The left-hand side of Equation 1.9, corresponding to the combination of the cable enthalpy and electrical resistivity, is conventionally defined as a function Γ [$\text{A}^2 \text{s}$]. This function is exclusively dependent on the conductor material properties and geometry:

$$\Gamma(T_{max}) = \int_0^\infty I^2(t) dt \quad [\text{A}^2 \text{s}] \quad (1.10)$$

On the other side, the integral of the square of the current is known as the quench integral and constitutes an observable during the magnet test. In this case, it is completely defined by the operational current and by the response of the full electrical circuit. Due to the high electrical currents transported by superconducting magnets, the units of the integral are often in the order of 10^6 [$\text{A}^2 \text{s}$], receiving the name of MIITs (M=Mega - I=Ampere - I=Ampere - T=Second). Equation 1.10 in combination with the characteristic curve of Γ vs T for the conductor gives an adiabatic estimation of the hot spot reached in the superconducting magnet.

The right-hand side of Equation 1.10 can be also further developed. The current decay in a single magnet is determined by the response of its RL circuit:

$$I(t) = I_0 \exp \left[-t \left(\frac{R_c + R_w}{L_d} \right) \right] \quad [\text{A}] \quad (1.11)$$

on which R_c [Ω] is the resistance of the normal zone in the superconducting coils, R_w [Ω] is the resistance of the warm part of the circuit and L_d [H] is the magnet differential inductance.

It follows from the combination of equations 1.10 and 1.11 that the magnet can be protected against overheating by two possible strategies: a) by considering in the magnet design a cable geometry and constituent materials that are able to withstand the expected quench loads, or b) to induce the fastest possible and most homogeneous current discharge in the magnet. For the latter case, either a decrease of the magnet inductance or an increase in the total resistance of the circuit (or straightforwardly, a combination of both), results into the desired effect.

The magnet designs on which the operational parameters and the cable characteristics ensure a safe quench discharge are known as "Self-protected" magnets. On the contrary, when this condition is not guaranteed, quench protection systems are needed. These systems exploit the possibility of acting in the magnet discharge during a quench, ensuring that the maximum allowable values are respected. Protection systems can be active or passive, and they can intervene in the quench process by extracting part of the stored energy to a external device, or by favoring a faster energy dump within the superconducting coils. A non-extensive list of the most employed protection methods in superconducting magnets is shown next:

- **Energy extraction (EE) systems:** The protection method by which part of the stored energy is actively discharged outside the magnet is referred as energy extraction. External resistors are the most relevant example. They are placed in series with the superconducting magnet and provide the capability of dumping a part of the energy on it, therefore decreasing the quench load in the superconducting magnet. The effect is clearly visible when considering the addition of a certain supplementary resistance in the R_w parameter of Equation 1.11. However, dump resistors have the drawback of creating an additional

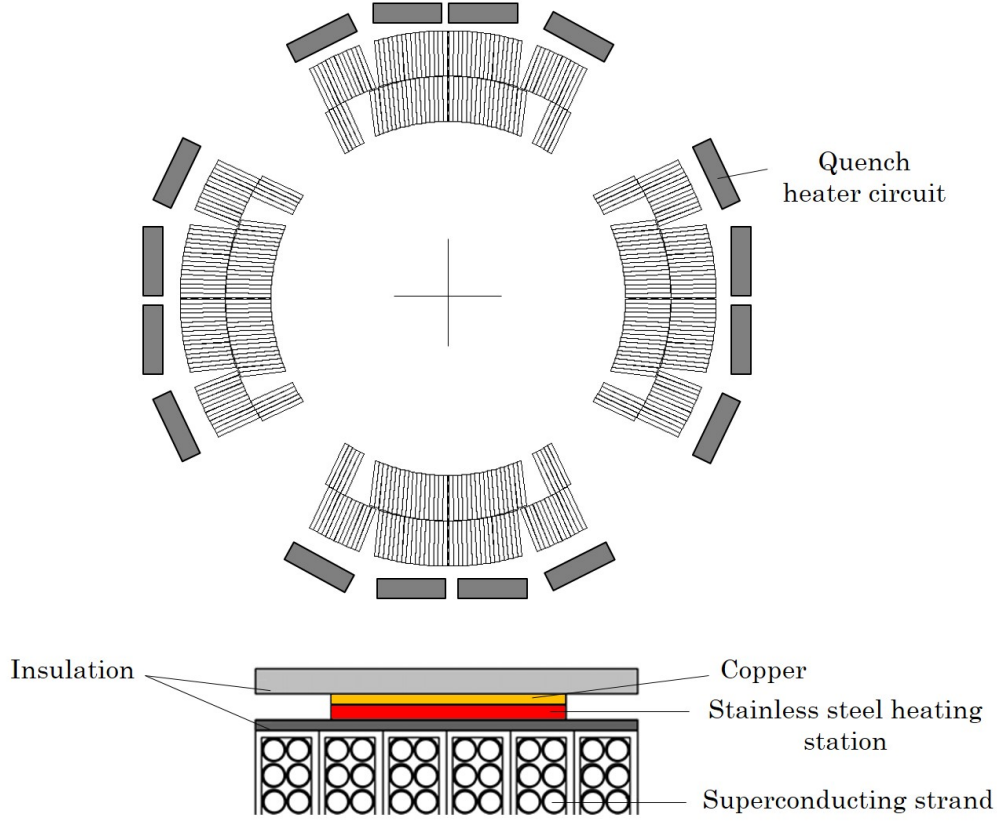


Figure 1-6: Implementation of external quench heater circuits in the new MQXF Nb_3Sn quadrupole magnet. The top plot shows the positioning of the different circuits in the high and low field region of each coil. The bottom plot depicts a schematic cross section assuming a circuit based on stainless steel stations [98].

voltage at the magnet leads that is proportional to the resistance value of the device. The size of the resistor is then limited by the maximum voltage threshold defined by the magnet insulation. For large energy stored magnets, the sole protection of the system by energy extraction will be likely not possible, and other alternatives need to be considered [87],[96].

- **Active heating systems:** Contrary to energy extraction systems, the protection by active heating methods relies on provoking a faster discharge of the energy by acting on the resistance developed inside the superconducting coils. The goal is to bring rapidly the largest possible portion of the conductor to the resistive state, helping to dissipate the energy in a faster and more homogeneous

way. Fundamentally, this task is accomplished by increasing the temperature of the conductor all along the superconducting coil. Active heating systems are widely used in superconducting magnets, where two different methods constitute the current state of the art in this category:

- Quench heaters (QH): They are an external electrical circuit consisting in a series of heating stations placed close to the cable turns. When a quench is detected and validated, an electric current is introduced in the heater circuit releasing heat by Joule effect. The efficacy of quench heaters is hence determined by the heat conduction across the cable insulation, and depends on the available margin to quench of each conductor. The heater design must guarantee two conflicting conditions: a) a good thermal conductivity between the heating stations and the cables, in order to improve the performance of the protection and b) a good electrical insulation between the heater circuit and the coil, to cope with the large voltage differences generated during the transient. To ensure a safe discharge in new Nb₃Sn high field magnets, heater systems must be able to quench completely the superconducting coils in a time margin ranging generally from 10 ms to 50 ms [87]. This value is substantially lower than the margin available in Nb-Ti magnets, where the range is usually in the order of 50 ms to 200 ms [97]. The difference highlights the important challenges associated to the protection of new Nb₃Sn high field magnets, and the necessity of alternative protection systems. Figure 1-6 shows the usual positioning of the quench heater circuits in the external surfaces of the coils and a schematic view of its integration above the conductor [98].
- Coupling Loss Induced Quench (CLIQ) system: CLIQ technology relies on the generation of high coupling losses within the superconductor to quench the largest possible length of cable in the magnet [97]. The main exploited mechanism behind the heat deposition is the generation of interfilament coupling currents. These currents, induced by the injection in the system of

current impulses via the discharge of a capacitor bank, release the required energy to initiate the transition of the superconductor to the normal state. Acting directly in the interior of the superconductor, the effectiveness of CLIQ is by definition larger than the one of the quench heaters. Some calculations based on specific CLIQ designs for high field magnets suggest that the system is able to induce the transition to the normal state of an entire full-scale winding pack in a time range of 10 ms to 40 ms [97].

- **Coupled systems:** Based on the electromagnetic coupling between the main magnet and a secondary circuit, these systems are able to extract part of the magnetic stored energy outside the magnet [2]. They have been used in detector magnets for HEP applications, as for example the case of the Compact Muon Solenoid (CMS) at the LHC. Coupled protection systems are passive systems, which do not need to be activated by any external triggering device.
- **By-pass devices:** The last described protection system consists in placing by-pass elements across the superconducting entity that needs to be protected [3]. The growing electrical resistance that develops during a quench results into the transfer of part of the current to the by-pass path. The advantages of the current transfer are two: first, a fraction of the energy is dissipated in the by-pass element and second, the voltage is also limited across the branch. Resistors and diodes belong to this category. As in the case of the coupled systems, this kind of protection system responds to a passive protection strategy.

1.7 Scope and structure of the work

The constant search for higher magnetic fields in superconducting accelerator magnets has contributed over the last decades to several important technological breakthroughs. One of the keys for this evolution has been the development of practical superconductors able to operate beyond the limits of the well-known Nb-Ti technology. As described, due to their larger upper critical field and critical temperature,

Nb₃Sn conductors have become in the present days the preferred solution for the winding of high field superconducting coils. Unlocking the previous magnetic field frontiers, Nb₃Sn allows the design of compact accelerator magnets for their operation above 10 T [99].

It has been presented as well that one of the most important characteristics of Nb₃Sn technology is, on the contrary, the reduced tolerance to mechanical loads of the superconducting phase. If the loads are not kept under precise limits during the full life-cycle of the magnet, the conductor performance can be reduced both in a reversible and a irreversible manner.

Since large electromagnetic forces are expected during the operation of new generation Nb₃Sn accelerator magnets, due to their high field nature, new challenges in magnet design have to be faced to maintain the coil stresses below reasonable limits. Current high field designs entail peak stresses on the coils that are in the level of 150 MPa to 200 MPa during magnet assembly and operation [100]-[101], approaching and in some cases surpassing the conductor limits. On the other hand, their large stored energies and current densities push in parallel the boundaries of quench protection towards new frontiers [102], where high coil temperatures and large internal thermal gradients can be attained during a quench discharge.

From the combination of all these aspects, a new concern arises in Nb₃Sn high field magnets: the potential appearance of large mechanical loads during quench that can put in serious risk the integrity of the strain sensitive superconductor first, and of the magnet structure in general.

The scope of this thesis is hence to present, for the first time, an exhaustive three-dimensional analysis of the mechanics of Nb₃Sn high field superconducting magnets during quench processes. Furthermore, to characterize the conductor capability to withstand the computed expected loads, and to establish clear limits regarding conductor degradation during a quench, a complete experimental campaign for the electro-mechanical characterization of Nb₃Sn wires is presented in parallel. The work focuses on the case study of the new MQXF quadrupole magnet built for the High-Luminosity upgrade of the LHC (HL-LHC) at CERN. These magnets will be part of

the first Nb₃Sn units ever operating in a particle accelerator, and will pave the way towards potential future hadron colliders searching for larger collision energies.

The analysis is performed via Finite Element Modelling (FEM) techniques, widely used in the design of superconducting magnets. Firstly, a 3D numerical model reproducing the quench propagation in a superconducting magnet has been developed using ANSYS APDL [103]. The results from this model, which has been validated with the help of a large amount of experimental signals recorded during real tests, are lately used as an input in the magnet mechanical models (also built in ANSYS APDL). This strategy enables the study of the magnet quench mechanics based on a quasi-static approximation. Finally, the predicted quench mechanical loads are compared to the critical limits found in the conductor experiments.

The results of the thesis are expected to provide key elements to assess the engineering margins for the design and operation of new accelerator magnets based on Nb₃Sn, and therefore, to adopt appropriate protective actions in case of a quench.

1.8 Contents of the thesis

The research work is divided in five main chapters, whose content is briefly described in the following paragraphs:

Chapter 2: An introduction to the Large Hadron Collider and its High Luminosity upgrade puts in context the detailed description of the Nb₃Sn MQXF quadrupole magnet. The main magnet features and operational parameters are discussed here, and will serve as a reference for the rest of the study.

Chapter 3: The third chapter shows the development of a three-dimensional Finite Element (FE) code able to reproduce the physics of quench propagation. The modelling assumptions, material properties and the description of specially developed solving algorithms are treated in detail. To conclude, the numerical model is validated against experimental results at different size scales.

Chapter 4: The two-dimensional and three-dimensional mechanical models of MQXF magnet are presented in this chapter. The complete study of the magnet

mechanical behavior during a quench, and its validation, is shown both in two and three dimensions.

Chapter 5: In this last chapter, we describe the complete electro-mechanical characterization campaign performed for Nb₃Sn superconducting wires. First, an introduction to the experimental setup is provided. Second, the results of the tests are shown and discussed in detail. To conclude, the link between the expected mechanical loads during a quench and the results from this experimental campaign is tackled.

Chapter 2

MQXF: The *low* – β quadrupole magnet for the High Luminosity upgrade of the LHC

The analysis of the magnet mechanics during a quench that will be developed in next chapters will elaborate on the reference case of MQXF, a so-called low – β Nb₃Sn accelerator quadrupole used for beam focusing/defocusing. In this chapter we present a detailed description of the magnet, which is currently under production for installation in the High Luminosity upgrade of the LHC. Its large stored energy and demanding protection turns the case of MQXF into one of the most interesting among the new Nb₃Sn accelerator magnet designs. To put the main operational parameters in context, a brief introduction to the Large Hadron Collider at CERN and its High Luminosity upgrade is provided.

2.1 Particle accelerators: LHC, the Large Hadron Collider at CERN

In the past century, the discovery and understanding of radiation guided the scientific community towards the novel idea of a beam composed of elementary particles. Since

then, the great advancements achieved in what regards the collection, focusing and acceleration of such beams have given rise to a relatively large variety of facilities that produce particle beams for various purposes [88].

Particle accelerators are machines that employ macroscopic electromagnetic fields for the acceleration and driving of charged particle beams to large kinetic energies. They are used for instance in: a) research on particle physics and nuclear physics, b) as sources for synchrotron radiation, c) medical applications, d) for plasma heating in fusion reactors, e) oil and natural gas exploration, f) food sterilization, and others.

The LHC, Large Hadron Collider [7], is the largest and most powerful particle accelerator operating in the present days worldwide. Conceived for the research on high energy particle physics, two particle beams are accelerated in the machine and then brought into head-on collision at specific interaction points. In these points, complex particle detectors are located to study in detail the results of the interaction between the high energy particles. The data are provided to scientists from all over the world, who try to give an answer on the most fundamental questions about the constituents of matter and the origin of the universe.

In particular, the LHC is a circular accelerator of the synchrotron type. The two particle beams travel in opposite directions at near the speed of light, being guided through the accelerator by the large magnetic fields produced by superconducting magnets. As explained in the introduction chapter, the beam energy and the magnetic field in the main magnets increases synchronously with repetitive revolutions along the circular path (see Section 1.4, the concept of a synchrotron accelerator is briefly described to motivate the need of superconducting magnets). With a circumference of 27 km, the ring of the LHC extends along the Swiss-French border that surrounds the city of Geneva, and belongs to the facilities of the European Organization for Nuclear Research (CERN).

The ubiquitous schematic representation of the LHC is presented in Figure 2-1. The machine layout accounts for eight arc sections, divided in a total of 23 smaller cells, where the so-called lattice magnets impart the curved beam trajectory (dipoles), focus the beam keeping a stable and aligned motion (quadrupoles) and correct the

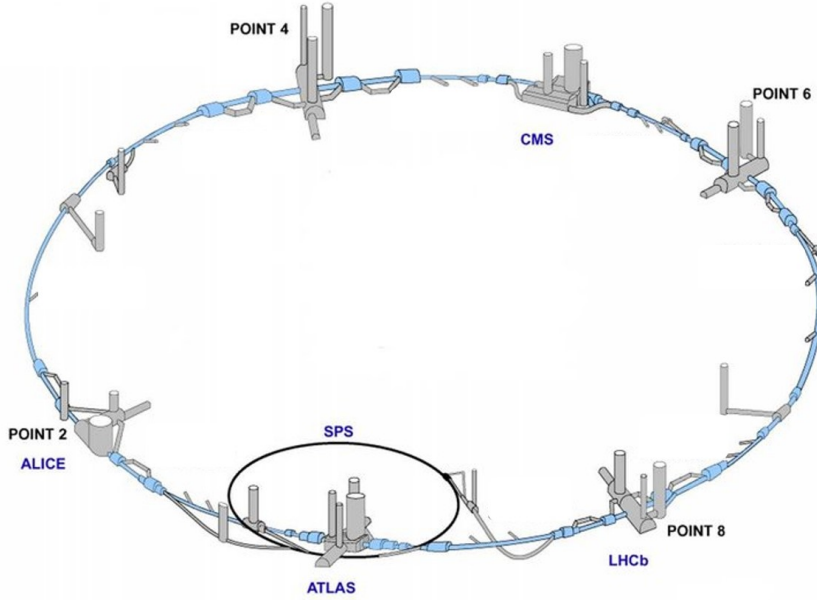


Figure 2-1: Schematic view of the LHC tunnel. The four main experiments are indicated in magenta. Figure modified from [104].

inherent field imperfections (higher order multipole magnets). Completing the remaining accelerator length, other eight straight sections called Insertion Regions (IR) accommodate: a) the four main experiments, and their closest regions, where the counter-rotating beams collide, b) the collimation and machine protection systems, c) the radiofrequency systems, and d) a beam extraction section based on fast pulsed kicker magnets. Correspondingly, the magnets placed in the IR are called insertion magnets.

Each of the four main experiments, ATLAS, CMS, ALICE and LHCb, has been designed using a different detector architecture, and thus, they are specialized in the study of different physics phenomena. Before and after the four interaction points emplacing these experiments, a so-called inner triplet region provides the final focusing to the beam prior to entering the detector. The strong focusing, in addition to be needed in the lattice for the stable motion of the beam, is used in the triplet regions to reduce the beam size and to increase the probability of achieving the mentioned head-on particle collisions at the detector center. In the LHC, the beam is squeezed

by the inner triplet into a size 12.5 times narrower, passing from 0.2 mm down to 16.7 μm across.

Following our development on the probability of particle collisions, the rate of events per second in the LHC synchrotron is given by

$$N = L\sigma \quad (2.1)$$

where L [$\text{s}^{-1} \text{cm}^{-2}$] is the essential concept of machine luminosity (equation in the Appendix) and σ [cm^{-2}] is the interaction cross section. Without entering into the details of accelerator physics, it is worth to mention that the luminosity of a collider depends exclusively on the accelerator features and beam parameters, and by implication, it is governed by the machine layout and main systems. As suggested by the text, the effect of the strong quadrupole magnets located in the inner triplets is accounted for in the machine luminosity concept.

A total of 1232 dipole and 392 quadrupole magnets are present in the LHC. At their nominal operation parameters, the main Nb-Ti bending dipoles are designed to produce a magnetic field of 8.33 T, where the nominal centre-of-mass beam energy becomes 7 TeV¹. The total collision energy equals, consequently, 14 TeV and the design luminosity value is $1 \times 10^{34} \text{cm}^{-2} \text{s}^{-1}$. We will review in the next section some details about the machine operation up to the present days.

2.2 HL-LHC: The high luminosity upgrade

Since its commissioning for proton-proton collisions in 2010, the LHC has successfully demonstrated its potential as a machine for discovery, with historic results achieved in the field of high energy physics. We highlight, as it could not be done otherwise, the discovery of the elusive Higgs boson in 2012, which became a major milestone in the history of science.

¹One could use Equation 1.7 to get an estimation of the energy, purely based on the magnetic field. Considering that the magnetic length of the dipole magnets is 14.3 m, the curvature radius is then $R = (1232 \times 14.3 \times 10^{-3} \text{ km} / 2\pi)$, and the energy, $E \approx 7 \text{ TeV}$.

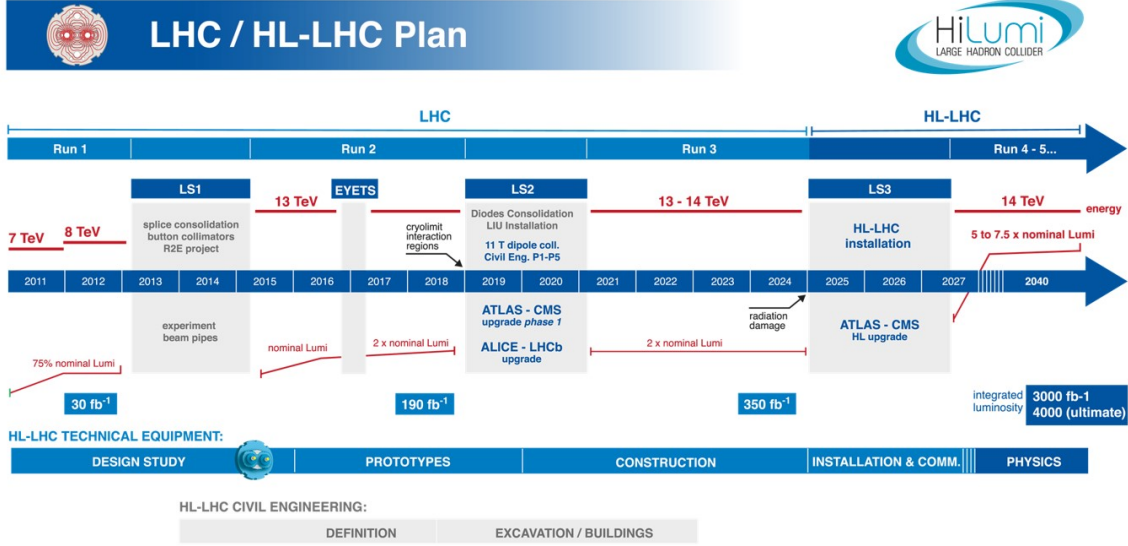


Figure 2-2: LHC and HL-LHC baseline plan for the next decade and beyond. Luminosity and centre-of-mass proton collision energy values are shown in red. Updated in 2020 [106].

In terms of operation, the LHC delivered during its first run (Run 1), 8 TeV centre-of-mass proton collisions out of the nominal design value of 14 TeV, reaching the end of the run in 2013. Following a long technical stop, the machine operation was then re-started in 2015 for Run 2, delivering in this case 13 TeV centre-of-mass collisions up to the present date. Furthermore, the design luminosity value of $1 \times 10^{34} \text{ cm}^{-2} \text{ s}^{-1}$ was achieved in 2016. Since then, larger peak values have been routinely obtained [14] showing a remarkable performance of the machine.

The positive results from the first two runs have fostered the hope for new discoveries, and have consolidated the leading position of Europe in high-energy physics. To increase its potential, while maintaining scientific progress² and while exploiting the full machine capacity, the LHC will undergo a major upgrade starting in 2020. The novel machine configuration, called the High Luminosity Large Hadron Collider, seeks to increase the luminosity by a factor five beyond the LHC nominal parameters, and its integrated value by a factor of ten. A decrease of the beam size in the interaction points, by a factor close to two with respect to the current values, is also

²After Run 3, the statistical gain in running the accelerator without a significant luminosity increase beyond its design and ultimate values would become marginal [14].

targeted. The upgrade accounts as well for the essential task of replacing the equipment that after years of operation in the LHC are becoming vulnerable to breakdown and accelerated aging³, or that can be a potential bottleneck for operation in a higher radiation environment [105].

All together, the development and installation of the new equipment for HL-LHC relies in cutting-edge technologies that span over very different domains. Upgrades on superconducting magnets, machine protection, diagnostics, cryogenics, collimation and vacuum will be implemented with the objective of starting the operation of HL-LHC in 2026. The project timeline including the luminosity and energy values is shown in Figure 2-2 [106].

2.3 The MQXF *low* – β quadrupole

An important part of the HL-LHC upgrade is focused in the machine insertion regions. There, the update of the inner triplet plays a key role in the desired luminosity increase and in the reduction of the beam size at the interaction point.

Among other components to be upgraded, the installation of new inner triplet (or *low* – β) quadrupole magnets allowing for a stronger beam focusing is envisaged. The new magnets, called MQXF [107]-[115], will replace the today's present Nb-Ti quadrupoles Q1, Q2a, Q2b and Q3, whose names denote their position in the machine layout. These original units were designed to produce a gradient of 215 T m^{-1} in a 70 mm aperture, featuring a magnetic length of 6.3 m for Q1 and Q3 [116], and 5.5 m for Q2a and Q2b [117].

For the new design, it can be proven (based on the machine optics) that the target of decreasing the beam size by a factor two in the interaction point, requires doubling the aperture of the triplet quadrupole magnets⁴ [88]. Hence, in order to limit the increase in magnetic length of the units below acceptable values, the new MQXF design accounts for the use of Nb₃Sn superconductors in the coil windings.

³The radiation damage in the presently installed Nb-Ti quadrupole magnets is one of the best examples regarding the causes for vulnerabilities.

⁴Under constant beam parameters, see Appendix A.

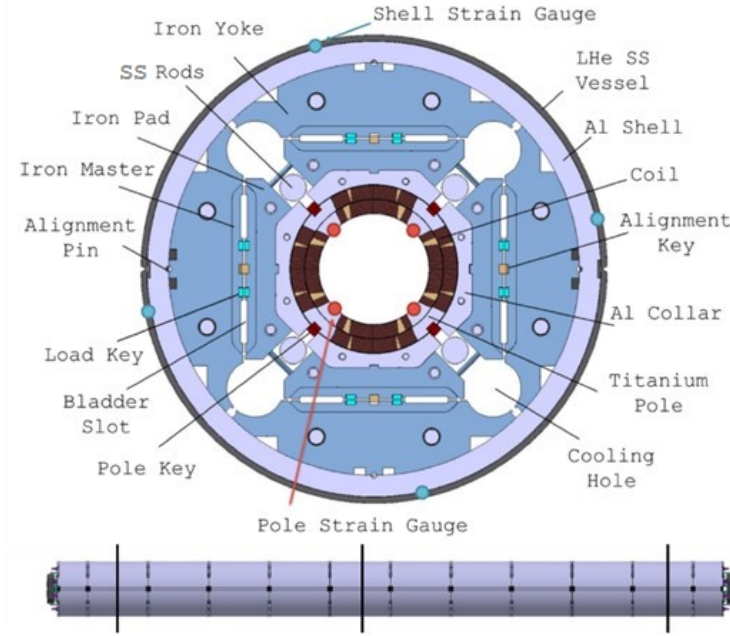


Figure 2-3: MQXF magnet cross section and side view. The position of the mechanical instrumentation is indicated by the round markers. Vertical lines define the longitudinal position of the measuring stations for the long magnets.

In particular, such a technological choice profits from the possibility of operating the new magnets with a large conductor peak field level, otherwise not reachable with Nb-Ti.

The MQXF *low* $-\beta$ quadrupoles for HL-LHC will therefore produce a gradient of 132.6 T m^{-1} in an enhanced 150 mm single aperture, and will operate at a conductor peak field of 11.4 T in nominal conditions. Two magnets, called MQXFA with a magnetic length of 4.2 m will be inserted in a common cold-mass and cryostat for Q1/Q3, whilst a longer version, called MQXFB, with a magnetic length 7.15 m will be used in a single configuration for Q2a/Q2b. All the units share the same design in terms of the magnet cross section, just differing on their length. Moreover, the magnet development and production is a result of the joint effort between CERN and the US Accelerator Research Program (AUP, previously LARP) [118].

As a crucial part of the project, the CERN-AUP-LARP collaboration envisioned a research phase after the initial design activities on which so-called short model magnets have been produced to validate the system's performance and fabrication

Table 2.1: Strand specifications.

Parameter	Unit	RRP	PIT
Strand diameter	mm	0.85	
Sub-element diameter	μm	≤ 55	
Filament twist pitch	mm	19 ± 3	
Cu/SC		1.2 ± 0.1	
RRR		>150	
I_c (12 T, 4.2 K), no self-field corr.	A	>632	>590
I_c (15 T, 4.2 K), no self-field corr.	A	>331	>331
Non-Cu J_c (12 T, 4.2 K), no self-field corr.	A mm^{-2}	>2450	>2290
Non-Cu J_c (15 T, 4.2 K), no self-field corr.	A mm^{-2}	>1280	>1280

procedures. Model magnets follow the final design and cross section, but their total length is reduced. This is done to decrease the cost of the research activities, their complexity and the time required to obtain a feed-back on the design based on test results. In the case of MQXF, the short models feature a magnetic length of 1.2 m and are called MQXFS magnets. It is important to note that indeed, as a result of the short model experience, the MQXF design underwent an update from the so-called "first generation" design to the "second generation" one [114]. In what follows we will just focus on the final parameters after the aforementioned update.

At the moment of writing this thesis, the short model phase of the project has been almost completed and the production of the full length prototypes is already on-going. We describe next the main magnet and conductor features, together with a brief literature research on the test results available up to the date.

2.3.1 Superconducting strand and cable

The coils of the MQXF quadrupole are wound using a Rutherford-type cable composed by 40 strands of 0.85 mm diameter. A unique Nb_3Sn strand architecture has been selected for the production of all the series magnets, following a development phase where different options were tested [29]. The chosen strand presents an hexagonal stack of 108 superconducting sub-elements inside the copper matrix, and is produced by the Redstack-Rod-Process method (RRP 108/127). See Figure 1-3.

Table 2.2: Cable specifications.

Parameter	Unit	
Number of strands in cable		40
Cable bare width (before/after HT)	mm	18.150 / 18.363
Cable bare mid-thick. (before/after HT)	mm	1.525 / 1.594
Cable bare inner-thick. (before/after HT)	mm	1.462 / 1.530
Cable bare outer-thick. (before/after HT)	mm	1.588 / 1.658
Cable width expansion during HT	%	1.2
Cable mid-thick. expansion during HT	%	4.5
Keystone angle	°	0.4
Pitch length	mm	109
Cable core width	mm	12
Cable core thickness	μm	25
Cabling I_c degradation	%	<5
RRR after cabling		>100
Insulation thickness per side at 5 MPa	μm	145±5

The nominal copper to non-copper ratio (Cu/non-Cu) is defined as 1.2, while the minimum residual resistivity ratio of the stabilizing matrix (RRR^5) is specified to be 150. The complete strand specifications are provided in Table 2.1, which sets as well the essential parameter of the minimum critical current density required by the project. The reference J_c parametrization, assuming a 5 % degradation component due to cabling deformations and accounting for the strand self-field correction, is obtained via Equation 1.2. Its field dependence is shown in Figure 2-4.

In what regards the Rutherford cable composed of 40 of these strands, it is produced in a trapezoidal shape with a 0.4° keystone angle. The reference cable width before the thermal heat treatment is 18.15 mm and the mid-thickness, 1.525 mm. To account for the dimensional changes of the cable during the thermal reaction cycle, two different thermal expansion ratios have been considered in the design of the superconducting coils and the coil tooling: 4.5 % cable expansion in thickness and 1.2 % in width [119]. In addition, the cable contains a 25 μm thick 316L stainless steel core at its center, aimed at reducing dynamic effects during fast transients. At last, the turn to turn insulation is provided by (145 ± 5) μm thick braided S2-glass, being this

⁵ $RRR = \frac{\rho(T_w, B=0)}{\rho(T_c, B=0)}$, where $T_c \approx 4$ K and $T_w = 273$ K to 295 K depending on the model considered.

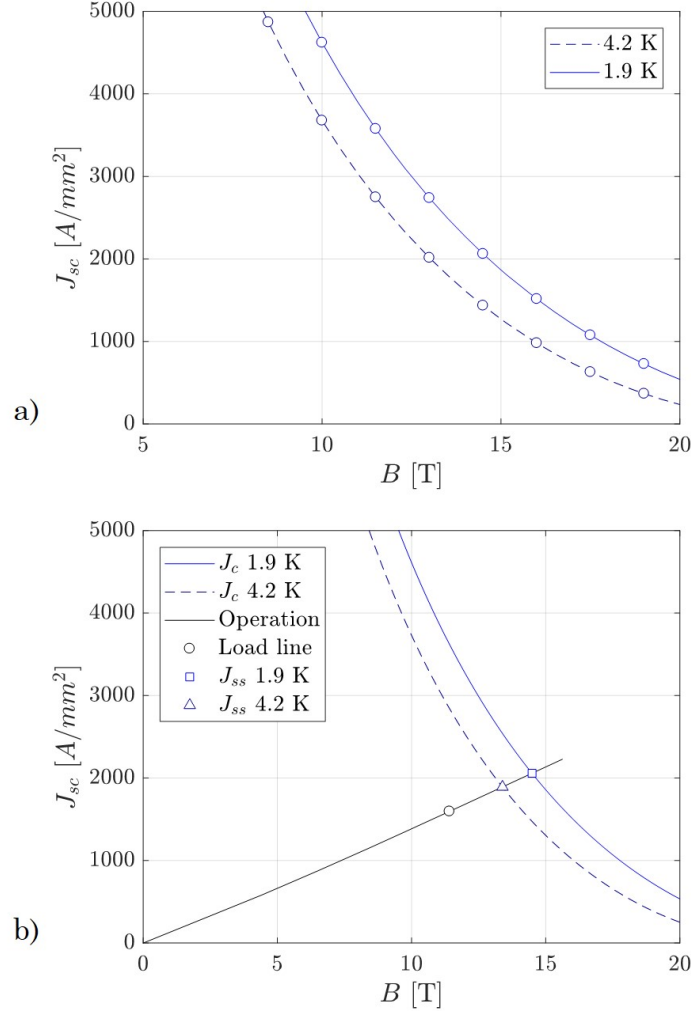


Figure 2-4: a) Non-Cu critical current density as function of the total magnetic field for the reference strand specifications. It includes the self-field correction and assumes a 5 % critical current density degradation due to cabling. b) MQXF non-linear magnet load line. The operation regime is represented by the empty dot. The short sample limits are instead highlighted with a triangular and squared marker.

value specified at a transverse pressure of 5 MPa. The full cable characteristics are summarized in Table 2.2.

2.3.2 Magnet design

Magnetic concept

The MQXF magnetic design is based in the well-known $\cos 2\theta$ coil geometry [2]-[3], and has its origin in the previous LARP experience regarding the conception of Nb₃Sn

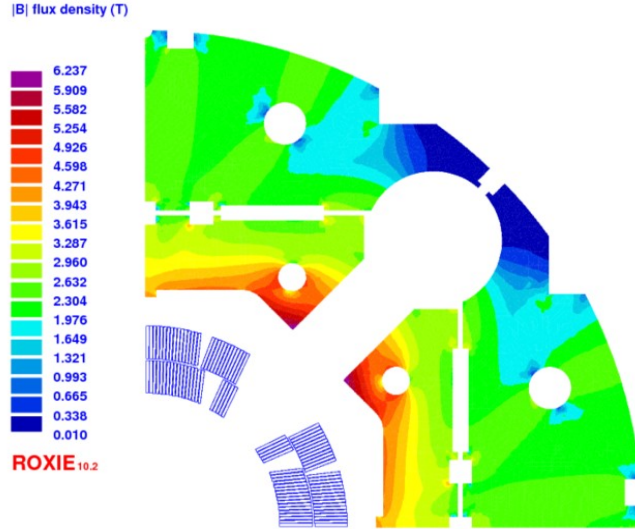


Figure 2-5: Magnetic flux density [T] for the iron parts in one quarter of the magnet cross section.

quadrupoles (TQ, LQ, HQ, LHQ coils) [109]-[110]. Each coil is composed of four conductor blocks distributed in two layers (inner and outer), providing the necessary geometrical freedom needed to reach the field quality target while maintaining a low level of complexity for the winding operations [125]. The design aims as well at balancing the azimuthal magnetic force and mechanical pre-load in both coil layers, thus generating almost the same peak stress for the inner and outer conductors.

In order to reduce the magnet current, the number of cable turns were maximized for the presented coil layout. The resulting high conductor packing translates into a large overall current density⁶ ($J_{overall} = 469 \text{ A mm}^{-2}$) that influences the magnet operational margin, as will be shown in next subsection. A total of 50 cable turns compose the coil windings, where the inner layer is made of 22 turns and the outer layer completes the 28 remaining ones. The cables are wound around a central Titanium pole, and copper wedges act as the required spacers between conductor blocks (See Figure 2-3).

An additional contribution to the main magnetic field is obtained in the design through the presence of an iron yoke and iron pads. These two components, which

⁶The overall or engineering current density is defined for the insulated conductor area.

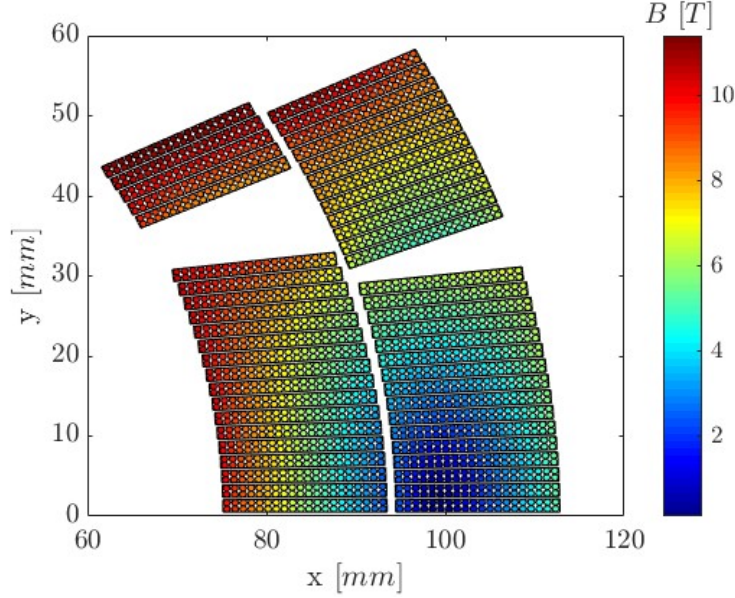


Figure 2-6: 2D cable and strand disposition for a magnet octant. The magnetic flux density in T is shown for all the strands. The coil peak field is identified for the inner layer conductor located in contact with the pole.

surround the superconducting coils, reinforce the field in the aperture and reduce the stray magnetic field outside the magnet. The magnetic flux density in the iron parts can be seen in Figure 2-5. As it follows from the plot, the large magnetic field level translates into a highly saturated iron yoke, which still contributes to the enhancement of the magnet gradient by about $\approx 8\%$ at nominal current. The large iron saturation has, additionally, a strong impact in the magnet differential inductance (L_d). A remarkable non-linear dependence of L_d with the current level is identified for the magnet.

Under these design choices, the coil peak magnetic field is located in the upper block of the inner layer, and corresponds to the cable turn in contact with the pole. The two dimensional conductor disposition, and corresponding magnetic field map, are shown for one magnet octant in Figure 2-6.

For the three-dimensional coil geometry (Figure 2-7), a solution based on the subdivision of the straight section layout in 6 end-blocks was adopted. Such a design, where the conductor groups are separated by so-called end-spacers aims at: a) reducing the peak field in the ends below the straight section value. With this objective,



Figure 2-7: MQXF short model coil design. Views from the outer layer (top) and inner layer (bottom).

the iron pads are as well replaced by non-magnetic pads in the magnet extremities, b) keeping a mechanically suitable shape, which minimizes the strain energy in the conductor and guarantees a correct cable stability, c) producing a compact end geometry, in order to increase the magnetic length for a given coil length, and d) maintaining the integrated field quality below acceptable levels and minimizing the field errors.

Further details on the coil fabrication and on the quality of the magnetic field produced by the magnet can be found in [29], [111], [120]-[124].

Magnet parameters

In view of our future development on quench processes and their mechanical effects, it becomes specially important to revisit here the concept of the magnet operational margin. It is also interesting to review some simplified mechanical implications of the chosen magnetic design.

As already introduced, the MQXF quadrupole aims at generating at 1.9 K, a magnetic field gradient of 132.6 T m^{-1} in a 150 mm wide aperture. This nominal operation regime is reached under a magnet current of 16.47 kA and a peak field of 11.4 T (See Table 2.3).

The necessary steady-state operational margin for the system is set in terms of the so-called magnet short sample limit (I_{ss} or J_{ss}). This limit corresponds to the intersection of the magnet load line with the superconductor's critical surface. The

Table 2.3: Coil and magnet parameters*.

Parameter	Unit	
Coil clear aperture diameter	mm	150
Magnet (LHe vessel) outer diameter	mm	630
No. turns in layer 1 / 2 (octant)		22 / 28
Operational temperature, T_{op}	K	1.9
Magnetic length (Q1-Q3) / (Q2)	m	4.20 / 7.15
Operational gradient, G_{nom}	T/m	132.6
Operational current, I_{nom}	kA	16.47
Operational conductor peak field, B_{op}	T	11.4
$I_{\text{op}} / I_{\text{ss}}$ at 1.9 K for RRP / PIT (specs.)	%	77 / 79
Ultimate gradient, G_{ult}	T/m	143.2
Ultimate current, I_{ult}	kA	17.89
Ultimate conductor peak field, B_{ult}	T	12.3
$I_{\text{ult}} / I_{\text{ss}}$ at 1.9 K for RRP/PIT (specs.)	%	84 / 86
Stored energy at I_{nom} (Q1-Q3) / (Q2)	MJ/m	1.17
Differential inductance at I_{nom}	mH/m	8.21
Stored energy at I_{nom} (Q1-Q3) / (Q2)	MJ	4.91 / 8.37
F_x / F_y (per octant) at I_{nom}	MN/m	+2.47 / -3.48
F_z (whole magnet) at I_{nom}	MN	1.17
F_θ layer 1 / layer 2 (per octant)	MN/m	-1.84 / -2.14

*At the time of writing this thesis, an update of the operational parameters for the magnet is being performed.

load line being, the curve defined by the current (or current density) level of a certain conductor versus its corresponding magnetic field value. In the case of the magnet, the global load line is defined by the peak field conductor when the effect of mechanical strain is neglected (Figure 2-4). For the presented design, MQXF will operate in nominal conditions at a 77% of its short sample limit. In other words, the design accounts for a 23% of margin on the magnet load line.

Although the concept of load line margin has not a physical meaning, it is widely used in the design of superconducting magnets. Interestingly, its value can be converted into the corresponding temperature and energy gaps between the operation point and the critical surface. The two new margin concepts, known as temperature and energy margins, have a greater physical significance and become essential in the understanding of the magnet performance and limits. The three definitions are

Table 2.4: Magnet margin concepts.

Concept	MQXF	Description
LL_{margin}	23 %	<i>Margin available in the magnet loadline.</i> Represented in Figure 2-4.
$T_{margin} = T_{cs} - T_{op}$	5 K	<i>Temperature margin:</i> Distance in temperature from the operation point to the critical surface. See Figure 2-9. The intersection with the critical surface is called the current sharing temperature (T_{cs}), and sets the temperature level at which the current starts to be shared with the stabilizing matrix [82].
$E_{margin} = \int_{T_{op}}^{T_{cs}} \bar{C}_p(T) dT$	21.2 mJ cm ⁻³	<i>Energy margin:</i> Corresponds to the conversion of the temperature margin into its energy equivalent for the insulated conductor. Under an adiabatic approximation, it is done via the integral of the weighted volumetric specific heat of Equation 1.9.

summarized in Table 2.4

The temperature margin can be seen as the capital available to absorb external disturbances when continuous energy deposition mechanisms are present. Its value can be computed for each superconducting strand as shown in Figure 2-8. Following the same development than for the load line case, the lowest margin corresponds to the peak field strand, and defines the magnet margin ($T_{margin} = 5$ K, Figure 2-9). On the other hand, the energy gap provides a quantitative estimation of the budget available in the case of sudden energy losses. As pointed out in the introduction, the low specific heat of the coil constituents at cryogenic temperature translates into a very reduced tolerance to external energy inputs. The three concepts will become important in future chapters of this thesis.

To conclude the review on the essential magnet characteristics during operation,

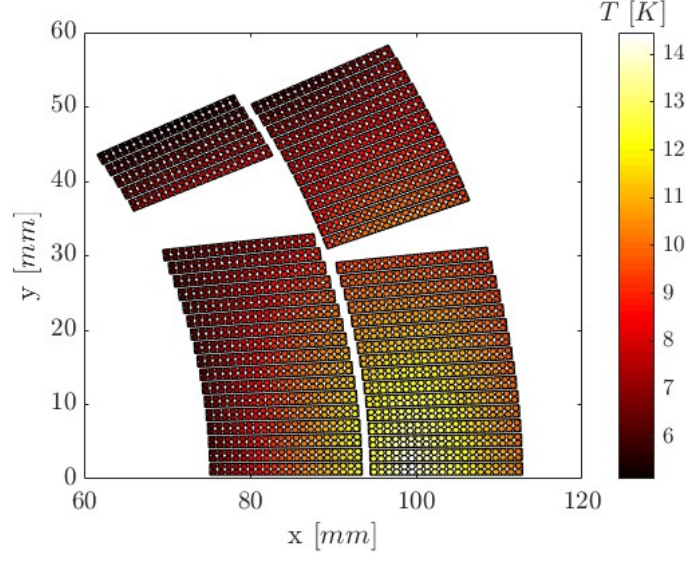


Figure 2-8: Computed temperature margin for each superconducting strand in one magnet octant. The peak field strand shows the lowest temperature margin, defining the corresponding value for the magnet.

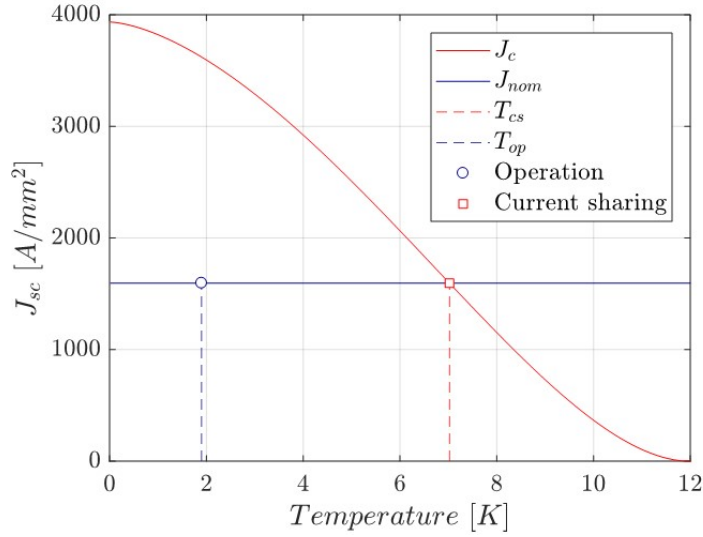


Figure 2-9: MQXF temperature margin for the peak field conductor obtained at nominal current ($B_p = 11.4$ T and $I = 16\,470$ A). The operation regime is represented by the empty dot. The current sharing temperature is highlighted with a squared marker.

a first insight on the mechanical implications of the magnet design is given using simplified analytical expressions in Table 2.5 [87], [126]-[127]. These calculations, which are done approximating the detailed MQXF conductor layout by a single sector

Table 2.5: Analytical mechanical estimates for MQXF (single sector coil approx.).

Expression*	MQXF	Description
$\sigma_{\theta}^{Max} = \frac{JGr_{ap}^2}{4}f_{corr}$	110 MPa	<i>Accumulated azimuthal stress in the coil mid-plane:</i> Obtained from the integration of $(J \times B)$ along the coil inner radius. Note the quadratic dependence on the magnet aperture.
$\sigma_r^{Accum.} = \frac{5}{12}wJGr_{ap}$	69 MPa	<i>Accumulated radial stress along the mid-plane:</i> Obtained through integration of $(J \times B)$ along the horizontal axis. Considers a linear magnetic field that becomes null at 2/3 of the coil width.
$F_z = \frac{E_{stored}}{l}$	1.17 MN	<i>Total electromagnetic force in axial direction:</i> The mechanical concept implies that the force would be entirely transmitted to the longitudinal support system. In reality, a relevant fraction of the axial force is kept by the friction between the structure parts and the coils. Both the experimental results and numerical simulations confirm that for MQXF, less than 5 % of the force is transmitted to the axial support system.

* where J [$A\ m^{-2}$] is the sector overall current density, G [$T\ m^{-1}$] is the gradient, r_{ap} [m] is the aperture radius, f_{corr} is a correction factor that accounts for the stress accumulation inside the coil [126]-[127], w [m] is the total coil width, E_{stored} [J] is the magnetic stored energy and l [m] is the magnetic length.

coil, unveil a clear parametric dependence for the coil loads [87]. They also show straightforwardly the necessity of designing a mechanical structure able to support and manage the large electromagnetic forces.

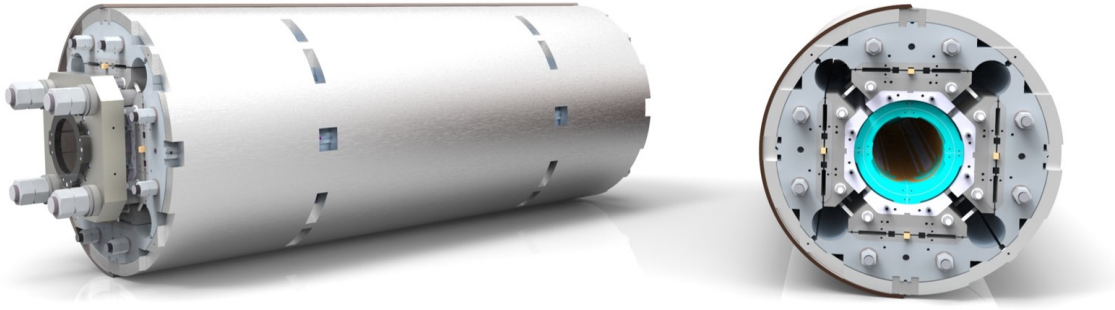


Figure 2-10: Rendered view of the MQXFS short model magnet. Half of the stainless steel shell for He containment has been removed for visualization purposes. The longitudinal support system based on tie rods and end plates can be seen in the left image.

Mechanical concept

The magnet mechanical design has its foundation in the bladder and key technology [128], successfully developed as part of the LARP research program. The four superconducting coils are assembled inside a shell-based support structure, which is conceived for managing the large electromagnetic forces whilst minimizing any potential displacement in the magnet components.

The design of the structure has its key characteristic in the application of an initial mechanical pre-load to the superconducting coils through a system of water-pressurized bladders and interference keys. The pre-load, also referred extensively as pre-stress, seeks to counteract the effect of the Lorentz forces that will later develop during magnet operation. Thanks to it, the conductor displacement during powering can be minimized. In addition, the stress state of the magnet constituents and their corresponding interfaces can be also controlled.

The pre-stress is applied during the magnet assembly operations, when the bladders (whose location is shown in Figure 2-3) are inflated in successive pressure steps. At each step, the provoked bladder expansion results in the compression of the four coils and in the pre-tensioning of the outer shell. The balance between the shell azimuthal tension and the coil azimuthal compression is then kept via the insertion of

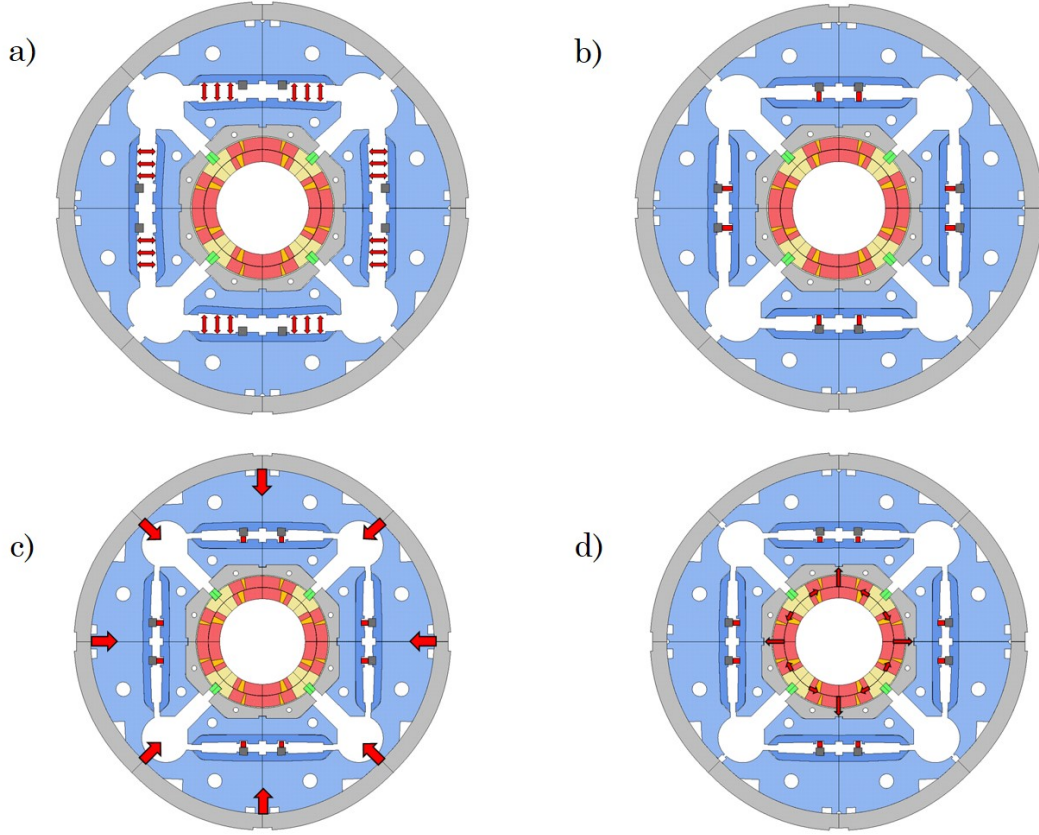


Figure 2-11: Pre-loading sequence using the bladder and key concept: a) the pressurized bladders are inflated, opening the required gap for key insertion, b) the keys are inserted and the bladder pressure is released, c) cool down process and d) magnet powering. Red arrows provide a schematic representation of the forces in the structure.

the interference keys. The operations, which are done at room temperature, finish once the desired level of coil pre-stress at warm is reached. Such level will be further increased up to the final target at cryogenic temperature during the magnet cool-down process. An additional pre-load gain is then achieved thanks to the selection of a material for the outer shell (aluminum), which presents a larger thermal contraction than the rest of the magnet components. The tensioned shell increases its stress during cool down, due to the thermal contraction mismatch, further compressing the superconducting coils. The complete process is described in Figure 2-11.

Looking at the different MQXF structure components from the magnet aperture to the outer radius, on first instance, aluminum collars allocate the four superconducting



Figure 2-12: View of the longitudinal support system composed of four tie rods and two thick end-plates.

coils. For these, a G11 pole key placed on top of the pole guarantees their correct alignment. Iron pads made of thick laminations are then bolted around, defining the so-called coil pack. This sub-assembly is connected to the iron yoke by the iron masters, which provide the necessary slots for the keys and the bladders. Finally, the shell made of aluminum completes the structure and a stainless steel vessel guarantees the necessary LHe containment. The function of the aluminum collars, not like in other magnet designs, is of purely alignment and they do not provide any mechanical pre-load. Nevertheless, it is important to note that part of the azimuthal force from the shell can be intercepted by the pole key, when the latter is in contact with the collars (See Figure 2-3).

In what concerns the three dimensional characteristics of the structure, the design accounts for the segmentation of the outer aluminum shell in axial direction. The reason for this choice is twofold: a) first, it is done to minimize the shell axial tension after cool down, which results from the presence of friction and thermal contraction differences with the yoke (the objective being to avoid potential stick-slip motion during powering), and b) to ensure a uniform azimuthal load of the coils. The different shell parts can be seen in the magnet side view of Figure 2-3.

For supporting the longitudinal magnet forces arising in the coil ends, a dedicated system is used. The so-called longitudinal support system shares the same concept of mechanical pre-load with the radial structure, where a pre-stress is applied to min-

imize the motion of the conductor in the ends. A system of four rods connected to two thick plates on both sides of the magnet, as shown in Figure 2-12, provides the necessary axial force. The two end-plates are produced in Nitronic 50 and the connection between them and the coils is done via bullet screws. At room temperature, the loading operations are carried out by pre-tensioning the rods with a piston. As in the case of the radial structure, the target axial pre-compression is completed during cool-down thanks to the differential thermal contraction between the support system and the superconducting coils. Aluminum rods were used for the short model magnets, while the long series units will be assembled with stainless steel rods. Stainless steel increases the stiffness of the structure, becoming more appropriate in view of the longer magnet length.

The absolute pre-load level, both for the azimuthal and axial directions, is a compromise between the total or partial counteraction of electromagnetic forces and the strain sensitivity of Nb₃Sn conductors. Essentially, the short model magnet phase of the project served to validate the initial design assumptions and to explore a full range of pre-load targets. While the complete original design choices⁷ can be checked in [109], it has been experimentally found that a good compromise between reaching the necessary magnet performance and guaranteeing enough margin against conductor degradation can be obtained by: a) applying an initial pre-compression to the coils after cool-down of 110 MPa in azimuthal direction, and b) by targeting a total compressive axial force of 1.17 MN delivered by the rods after cool down, corresponding to 100 % of the total axial electromagnetic force (F_z) in the magnet.

The detailed magnet mechanical behavior will be described in Chapter 4 with the help of 2D and 3D Finite Element models. For a review on experimental results, the reader is encouraged to check [129]-[138].

⁷The magnet pre-load was initially chosen to ensure that the mid-point of the conductor block in contact with the pole, remained under compression when the magnet was powered to the operational current.

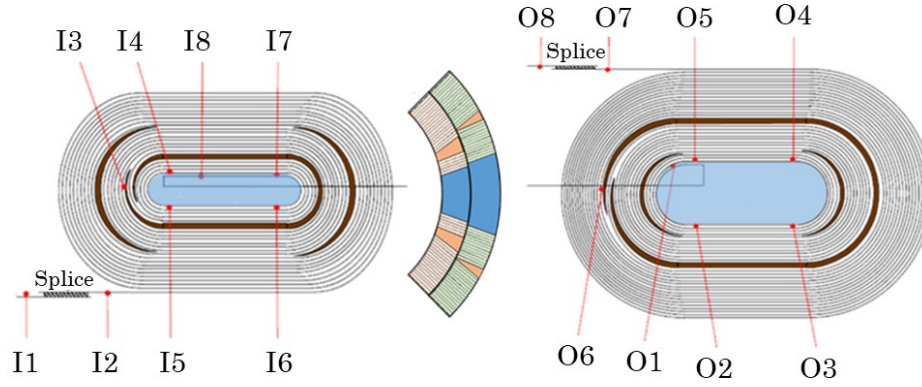


Figure 2-13: Voltage taps installed in the MQXF short model magnets. Inner layer taps are named using the I- prefix, while outer layer ones use the O- prefix.

2.3.3 Magnet instrumentation

Monitoring the magnet behavior, both in terms of mechanical and electromagnetic performance, is an activity of special importance during the initial development phase. In order to provide a clear feed-back on the design assumptions and operational parameters, the MQXF short model magnets have been equipped both with mechanical instrumentation and electrical diagnostic systems.

In terms of mechanical instrumentation, the strategy relies on the measurement of the structure and coil deformation at selected key regions. Namely, electrical strain gauges and optic fibers were used to this end. Both systems underwent a successful validation campaign prior to their implementation, and showed to be very consistent in their readings [139]-[142]. We will extensively deal with the experimental signals provided by both systems in our analysis of the quench mechanics. The position of the mechanical instrumentation is shown with markers in Figure 2-3.

In what regards the electrical diagnostic systems, voltage taps have been placed along the superconducting coils. These taps allow to monitor electric potential differences during magnet powering for various conductor segments. From the study of the signals, important information on the electromagnetic conductor performance and the magnet quench behavior is obtained. A scheme of the voltage taps disposition for the short model magnets is shown in Figure 2-13.

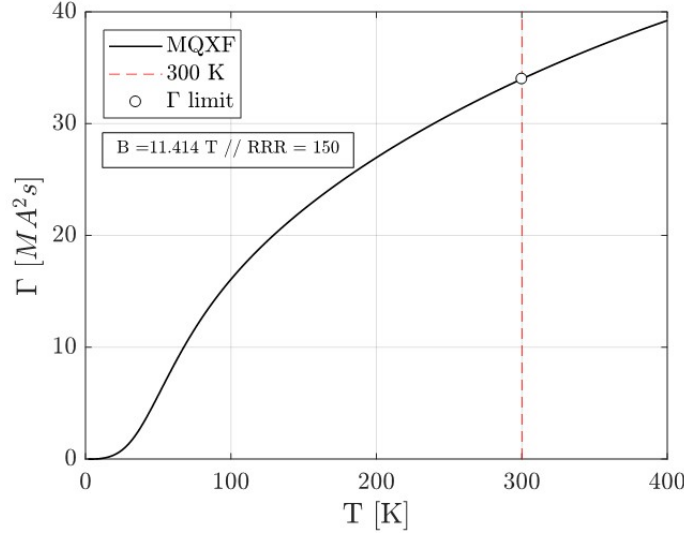


Figure 2-14: Γ function computed for the MQXF insulated conductor at a constant peak field and at the reference RRR value. The conservative limit of 300 K is shown by the round marker and corresponds to 34 MIITs.

2.3.4 Quench protection

The large stored magnetic energy density in the MQXF coils ($E_{dens} = 0.083 \text{ J mm}^{-3}$) and their high operational current density, turn the magnet protection into a very demanding exercise. The main challenges can be easily put in evidence by reviewing the most fundamental analytical concepts for a single magnet unit.

Under the adiabatic assumption described in Equation 1.9, the Γ function for the MQXF insulated conductor results⁸ in the curve shown in Figure 2-14. Considering a conventional maximum allowable temperature of 300 K (keeping the value used for Nb-Ti magnets), the function shows that a maximum quench integral of 34 MIITs can be absorbed by the MQXF coils. This limit, which is computed assuming a peak magnetic field constant during the discharge (11.4 T), provides essential information for the design of the magnet protection as shown next.

Let us start by considering a simple analytical estimation of the complete discharge of the magnetic stored energy in the coil windings. It can be done based on the conductor geometry, the magnet load line, and our recurrent adiabatic estimate of

⁸The material properties used in the calculation will be described in detail in Chapter 3.

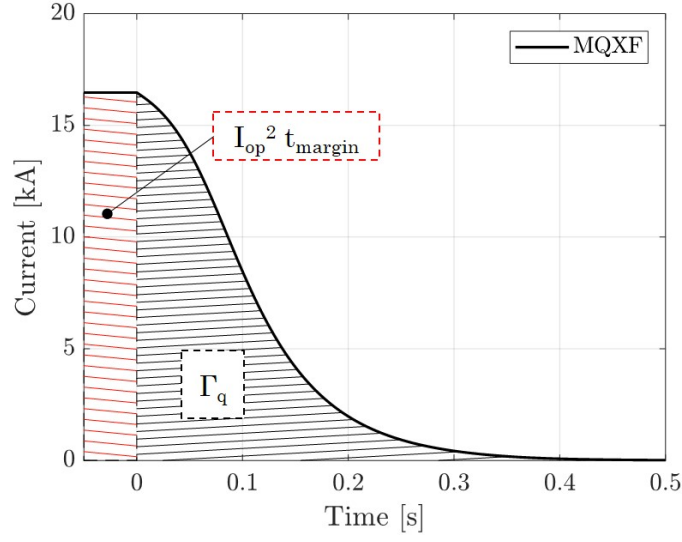


Figure 2-15: Computed MQXF analytical current decay for reference parameters, assuming that all the conductor length is quenched instantaneously at $t = 0$ s. The concept of time margin is highlighted by the red diagonal pattern.

Equation 1.9. An extreme case corresponds to the assumption that the full coil windings are quenched instantaneously at $t = 0$ s. For such a case, the current decay results in the function shown in Figure 2-15. Considering the start of the integration time at $t = 0$ s, the energy discharge translates into a quench integral of 21 MIITs. The value is depicted in the figure as Γ_q . The calculation shows, in consequence, that there is a tolerable margin of 13 MIITs before reaching the temperature limit, just by direct comparison with the maximum allowable quench integral. At constant operating current, this MIITs budget can be straightforwardly transformed into the important concept of time margin ($t_{margin} = MIITs^{available} / I_{op}^2$). The exercise brings for MQXF a time margin of 48 ms. If the maximum temperature is set to 350 K, following the baseline for Nb₃Sn magnets, the time margin increases to almost 60 ms.

The simplified analytical exercise tells us that in a first order approximation, to preserve the magnet integrity, the MQXF protection system should be designed to quench the complete magnet within a time gap as reduced as 48 ms - 60 ms from the moment the quench starts. Furthermore, since the inductance for the circuit on which MQXF will be installed prevents from extracting a relevant energy fraction by a dump resistor, the protection system is obliged to dump all the stored energy in

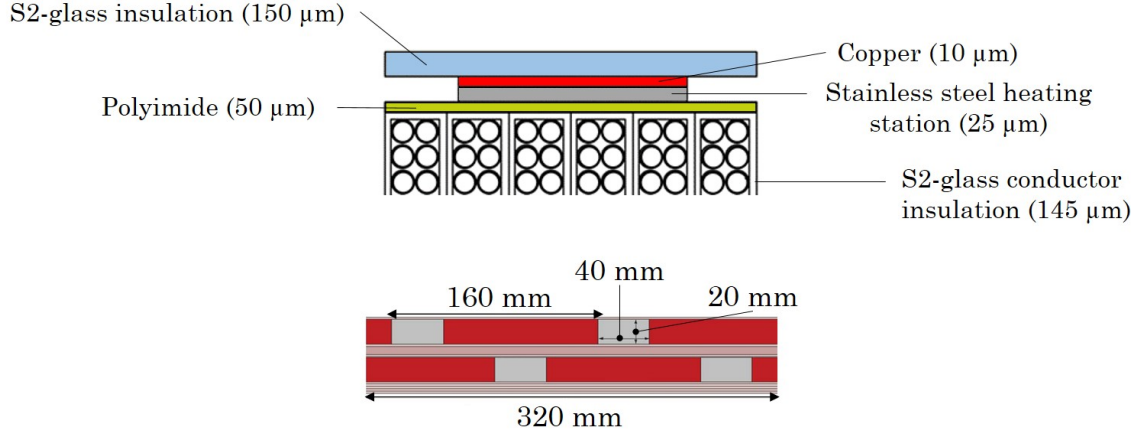


Figure 2-16: Geometry of the outer layer quench heater strips. The heating stations are shown in grey, while the copper cladding is drawn in red.

the superconducting coils.

With these objectives and constraints, the first MQXF protection strategy relied on the installation of quench heater circuits both in the inner and outer surfaces of the coils, to quench the largest portion of conductor as soon as possible. The proposal accounted as well for the parallel development of the magnet protection including CLIQ systems. The possibility of having a backup solution was a necessary strategy due to previous problems experienced by the LARP collaboration with inner layer quench heaters (IL-QH). In fact, the results from the first MQXF short model tests revealed serious detachment problems for the IL-QH due to the lack of structural support. To avoid compromising the electrical integrity of the system, it was decided to change the baseline for the magnet protection. The final strategy relies only on outer layer quench heaters plus CLIQ, reaching the challenging objectives of the quench protection.

The outer layer quench heaters are impregnated with the coils, covering the high field and low field conductor blocks (denoted as OL-HF and OL-LF respectively) as shown in Figure 1-6. The heaters are composed of 25 μm thick stainless steel strips bonded to a 50 μm thick polyimide layer. In order to reduce the total strip resistance, and to limit the firing voltage, the stainless steel is copper plated along most of its length with a layer of 10 μm in thickness. The non-plated regions, constitute the

Table 2.6: Outer layer quench heater circuit powering parameters.

Parameter	Unit	Q1 / Q3	Q2a / Q2b
Cold circuit resistance	Ω	2.2	3.9
Warm circuit resistance	Ω	2.3	0.6
Total resistance heater circuit	Ω	4.5	4.5
Voltage	V	900	900
Capacitance	mF	7.05	7.05
Peak current	A	200	200
RC	ms	32	32
Peak deposited power density (heating station)	W cm^{-2}	218	218
Deposited energy density (heating station)	J cm^{-2}	3.46	3.46

so-called heating stations. Both OL-HF and OL-LF heaters account for 40 mm long and 20 mm wide stations separated by 120 mm, as shown in Figure 2-16. The pattern has been optimized to maximize the number of turns covered by the stations, and to be able to quench the complete conductor segment between two consecutive stations stations in less than 5 ms at the operational current. The main parameters of the quench heater system are shown in Table 2.6. Note that during the model magnet phase of the project, an external resistor was added to the heater circuit to account for the shorter coil length. By doing so, the same time constant for the circuit discharge is obtained, providing a peak deposited power and energy density values that were always consistent with the series magnet baseline [98].

Regarding the characteristics of the Coupling-Loss Induced Quench System (CLIQ)⁹, the baseline for the protection of the inner triplet circuit includes one CLIQ unit for each of the six magnets (4 MQXFA and 2 MQXFB) [143]-[148]. A common CLIQ design is shared for the production of all units, being their main operational parameters adjusted for each MQXF version (see Table 2.7). The difference in the charging voltage seeks to target the same deposited peak power density for the long and short magnets. It has been shown that the CLIQ peak power density is roughly proportional to $(U_0/l_m)^2$, where U_0 [V] is the charging voltage and l_m [m] is the magnetic length [143]. Therefore, the CLIQ units for the shorter magnets need to be charged

⁹See Chapter 1

Table 2.7: CLIQ parameters.

Parameter	Unit	Q1 / Q3	Q2a / Q2b
Unit charging voltage	V	600	1000
Unit capacitance	mF	40	40
CLIQ circuit resistance	m Ω	≤ 100	≤ 100

to a lower voltage to compensate for the length. In addition, and as in the case of the quench heater circuits, in order to test the CLIQ performance in the short model magnets, the capacitance (C [F]) of the units had to be increased. In particular, the reason behind this choice relies in that the CLIQ oscillation frequency is shown to be inversely proportional to $\sqrt{l_m C}$. A shorter magnetic length results in a greater oscillation frequency, thus reducing the loss deposited over time due to a more frequent reset of the coupling currents [143].

2.3.5 Available test results

With the short model magnet campaign coming to an end at the moment of writing this thesis, the production and test of the MQXFA and MQXFB units is advancing towards the forthcoming installation deadlines.

Regarding MQXFS, a total of six short models have been tested by the CERN / LARP / AUP collaboration. Different configurations, also referred as magnet assemblies, have been tested so far for most of the short models. They account for example for the replacement of superconducting coils, or for the change of the mechanical pre-load. A small letter following the magnet name is used to distinguish between assemblies. In what follows, the naming to the magnets is introduced and available detailed publications about the tests are listed.

- **MQXFS1a/b/c/d/e:** The first MQXFS magnet produced and tested. The four superconducting coils used were wound using RRP conductors. The test was performed at Fermilab National Lab. and the campaign extended from 2015 to 2018 through five different assemblies [149] - [150].

- **MQXFS3a/b/c:** Finished its production in 2016, it was the first MQXFS magnet tested at CERN. Three magnet assemblies were explored in a test campaign that ended in 2018. As in the previous case, RRP superconductors were used [151] - [152].
- **MQXFS5:** As a part of the development campaign, and following the original baseline of producing two of the final series magnets with PIT superconductors, MQXFS5 was tested in a unique assembly composed of 4 PIT coils. The test was performed at CERN in 2017 [151].
- **MQXFS4a/b/c:** A magnet with the final RRP cable design, which set the target assembly and performance levels for the project. While its first test was performed in 2018, the test campaign concluded in 2019 with the third magnet assembly [152].
- **MQXFS6a/b/c/d:** The second PIT magnet tested at CERN in 2020. The complete campaign of four magnet assemblies was performed in 2020 [153] - [154].
- **MQXFS7a/b:** A short model magnet incorporating two PIT coils (with impregnated quench heaters) and two RRP ones (with non-impregnated quench heaters). Tested at CERN in 2021.

On the other hand, for the full length magnets, six MQXFA units have been produced and tested by LARP/AUP. On the CERN side, two MQXFB prototypes have been tested.

- **MQXFAP1 / MQXFAP1b:** It is the first 4.2 m long prototype (note the shorter length) produced and tested for the project. The magnet was re-assembled once to replace one of its four coils. The first assembly was tested in 2018, while the second one in 2019 [155].
- **MQXFAP2:** The second prototype for MQXFA, tested as well in 2018 by LARP/AUP.

- **MQXFA03:** Named as the first MQXFA pre-series magnet, it was tested in 2019 and recently accepted to be installed in the first final cold-mass for use in HL-LHC [156].
- **MQXFA04:** The magnet constitutes the second pre-series magnet, and it is currently under validation to be accepted for installation in a final cold-mass. The test was performed in 2020 [156].
- **MQXFA05:** Third MQXFA pre-series magnet, tested in 2021.
- **MQXFA06:** Fourth MQXFA pre-series magnet, tested in 2021.
- **MQXFBP1:** The first 7.15 m long prototype produced and tested at CERN in 2020.
- **MQXFBP2:** The second MQXFB prototype tested at CERN in 2021.

Chapter 3

Three-dimensional Finite Element modelling of quench transients: Multi-physics simulation

In this third chapter, we present the development of a 3D finite element code aimed at reproducing the thermal and electromagnetic response of a superconducting magnet during a quench. The model has been built in ANSYS APDL software and constitutes an essential step towards the later mechanical simulation. We provide, first, a brief introduction to the use of numerical modelling techniques. The detailed description of the code will follow next, covering the essential domains of: 1) the simulation of coupled physics and 2) the materials' characterization. Finally, the model validation using experimental results is shown.

3.1 Introduction to quench modelling

3.1.1 Numerical simulation and main challenges

An overview on the basics of quench processes and the required magnet protection has been presented in Chapter 1. It follows from such introductory material that a quench is a complex multi-physics transient, which in addition spans over very different scales

in space and time (as it will be further developed along next paragraphs). In the light of these characteristics, the rigorous study of the events and the mitigation of their effects on the magnet protection becomes an elaborate, but essential, problem for superconducting devices.

On this account, it is worth recalling that the definition of the protection strategy is a critical step in the design of a SC accelerator magnet. Independently of the method considered (where we previously tackled the concepts of tailored quench protection systems and self-protected magnets), the development of simulation tools capable of reproducing the magnet behavior during the quench results indispensable. These computations are needed for the sound definition of the strategy itself, to test the effects of potential alternatives, or to analyze the experimental data obtained from the magnet test phase.

It has been shown as well that powerful first order estimates for certain variables of interest can be obtained using simplified expressions. The aforementioned analytical solutions offer, for example, a great framework to carry out parametric analysis or to perform the comparison of various magnet designs. However, their modelling capabilities are limited and most of the times below the level of detail required for the conception of the protection system. When a deeper insight into the transient is pursued, more advanced calculations are necessary. We are referring in this latter case to the use of numerical simulations for the quench studies.

As for other disciplines of the magnet technology, one-dimensional (1D), 2D, 3D and lumped-element numerical models are routinely applied in the present days to the analysis of quench events. But before continuing with the fundamentals of these approaches, let us firstly review the main aspects to be taken into consideration for the modelling of the problem:

- **Coupled multi-physics nature:** In general terms, a quench is a multi-physics process governed by the coupled interaction of thermal, electrical and magnetic phenomena¹.

¹It should be noted that the effect of mechanical strain in the superconducting properties (when applicable) will influence the quench development. Although it is certainly a second order effect,

To understand the mutual dependency between the three fields, it results particularly revealing to analyze the case of a short-circuited magnet. In suchlike simple layout, the irreversible transition of a given length of superconductor results in the complete discharge of the magnet stored energy. The process begins at the point of the quench, where a new current distribution is originated (with the current switching from the superconducting phase to the stabilizing matrix). Straightforwardly, the change leads to the generation of ohmic losses (electric field) that will cause a localized temperature increment (thermal field) and a decay of the coil current (electric field). Following the variation in current, the magnetic flux density in the coil (magnetic field) will also decrease accordingly and will give rise to additional transitory effects in the system. For instance, induced currents (electric field) will generate new energy losses (thermal field) that will contribute to the faster development of the quench. Ultimately, the resistive zone will propagate within the magnet and the explained effects will continue until the completion of the magnet discharge. Furthermore, the change in magnetic field also affects the properties of the materials for the other physics, with a remarkable impact in the copper resistivity (electric field). Henceforth, numerical codes aiming at providing accurate information on a quench, need imperatively to be able to reproduce the tight interaction between all physics.

- **Non-linear system:** The simulation of a quench event is, in parallel, a strong non-linear problem. Restricting ourselves to our application, the main sources for such behavior can be grouped as follows: 1) the material properties, which show a strong non-linear dependency in the temperature and magnetic field, 2) the cooling processes occurring in the magnet boundaries in contact with the cryogenic bath, which account for non-linear heat flux regimes and phase changes, and 3) the modelling of the transition from superconducting to resistive state, which features a non-linear intermediate regime known as current sharing [82]. At last, it should not be neither forgotten that superconducting materials

the list of the involved physics should include the system's mechanics when strain sensitive superconductors are used.

exhibit magnetic hysteresis behavior.

- **Complex geometry:** The three-dimensional geometry of the superconducting coils used in accelerator magnets are normally based on delicate mathematical formulations. This statement is particularly true for the definition of the coil ends [82], where the conductors follow complex curved trajectories. The implementation of an accurate geometrical description in the numerical analysis becomes consequently a difficult task. As a part of the simulation, it is often required to adopt small simplifications in order to permit the solution of the problem.
- **Transient analysis:** The time dependence of the solution constitutes the fourth fundamental characteristic of a quench simulation. Transient analysis techniques (for example, on which the total simulation time is discretized in a finite number of solution steps) are required to obtain the evolution of the different variables and their derivatives along time. The correct evaluation of these, generally involves the use of very small steps sizes and turns the analysis into a very expensive problem in terms of computational resources.
- **Spatial and temporal resolution:** The last consideration is directly linked to the previous one, and highlights the large spread that exists with respect to the space and time scales implicated in a quench. For instance, the onset of a quench or its initial propagation develop in characteristic times that are in the order of micro-seconds (μs). On the contrary, the subsequent discharge of the magnet stored energy is a slower mechanism that may take many seconds to complete. The same principle applies to the spatial scale, where in practical terms the size resolution ranges from the microscopic conductor level up to the full magnet (or even the machine circuit) dimensions. In order to maintain the computational costs within fair limits, the solution of the model must be adapted to the appropriate level of detail as the process evolves.

Dealing with these stringent particularities, a flexible numerical code should therefore provide the needed information for magnet designers using reasonable calculation

efforts. The temperature distribution and electric voltages are, as described in the introduction, the two crucial outputs of a quench simulation for the definition of the magnet protection. Nevertheless, we will show how other important information such as quench propagation velocities, the protection delays, the quench onset locations and finally, the analysis of the magnet mechanics, can be also obtained using quench simulation tools.

3.1.2 The quench simulation concept

On the basis of what has been mentioned above, it results evident that the demanding task of simulating a quench event cannot be performed using one single physics model. Alternatively, the problem calls for the use of multi-field computations that incorporate the modelling capabilities for the various phenomena involved.

We will illustrate in the following the most general concept of a multi-physics numerical quench simulation. This reference case can be seen as an idealized scenario, from which we will later profit to develop our own methodology. In essence, any quench code could be represented as a variation of the described architecture (in particular, modifying the number of models included or the coupling between them).

Figure 3-1 shows the graphical representation of the concept. The next essential components need to be considered:

- (i) A detailed geometrical description of the superconducting coils and the magnet structure, based on whom the rest of the models are developed.
- (ii) An electro-magnetic field model (including the study of the magnet electrical response), providing the field distribution and the calculation of the transitory losses that occur during a quench.
- (iii) An electrical network model that describes the circuit behavior.
- (iv) A thermal model that determines the temperature distribution and the quench propagation.

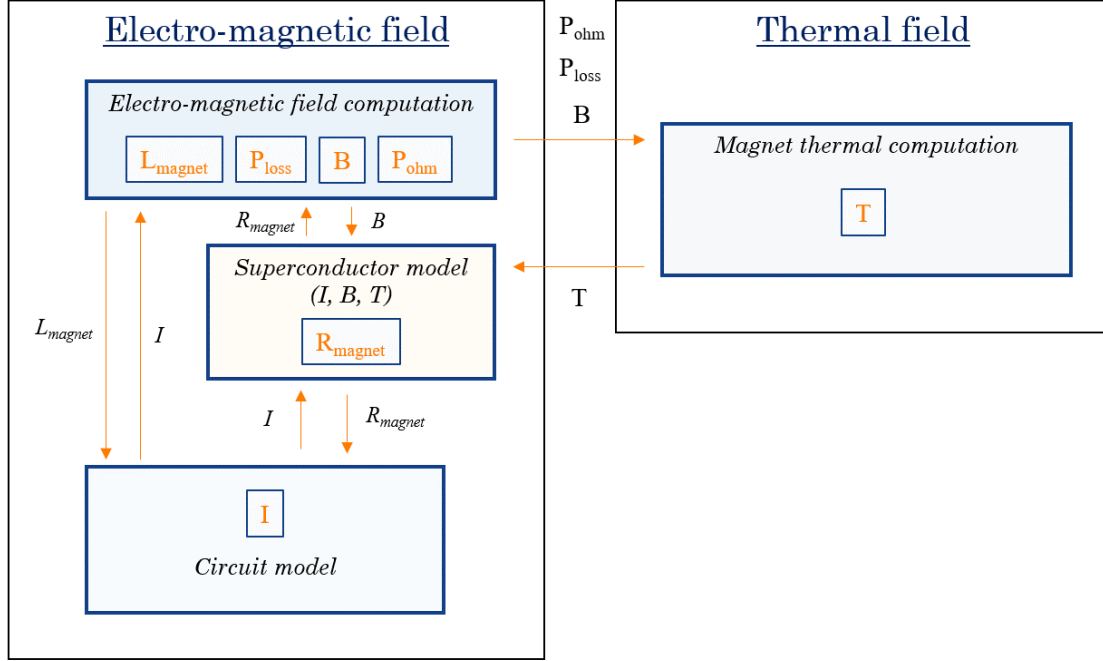


Figure 3-1: Quench simulation diagram. Arrows depict the coupling mechanisms, while the boxes group the main entities in hierarchical order. For the sake of a better understanding, we just explicitly refer to the shared variables between models (For example, important quantities as the peak voltages can be considered as internal results of the corresponding model).

- (v) At last, a model for the physics of superconductivity, which defines the superconducting state along the conductor geometry and its transition to the normal regime.

The solution to the quench problem is obtained from the interaction of the individual models by means of a so-called solving (or quench) algorithm². The latter establishes the degree of coupling between the physics and drives the solution process following the time transient. Disregarding for the moment the spatial dimensions or the specific numerical methods employed, the reference quench calculation can be described as the following process:

1. First, a time interval of interest is chosen. In this regard, usual time integration

²For some cases, the solution of two or more of the physics can be also incorporated in a single direct coupled-physics approach. This type of coupling is just a variant of the one presented in the figure. We will show it in next section.

schemes rely on dividing the total time into a finite number of time steps (time discretization).

2. At each of the steps, the magnetic field problem is solved by its corresponding model (denoted as electro-magnetic field computation in the diagram). It provides to the other computations, the transient results on the field distribution (B), the energy losses generated due to the e.m. transient (P_{loss}) and the detailed magnet inductance (L_{magnet}).
3. In a similar way, the magnet electrical model (considered as a part of the global e.m. one) solves for the electric field problem. The losses generated by joule effect in the resistive zones are calculated (P_{ohm}) and shared with the rest of the models.
4. The two previous heat inputs (P_{loss} and P_{ohm}), together with the magnetic field data, are now transferred to the thermal computation that calculates the corresponding new solution for this field. The modified temperature in the system (T) is obtained.
5. The superconducting state of each discretized conductor can be now updated using the magnetic field, current and new temperature values (simulation of the quench propagation). As a result, the updated magnet resistance is calculated (R_{magnet}).
6. This new resistance value and the magnet inductance would be transferred to the model of the electrical network, which computes the corresponding current decay in the magnet according to the circuit behavior.
7. To conclude, the information on the different variables is stored in a database and the solution of a new iteration starts. At the end of the simulation the time dependent solution of the transient is attained.

Please note that for the sake of a better understanding we have just explicitly referred to the shared variables between models. Important results, like for instance

the voltages, can be considered as internal results of the corresponding calculation.

Although the described concept is just a generalization of the quench simulation process, important information can be already extracted from it. The primary conclusion is that the computational efforts required to perform an analogous quench calculation (fully coupled) are very elevate, in particular, when the 3D simulation of several meters long magnets is needed. It is therefore a common strategy in practical codes to adopt some simplifications in the models, with the objective of decreasing the complexity of the solution.

One of the most recurrent proposals relies on decoupling the magnetic field problem from the rest of the simulation. In general terms, just the solving of the thermal and electric parts is performed. For the missing variables, scaled magnetic field maps and lumped inductances are used. The model architecture can be understood by removing the boxes of B , P_{loss} and L_{magnet} from Figure 3-1. Nevertheless, these solutions may have important drawbacks if not carefully reviewed, for instance, when strong saturation or dynamic phenomena are present in the magnet (note that their effects need to be now accounted as an external input and must ensure the energy preservation in the system).

A better approximation, which is the core of the methodology explained in [82], consists instead in replacing the strong coupling between the magnetic and the other models by a weak-coupling mechanism. Here, the update of the magnetic field is just performed at selected steps when the magnet current has gone through a significant change. This is particularly interesting due to the fact that the calculation of the magnetic field is computationally more expensive than the solution of a time step of the thermal model, also in view of the different time constants of both physics.

3.1.3 References on available codes

A large number of computer programs have been published since the early days of the field, closely following the presented guidelines. They usually differ with each other in the simplifications assumed and in the number of sub-models included in the simulation. Hence, ranging from very simplified models up to completely coupled

simulations. An example on the strong coupled approach can be found for solenoid magnets in [157].

The model dimensions and the numerical methods employed are also different between codes. They include from lumped-element formulations up to multi-dimensional models where the solution is obtained from the combination of various numerical methods. In this group, we distinguish between laboratory developed and/or proprietary codes [158]-[163], the just-mentioned lumped circuit element models [164]-[165], approaches based on the customization of the commercial software COMSOL Multiphysics [166]-[167] or the solutions based on the coupling of multiple softwares using co-simulation [168]-[171]. Strongly linked to our research, it is important to highlight the novel user-defined elements for ANSYS (incorporating the physics of superconductivity to the element formulation), which have been recently published in [172].

Centering our attention to the accelerator magnet community, a sound discussion on the main codes employed can be also found both in [173] and in [82].

3.1.4 The Finite Element Method (FEM)

The Finite Element is a powerful numerical method used for solving boundary-value problems based on the domain discretization and on the statement of the physical laws by means of weak³ or variational formulations.

Models based on Finite Elements are broadly used nowadays for the simulation of various phenomena in engineering and mathematical physics. In the field of superconducting magnets, FEMs are very well represented in the electromagnetic design, in the simulation of the magnet mechanics during the assembly and powering phases, or in the analysis of diverse multi-physics problems affecting magnet operation.

Belonging to the latter group, the FE method can be seen as a well suited approach for the solution of the various models that participate in a quench simulation. In fact,

³The weak form of a problem defined by differential equations is an equivalent integral formulation on which the equations are no longer required to hold absolutely. Instead, it is required that they are fulfilled in a weighted, projective sense over the problem domain for appropriately chosen weighting functions. The weak form allows to transfer the concepts of linear algebra to the solution of the problem.

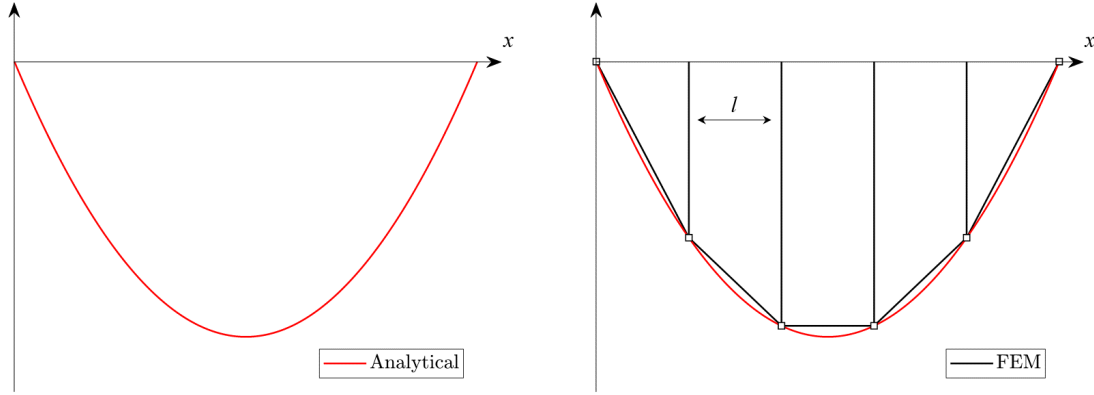


Figure 3-2: Analytical and finite element solution for the 1D boundary value problem solved in the Annex. The element size (l) is indicated by the black arrow. 5 elements have been considered.

FEMs allow for the study of the quench problem under the presence of complex magnet geometries and irregular boundary conditions. As a general drawback, depending on the level of detail established, they may incur in large computational costs compared to other approaches. As basic references on the Finite Element Method we look at [174]-[177]. Simple examples on its application to superconducting magnets in thermal and magnetic calculations are found in [178], [82].

The correct understanding of the FEM results crucial for its accurate and stable application to quench problems. We develop in the Appendix, step by step, the example presented in [82] to demonstrate the Finite Element Method. A one dimensional boundary problem, whose solution is advanced in Figure 3-2, has been chosen due to its simplicity. The results shown in the plot compare the exact (or analytical) solution and the FEM results using 5 linear elements. It is already instructive to see from such a simple case, how the number and size of the elements impact the approximation of the solution. For the presented case, a larger number of elements would result in a closer output with respect to the searched solution. A second main message can be also extracted from the interpolation function between nodes. Higher order elements (for instance, using quadratic functions instead of the linear interpolation adopted in the example) result as well in a better description. We will deal extensively with

these features in the next sections of the chapter.

Apart from the basics of the method, more complex considerations need to be taken into account for the application of FEM to quench studies. In this regard, two aspects are of particular importance:

- **Non-linearity:** The non-linear behavior of the quench problem requires, in practice, the addition of iterative solvers to obtain the desired approximation. It is the case for example of the non-linearities arising from the material properties. Methods based on the classical Newton-Raphson technique [174] are the most employed by commercial finite element codes and will be the ones used in this thesis.
- **Time integration:** On the other hand, the transient problem requires to include time integration techniques for the approximation of the unknown degrees of freedom and of their derivatives along time. On this subject, commercial finite element packages usually employ the Newmark and General Hilber-Hughes-Taylor method for time stepping in second order systems. In first order ones (for example, thermal or magnetic analysis), the generalized trapezoidal rule is employed. Detailed information can be found in [180]. Adaptive methods (where the time step size is determined as function of the problem solution) have been incorporated in some popular software packages e.g. ANSYS or COMSOL and will be extensively used in our analysis.

A careful treatment of all these characteristics can be found in [174], [178].

3.2 A 3D FEM quench simulation built in ANSYS APDL

A three-dimensional quench simulation based on the Finite Element Method, whose objective is of providing all the necessary information for the later mechanical analysis, is developed in this section.

The proposed quench code relies on the already presented strategy of decoupling the magnetic field calculation from the rest of the models, in a similar way as many other quench programs do. In addition, the core of the multi-physics FEM problem is solved in the commercial software ANSYS APDL. The latter corresponds to the same software employed for the analysis of the MQXF magnet mechanics, thus decreasing the number of modelling tools required. The main advantages of this practice are as well: 1) full compatibility is achieved for the quench⁴ and mechanical models, and 2) the load transfer between codes becomes straightforward.

The development of ANSYS models for the simulation of quench transients can be traced back to a series of key references, which had a strong influence in our present methodology. In [181] and [182], a thermal model was used to simulate the quench behavior in three-dimensional magnet geometries while solving the electrical part analytically (profiting from the inclusion of the corresponding equations in the code). Interestingly, the model was conceived initially as a required step for the simplified analysis of transient stresses. The strong coupling between the thermal and electric physics was then exploited in [183] and [184]. In these two references, the simulation of quench propagation was performed at a constant background magnetic field. As for the previous case, three-dimensional studies were performed with the ultimate goal of allowing for the final magnet mechanical analysis. Despite the fact that the quench propagation was well captured by the model, the magnetic part of the problem remained still out of the study. The inclusion of the magnetic field influence in the material properties was finally implemented in [185]. This last reference included, in parallel, a superconductor model to the simulation by adding a set of additional equations in the code. Unlike in the previous works, the analysis was restricted to the study of propagation velocities in detailed conductor geometries.

The exact characteristics of the 3D FEM simulation constructed for this thesis are shown in the next five subsections, including: i) the definition of the quench algorithm, ii) the description of the main thermal-electric FE code, iii) the superconductor model, iv) the magnetic, and v) the circuit simulations.

⁴We will refer as "quench" model to our developed multi-physics code.

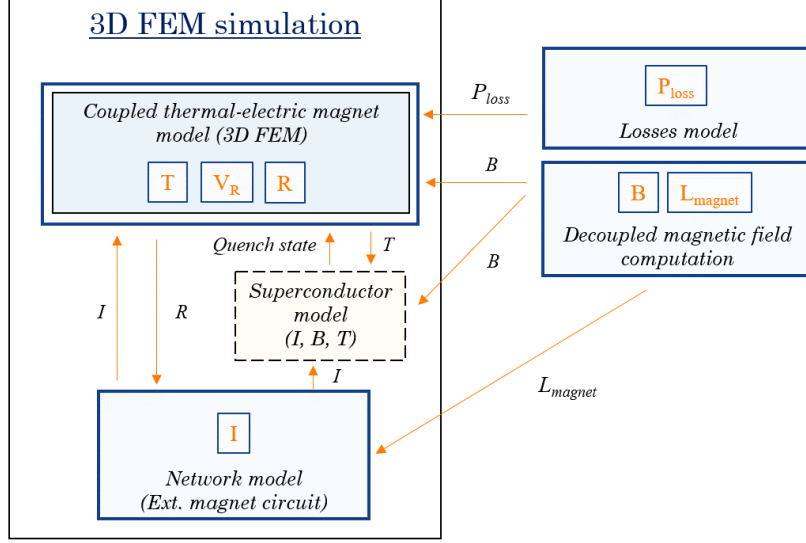


Figure 3-3: Quench simulation using a thermal-electric model. In this case, the magnetic part is completely decoupled. The necessary information is instead accounted as an external input to the calculation (B , P_{loss} and L_{magnet}).

3.2.1 The quench simulation algorithm

Figure 3-3 summarizes the final structure of the three-dimensional code. With the magnetic field decoupled from the other models, just the transient solution of the thermal and electric fields is required. On this subject, a direct coupling technique is used for the modelling of the two physics inside the magnet.

The direct, or strong coupling method, has its fundamentals in that all the degrees of freedom are contained in one single FEM simulation. Hence, the link between the thermal and electric phenomena is handled by calculating element matrices and element load vectors that contain all the necessary terms for the solution. In the diagram, the corresponding 3D computation is named as "Coupled thermal-electric magnet model".

Moving downwards in the figure, the physics of superconducting materials are taken into account by means of a superconductor model that incorporates the critical surface to the simulation. Thanks to this model, the code distinguishes between superconducting or resistive elements and reproduces the quench propagation.

The behavior of the external electric circuit is accounted by means of lumped-

element formulations. For a simple circuit, as for instance the protection of a single magnet with a dump resistor, such a network model results in a set of analytical equations that are easily solved at each iteration.

The necessary information on the variables belonging to the magnetic domain is included thanks to the decoupled field model. The separate magnetic computation is in charge of providing the field data for each conductor (conductor load lines), and also the information on the magnet differential inductance. At last, the losses due to the e.m. transient are added as external heat inputs.

Following the same treatment provided to the reference quench simulation, the algorithm for three-dimensional FEM analysis reads as follows:

1. First, the total time is discretized into several time steps. Adaptive stepping is used for their solution, profiting from the ANSYS capabilities. At the end of each step, the FEM solver is stopped and the quench algorithm drives the necessary actions (i.e. the algorithm is based in a stop and restart process of the FEM model).
2. For each discrete time frame, the temperature and the voltage degrees of freedom are obtained from the thermal-electric transient computation. As explained, the magnetic field influence in the material properties and the external transient e.m. losses are handled between steps as external inputs. This main model provides the detailed temperature in the magnet (T), the resistive voltages (V_R) and the total resistance (R).
3. Based on the previous results and the externally provided magnetic field, the superconductor model updates the state of all conductor elements by direct comparison of their current density with the critical one. In the same way than for the field or losses update, this operation is performed when the solver is stopped. The transition is accounted as a change in the material properties of the element, from superconducting to resistive, which becomes effective for the next solution step.

4. The magnet resistance obtained from the thermal-electric model is then transferred to the network model, which also requires the information on the magnet inductance. The latter is obtained as the third external input to the main simulation. Namely, the differential inductance curve as function of the current (either experimental or simulated) is used. As a result, the circuit model calculates the current value for the next time step (current decay).
5. Finally, the variables are stored in the database and the solution of the 3D model is restarted.

Each of the algorithm components (Figure 3-3) are explained in the next paragraphs.

3.2.2 The 3D coupled thermal-electric model

To build the 3D thermal-electric magnet model in ANSYS APDL, the direct coupled-field elements available in the software's library are employed [180]. The code is based in the following assumptions:

- **Homogenization of material properties:** The conductor, a Rutherford cable made of multiple Nb_3Sn strands, is modelled as a trapezoidal block of homogenized properties. This is a recurrent strategy in the simulation of quench events due to the high cost of reproducing the conductor down to the microscopic level.
- **Adiabatic boundary conditions:** Since our research deals with resin-impregnated superconducting coils, where the conductor is not directly exposed to the cryogenic bath, adiabatic boundary conditions are assumed for the external surfaces of the models. The heat transfer to the Helium bath is neglected and no magnet re-cooling is considered. Only conductive heat transfer is allowed in the model.
- **Current sharing regime neglected:** The superconducting to normal state transition is a step function at the current sharing temperature, T_{cs} . No current sharing regime is modelled.

Theory basis

It follows from our principal assumptions that the set of equations to be solved by the FEM computation reduce to those defining conductive heat transfer and the current flow problem.

The 3D partial differential equations governing conductive heat transfer in each material of the model write as follows:

$$C_i \frac{\partial T_i}{\partial t} - \nabla \cdot (\bar{\bar{k}}_i \nabla T_i) = \ddot{q} \quad (3.1)$$

where C_i denotes the volumetric heat capacity of the material i , T_i the temperature, $\bar{\bar{k}}_i$ the thermal conductivity tensor and $\ddot{q}$ the heat generation rate per unit volume. The latter could be further expanded as:

$$\ddot{q} = \rho_i J_i^2 + h_i \quad (3.2)$$

with J_i the current density, ρ_i the electrical resistivity and h_i any other external heat input.

On the other hand, the solution to the steady-state electric conduction problem is obtained from:

$$\nabla \cdot (\bar{\bar{\sigma}}_i \nabla V_i) = 0 \quad (3.3)$$

where $\bar{\bar{\sigma}}_i$ stands for the electrical conductivity tensor and V_i for the voltage. This equation ensures the local charge conservation ($\nabla \cdot \vec{J}_i = 0$) and computes the current distribution in the conductor elements.

Element Types

Equations 3.1 - 3.3 are solved using the 3D 8-Node Coupled-Field Solid (SOLID226) thermoelectric element for the conductor domain. Structural/insulation volumes are instead meshed with the 3D 8-Node Thermal Solid (SOLID278) element, which solves exclusively for the temperature according to Equation 3.1.

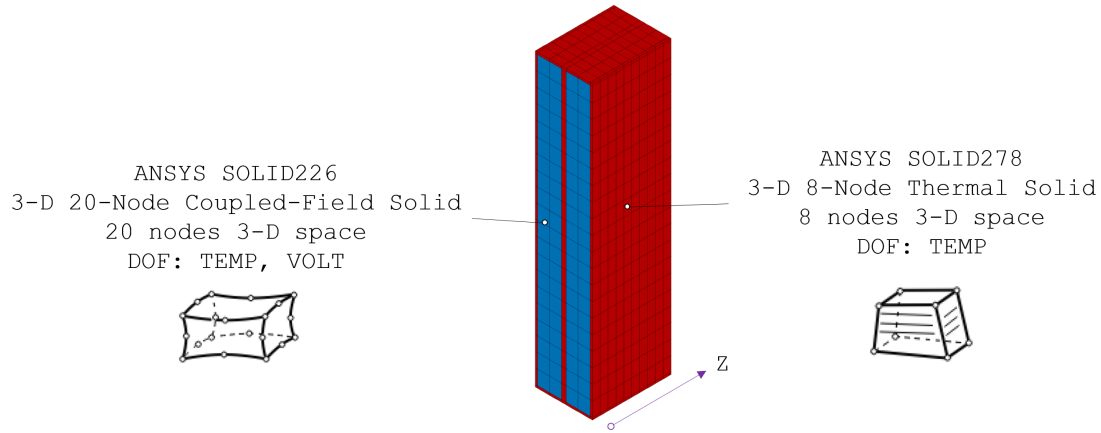


Figure 3-4: Element types used for the 3D FEM thermal-electric model.

Linear elements are used, since they have been proven to improve the numerical stability when solving thermal problems [186]. Figure 3-4 shows the example of a typical conductor discretization. The active conductor elements and the cable insulation are labelled with their corresponding element types.

Modelling considerations

The solution of the electric part of the problem requires, as for the thermal part, the imposition of suitable boundary conditions. As suggested in [183], the electric potential is fixed to zero at one end of each conductor domain, whereas an uniform and unknown floating potential is set at the opposite end. By proceeding in this way, the floating potential equals the voltage drop along the conductor, which grows as the quench develops.

Another important feature of the model is that the stop and restart strategy allows to manually adapt the time step size as a function of the problem requirements. Note that adaptive time stepping is already internally used by the ANSYS solver during the solution of each individual time step⁵. Here, however, we refer to the manual size adjustment that can be done prior to the restart of the simulation. The following logic is used: during the initial quench onset and propagation, small time steps in the

⁵The ANSYS adaptive time stepping algorithm divides, for each of the solution runs, the full step in various sub-steps to ensure the convergency of the results

order of $10\mu\text{s}$ are employed (the ANSYS adaptive stepping may use minimum and maximum sub-steps of $1\mu\text{s}$ and $5\mu\text{s}$ respectively for the solution of the $10\mu\text{s}$ step). After the triggering of the protection, when a large part of the magnet is quenched, the step size is gradually increased to 1 ms .

Material Properties

The most important assumptions for the material properties assignment are summarized hereinafter (Figure 3-5 and Figure 3-6):

1. All material properties are imposed as a function of temperature. The dependency in the temperature field is included in the matrix formulation by the ANSYS elements. Thus, the properties are internally updated during each time step of the transient solution.
2. Instead, magnetic field dependent material properties cannot be used with ANSYS coupled-field elements. To overcome this limitation, the properties are manually updated at the end of each step, when the solver is stopped.
3. The field dependent material properties, e.g. the specific heat of superconductors and the magnetoresistivity, are calculated with the average modulus of the magnetic field for each conductor.
4. The superconducting state is reproduced by setting a negligible conductor electrical resistivity of $1 \times 10^{-18} \Omega\text{ m}$.
5. Alternatively, for the conductor resistivity in normal state, copper material properties are employed. CUDI database has been used [187].
6. In the real superconducting cables, the current flows in circular strands that are transposed along the length. Since this geometrical description is not compatible with the homogenized conductor volumes, the copper resistivity needs to be first inversely scaled with the copper fraction. In addition, a second scaling factor is applied to account for the transposition pitch.

7. For the thermal conductivity and specific heat properties, the transposed geometry of the Rutherford cable is instead accounted by computing the material ratios in a differential volume of the cable. It can be done, equivalently, by computing the strand area as an ellipse.
8. The thermal conductivity for the homogenized conductor is assumed to be that of copper, weighted according to corresponding fraction. This assumption can be easily justified due to the fact that the thermal conductivity of copper is orders of magnitude larger than the one of the rest of constituents.
9. The conductor specific heat is calculated as a weighted average of all conductor components (Nb_3Sn , Copper and Epoxy). NIST database is used in this case [188].
10. G10 orthotropic material properties have been assigned to the cable insulation (composed of fiberglass and epoxy), extracted from NIST database.
11. Stainless steel, iron and aluminum material properties are obtained from [62], [190]-[189].

3.2.3 The superconductor model

The superconductor model (see Figure 3-3) implements the critical surface description in the simulation by means of the set of equations shown in Chapter 1 (Equations 1.2 - 1.6). The dependence on mechanical strain is not considered.

At the end of each time step, when the solution is stopped, the current density of each element is compared to its corresponding critical one (computed with the average element temperature and the externally provided peak magnetic field data). If the value is exceeded, the element status is changed from superconducting to resistive. The process is done in a loop sequence for all conductor elements as shown in Figure 3-7.

In this interpretation of the transition, the following considerations apply:

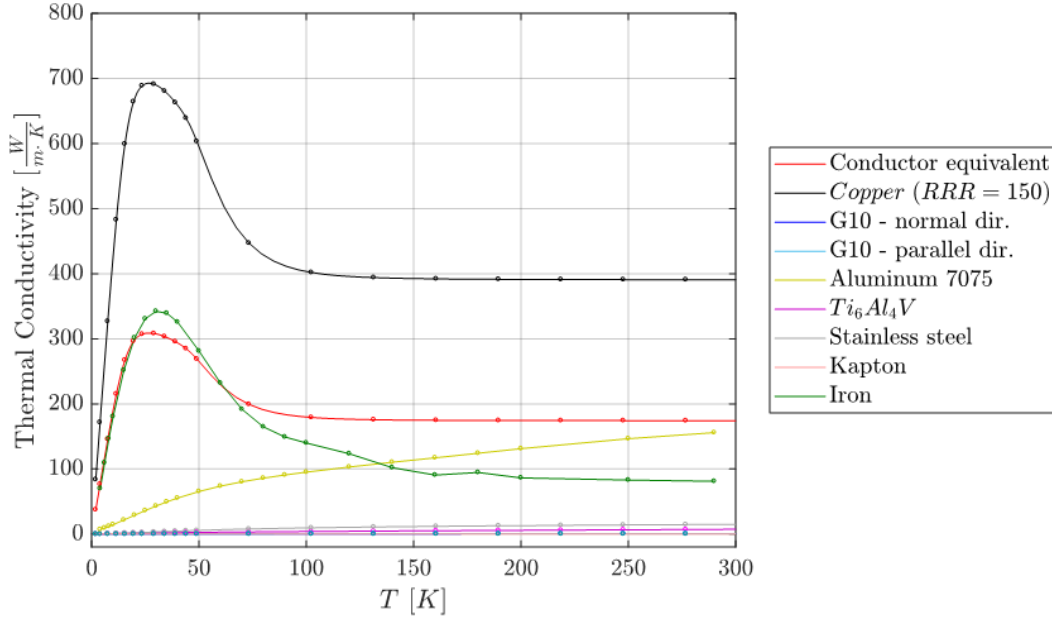


Figure 3-5: Thermal conductivity as a function of the temperature for the materials used in the 3D FEM model. When applicable, the material properties are evaluated at a magnetic field of 11.071 T (corresponding to the average field value found in the MQXF pole turn conductor at nominal magnet current).

- The quench decision is performed as a function of the peak field in the conductor.
- The superconducting to normal state transition is modelled as a sudden change in the element resistivity, from the negligible value of $1 \times 10^{-18} \Omega \text{ m}$ to the equivalent copper one.
- The necessary parameters for the critical surface parametrization are obtained from witness sample measurements from each coil/cable test.

3.2.4 The decoupled magnetic field model

The 3D quench simulation relies on the average and peak magnetic field values for each conductor, as well as on the magnet differential inductance. The mentioned information is externally provided by the decoupled magnetic computation in the form of three functions, whose independent variable is the transport current: $B_p(I)$, $B_{avg}(I)$ and $L_d(I)$. In practical terms, the two first functions, which relate the magnetic

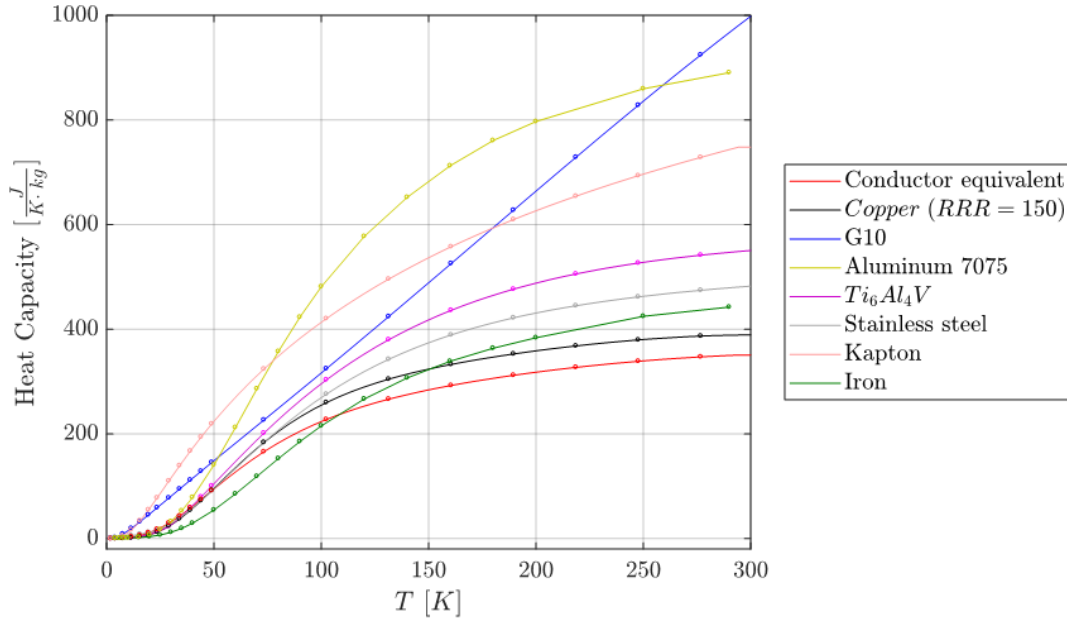


Figure 3-6: Heat capacity as a function of the temperature for the different materials used in the quench simulation. A consistent magnetic field value of 11.071 T has been used for the calculation of the equivalent conductor properties.

induction on each cable to the transport current, are the already explained conductor load lines.

As it can be seen, the dependency of the field quantities with the spatial position in the magnet has been disregarded in first instance. This simplification is based on two main pillars. First, assuming that the length of accelerator magnets is generally much greater than their cross-sectional dimensions, the solution of the 3D magnetic field problem can be reduced down to a two-dimensional approach (in other words, provided that end effects can be disregarded in the magnet straight section, the 2D results become representative of the vast majority of the 3D domain). Second, the Rutherford cables employed in most accelerator magnet coils feature a transposition pitch that is considerably shorter than the magnet length. In consequence, every strand occupies (at least once) all the positions within the cable section. Under these circumstances, the average and peak magnetic field for the straight parts of each conductor can be approximated as unique values using a 2D model.

To deal with the detailed variation in the coil ends, a simplified approach has

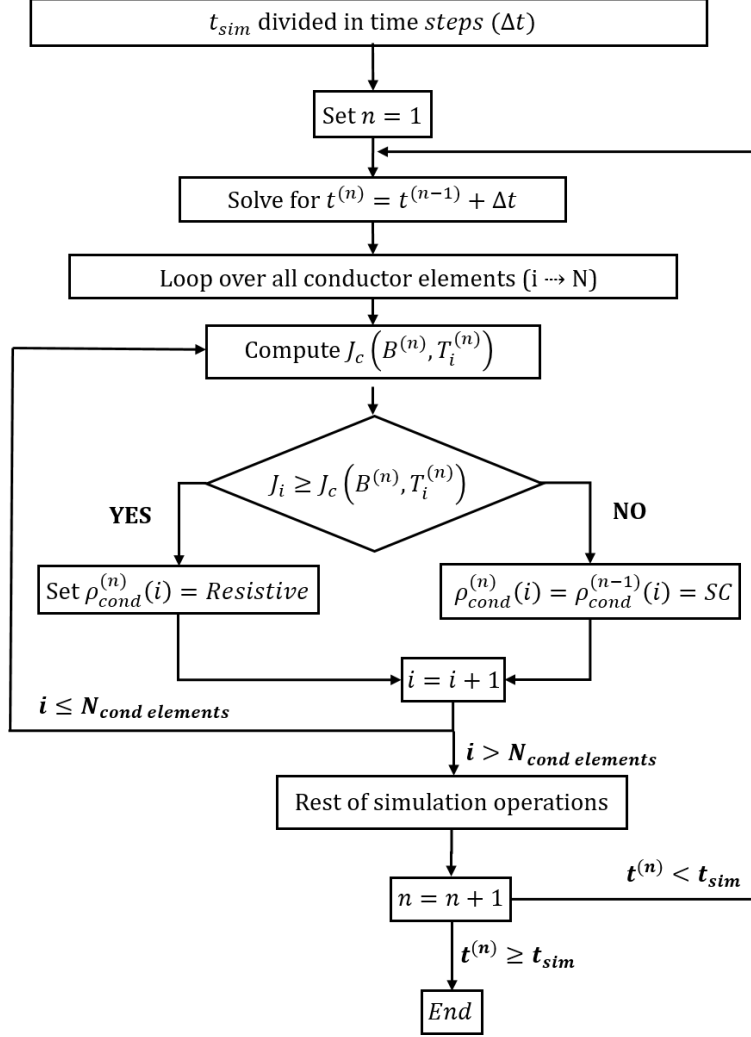


Figure 3-7: Description of the stop and restart process implemented in ANSYS APDL for the simulation of quench events.

been considered. The 3D FEM quench model divides each full cable in two volumes (straight section and end geometry). By doing so, two different functions for the peak and average magnetic field can be used. In the straight section (SS), the functions are obtained from the 2D magnetic model as explained in next paragraphs. In the ends, however, a scaling factor is applied to the SS values according to the three-dimensional magnetic design.

Restricting ourselves to the 2D magnetic field calculation, it is performed using the CERN proprietary code called ROXIE [158]. If the effects of persistent and induced currents are neglected in the calculations (these currents are assumed to be

significantly smaller than the main transport current), the program computes the total magnetic induction in the domain as the sum of the field created by the coil currents and the contribution from the magnetic materials. A coupled BEM-FEM⁶ method allows for such analysis, see [82].

To compute the field created by the transport current in the coils, the cross section of each conductor (i) is divided into a desired number of patches (N_{Disc}^i), and ideal line currents are placed in the center of these. In our case, N_{Disc}^i equals the number of strands for the MQXF cable, 40. To get a more accurate prediction of the magnetic field for each conductor, the self-field⁷ contribution in the strands is considered.

Peak and average magnetic fields for each conductor, at a given current level, are thus defined as:

$$B_{avg}^i = \frac{\sum_{k=1}^{N_{Disc}^i} |\vec{B}_k^i|}{N_{Disc}^i} \quad (3.4)$$

$$B_{peak}^i = \max\{|\vec{B}_k^i|\}_{k=1, \dots, N_{Disc}^i} \quad (3.5)$$

where $|\vec{B}_k^i|$ is the modulus of the magnetic induction at the center of the patch k , for the conductor i .

Neglecting second-order non-linear effects, like the just-mentioned strand self-fields or iron saturation, the relationship between field and current remains linear. In our case, although these non-linearities are present, simplified load lines are assumed:

$$B_{avg}^i(I) = f_{avg}^i I \quad (3.6)$$

$$B_p^i(I) = f_p^i I \quad (3.7)$$

f_{avg}^i [TA⁻¹] and f_p^i [TA⁻¹] receive the name of load line factors, and relate linearly the average or peak magnetic field in the conductor i to the magnet current (I). There

⁶BEM stands for Boundary Element Method [82].

⁷To evaluate the maximum field inside a strand, the line current approach is replaced by a homogeneous, cylindrical current distribution. Singular expressions are in this way avoided [82].

exists a known error in this approximation, which comes from the fact that the factors are calculated, for instance, at a certain level of iron saturation. Nevertheless, the use of linear load lines results much simpler for the implementation in the 3D FEM. In this thesis, the load line factors are always obtained at the nominal operation current.

To conclude, the calculation of the magnet differential inductance can be also performed using the 2D magnetic model. $L_d(I)$ is obtained in the ROXIE code based on magnetic vector potential analysis. We recall that the differential inductance is a non-linear function of the transport current due to iron saturation, induced currents and superconductor magnetization. In the present model, the effects of induced currents and superconductor magnetization are not taken into account.

3.2.5 The circuit model

On its baseline for machine operation, the MQXF magnet will be protected by the quench heaters and CLIQ systems. A conservative approach to test the mechanical consequences of a quench event consists in considering the situation where the magnet is protected only by the quench heaters. Since the CLIQ system does not participate in the protection, the expected peak temperatures are higher and so must be, intuitively, the mechanical stresses. All our simulated cases correspond to this particular configuration.

In the test of a single magnet, the equivalent electrical network of the system can be approximated by a simple RL circuit externally powered by a current source. Initially, the resistance of the magnet vanishes since it operates on its superconducting regime. When a quench happens, an electrical resistance appears in the coil windings and a resistive voltage arises. Upon the detection of the quench by the protection system, the current supply is disconnected and the energy stored in the magnet needs to be safely discharged thanks to the contribution of the quench heaters. The totality of this energy is dissipated in the superconducting coils and damped into the helium bath. The current decay is defined by the RL circuit response:

$$IR_Q + L_d \frac{dI}{dt} = 0 \quad (3.8)$$

where I is the magnet current, R_Q the total magnet resistance and L_d is the magnet differential inductance.

Equation 3.8 is solved with a step-by-step method, profiting from the stop and restart scheme implemented in the finite element model. At the end of each time step, the magnet resistance and differential inductance are updated, and the new current is computed in the form of a piecewise exponential.

3.2.6 The external losses model

Transitory losses such as eddy currents in the structural components, hysteresis losses, inter-strand (ISCL) or interfilament coupling losses (IFCL) develop in superconducting systems when the magnetic field varies fast with time. These losses can remarkably affect the current discharge in a twofold way: on one hand, they heat up the conductor, helping to speed up the quench mechanisms along the coil, in a phenomenon usually known as quench-back. Additionally, they also modify the magnet differential inductance contributing to a faster discharge of the current. An extensive treatment of transitory losses can be found in [2], [164], [95], [97], [24] and [191].

The importance of interfilament and interstrand coupling currents in the protection of MQXF magnets is today very well-known [95]. It has been demonstrated that for the case of the magnet protected with only quench heaters, the most important contribution comes from interfilament coupling currents, and that the effect of the change in the magnet differential inductance is limited.

The implementation of IFCL in the code as an external input relies on the assumption of a predetermined current path for the induced currents within the strands, which produces an equivalent magnetization in the conductor [2]. The details of this approach are shown in the following.

When multifilamentary superconducting strands are exposed to varying magnetic fields, interfilament coupling currents (IFCC) flow through the normal conducting

matrix in an attempt to oppose the magnetic field change. In such a case, the total magnetic field in the interior of the strand (B_s) equals to the sum of the applied (B_a) and induced (B_i) magnetic fields:

$$B_s = B_a + B_i \quad (3.9)$$

The relationship between them can be obtained in the form [24]:

$$B_i = -\tau_{if} \frac{dB_s}{dt} \quad (3.10)$$

Where τ_{if} is the time constant of the IFCCs, defined by:

$$\tau_{if} = \frac{\mu_0}{2\rho_{eff}} \left(\frac{l_f}{2\pi}\right)^2 \quad (3.11)$$

with μ_0 the magnetic permeability of vacuum, l_f [m] the filament twist-pitch and ρ_{eff} [Ωm] the effective transverse resistivity of the strand matrix.

Combining Equation 3.9 and Equation 3.10, the total magnetic field in the interior of the strand is obtained:

$$B_s = B_a - \tau_{if} \frac{dB_s}{dt} \quad (3.12)$$

This first-order linear ordinary differential equation can be solved to calculate the total magnetic field in a strand under the effect of IFCCs. The interfilament coupling loss ($[W/m^3]$) on the normal conducting matrix is then calculated using the equivalent magnetization approach proposed by Wilson [2]:

$$P_{IFCL} = \left(\frac{l_f}{2\pi}\right)^2 \frac{1}{\rho_{eff}} \left(\frac{dB_s}{dt}\right)^2 \quad (3.13)$$

The addition of these equations in the MQXF thermal-electric model has been done preserving the homogenized conductor approach. The differential equation (Equation 3.12) is solved for every conductor at the end of each time step using the average magnetic field for each cable (in other words, the cable is considered as an equivalent strand). The loss is ultimately scaled using a geometric factor that

accounts for the total modelled area of the conductor (A_c) and the real total area of strands (A_s):

$$P_{IFCL-FEM} = \left(\frac{A_s}{A_c}\right)P_{IFCL} \quad (3.14)$$

As explained in [97], this formulation has the important shortcoming of not correctly simulating the energy flow inside the system. IFCL are considered as an external heat input, whereas in reality this energy is subtracted from the magnet's energy affecting the differential inductance. It has been concluded that in the case of a quench protected with just quench heaters the error is small [95].

3.3 Model Validation

The validation of the proposed 3D FEM quench simulation approach has been performed thanks to the comparison of the model predictions with two different types of experimental results.

On the first instance, the capability of the model to reproduce three-dimensional quench propagation was assessed by means of a special test referred as "the cable experiment" [192]. In such a experiment, normal zone propagation velocities are measured using the resistive voltage signals obtained from a Nb₃Sn two-cable assembly. Besides the simplicity of the setup, the controlled nature of the tests turns this experimental data into a perfect test-bed for the validation of our model.

The second type of validation studies correspond to the comparison of the model results with the signals obtained during the cold tests of various MQXF superconducting model magnets. In this case, the exercise allows to confirm that the model simulates with the necessary precision the full magnet discharge and the effects of the protection system.

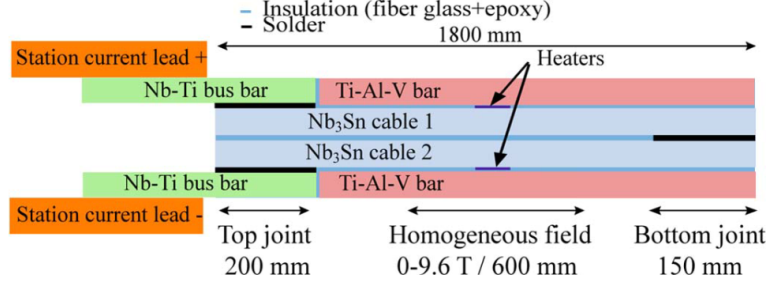


Figure 3-8: Experimental cable assembly setup. Schematic extracted from [192].

3.3.1 The cable experiment for quench propagation

In the framework of the HL-LHC magnet development activities, the test campaign carried out in [192] studies the longitudinal and transverse quench propagation phenomenon in the Nb₃Sn MQXF conductor. The ultimate objective of the study is the experimental assessment, under representative conditions of the magnet operation, of the normal zone propagation velocities in both directions⁸.

Fundamentally, the tests are based on the triggering of provoked quenches at different background magnetic fields and operating currents. A cable assembly composed of two impregnated conductors (See Figure 3-8) is used to this end. The quenches are induced by means of Thermofoil heaters, which are glued to the insulated cables in their central position. To ensure that the heat input is close to the minimum required quench energy, the thermofoil pulse is gradually increased until the quench energy is reached. The cables incorporate a thorough electrical instrumentation layout using voltage taps, which allows for the detailed study of the process. Figure 3-9 shows the resulting segments and their length. Finally, it is important to emphasize that the operating current for the cables and the background magnetic field is kept constant during each full test (no current, nor field decay).

The top plot in Figure 3-10 shows the typical outcome of a provoked quench event. The test starts by introducing a current pulse in the thermofoil heater, which results in a local heat source next to the cable insulation. Transverse heat conduction causes

⁸The quench propagation velocity constitutes an important parameter for the design of the magnet protection system and for quench localization. As an example, the distances between the quench heater stations are determined based on this information.

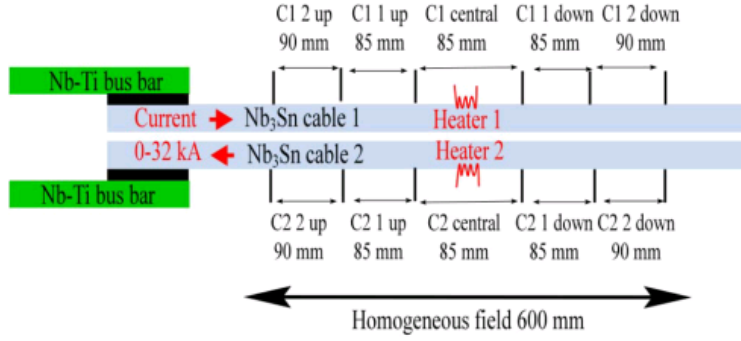


Figure 3-9: Detailed position of the voltage taps and the corresponding conductor segments. The homogeneous field region is indicated by the black arrow. The figure has been extracted from [192].

the temperature of the central part of the cable to increase until the critical surface is reached. At that point (quench onset), the quench starts to develop within the cable. Due to the constant current and magnetic field, the quench front travels initially along the cable in longitudinal direction, being purely determined by the temperature distribution. After approximately 9 ms, the figure shows that the quench has fully propagated in the first segment of voltage taps and enters the following one. The visible change in the slope for the first voltage curve corresponds to the quench front reaching both ends. Once fully quenched, the resistive voltage growth in the segment is purely determined by the temperature variation. After 12 ms, consistent with the thicker insulation thickness between cables, the quench starts as well in the parallel conductor and develops following the same mechanisms.

The quench propagation velocities are thus obtained using the distance between voltage taps and the time needed to reach them. The so-called turn-to-turn quench delay is instead defined as the time gap (from the onset in the first cable) required to quench the second cable by transverse heat conduction across the insulation.

Model setup

A detailed representation of the experimental setup was implemented in the 3D FEM model (Figure 3-11). Only the homogeneous field region is taken into account. The simulation includes the Titanium bars at both sides of the cables and the thermofoil

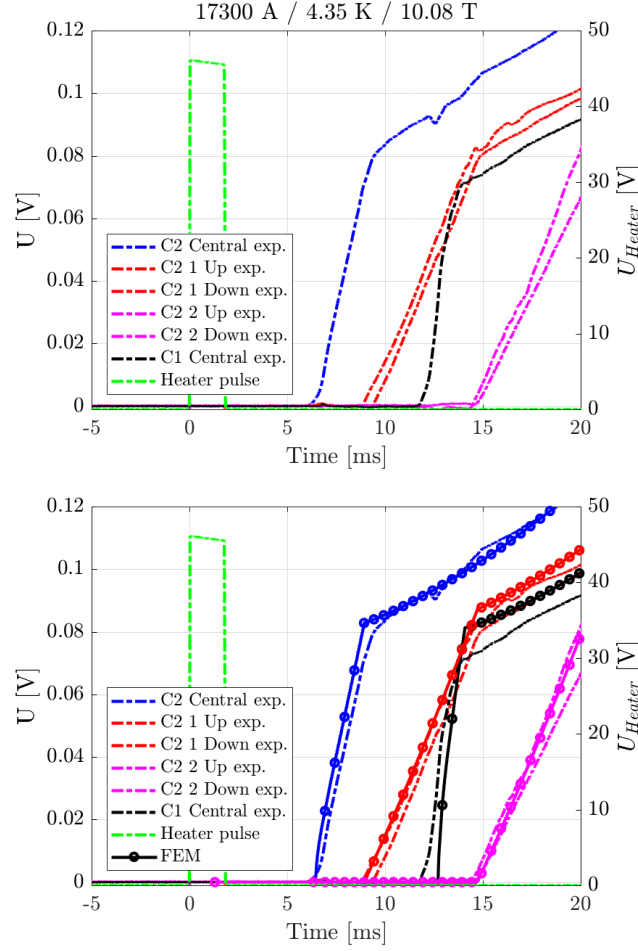


Figure 3-10: Comparison of experimental results, extracted from [192], with FEM simulations. The top plot shows only the signals from the experiment, while the bottom one superposes the results from the 3D model. The naming of the different segments corresponds to Figure 3-9.

heaters glued to the cables. The latter are powered with a heat source, which is calculated to match the initial quench delay of the first cable.

In view of the constant behavior of the magnetic field and the current, no decoupled magnetic nor circuit models are needed. The values of the peak and average magnetic field reported in the experiment are used in his stead⁹. The parameters for the critical surface parametrization of the conductor are also extracted from [192].

The spatial and time discretization details were defined based on a convergency study for the model. Figure 3-12 shows the voltage and temperature growth for the

⁹All the values reported in the text and figures of this subsection correspond to the average magnetic field in the cable cross-section.

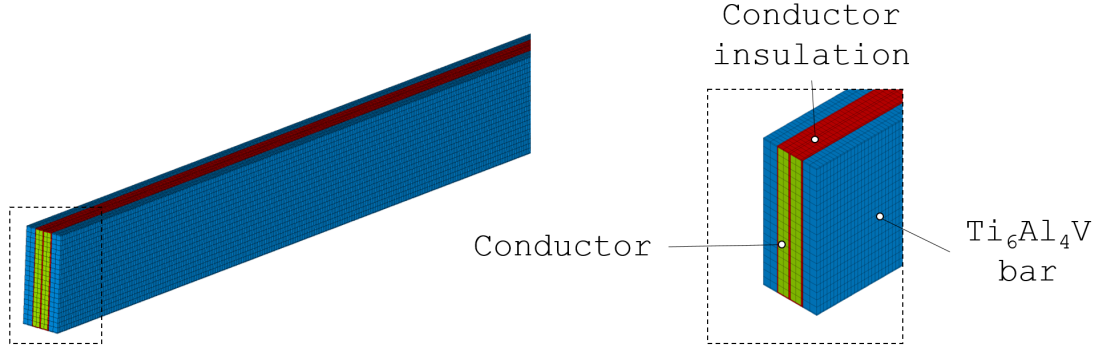


Figure 3-11: Components of the modelled cable assembly.

first central cable section at 4.35 K, 17.45 kA and 10.08 T, which corresponds to the highest longitudinal quench propagation velocity measured. Converged solution is obtained with a time step size of 10 ms and a longitudinal element size of 0.5 mm (in transverse direction the number of elements corresponds to the number of the strands in the cable, 40).

Simulation results

The results from three of the simulations are compared to the measured experimental signals in Figures 3-10 to 3-14. As for the real test, a pulse in the thermofoil heater glued to cable 2 originates the quench in its central section (Blue signal). Initially, the quench propagates in longitudinal direction, entering the following segment of voltage taps (Red). As a result of transverse heat conduction across the insulation, the quench is also finally initiated in Cable 1 (Black curve). Local pre-heating increases slightly the longitudinal propagation velocity in the newly quenched cable, while the symmetric propagation continues in cable 2 until reaching the last segment (Magenta curve).

The effect of pre-heating is seen and well represented by the model during initial quench onset on both cables. The energy dissipated in the heater increases the temperature of the conductor in contact over the heater length, till the quench occurs. In a similar way, the heat dissipated in cable 2 pre-heats the central section of cable 1, prior to quench initiation. The resulting faster propagation can be seen in the voltage

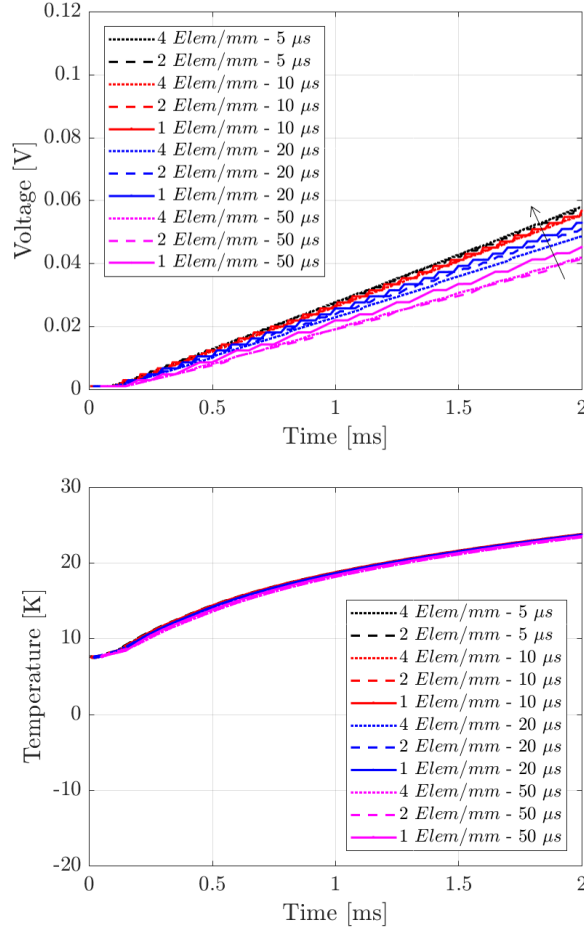


Figure 3-12: Spatial and temporal discretization study for the test at 4.35 K, 17.45 kA and 10.08 T. Converged solution is practically obtained with a time step size of 10 μ s and a longitudinal element size of 0.5 mm

slopes for both segments (Blue and black signals). Additionally, a sharp voltage rise develops in the simulation during initial onset for cable 1 and cable 2. This behavior contrasts with the measured superconducting to normal transition, and can be explained by the assumption made on neglecting the current sharing regime.

The model captures accurately the longitudinal component of propagation, and hence, the resistive voltage rise on each segment. For the quench at 17.3 kA, 4.35 K and 10.08 T, a propagation velocity of 15.5 m/s is measured, compared to 15 m/s obtained in the simulation. In the case of the quench at 13 kA, 4.35 K and 9.96 T, the model and experimental results are 8.5 m/s and 8.3 m/s respectively. The last simulation performed at 1.9 K shows an experimental propagation velocity of 9.5 m/s

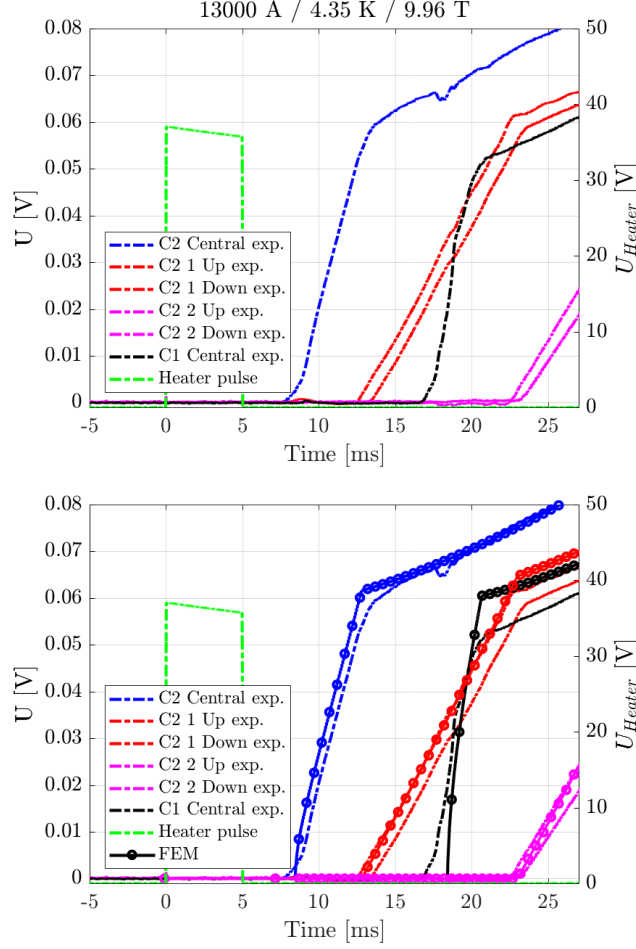


Figure 3-13: Model and experimental results comparison for the test at 13 kA, 4.35 K and 9.96 T.

and a computed one of 10 m/s.

In terms of the transverse cable-to-cable propagation, the model generally predicts larger delays than those obtained experimentally. For instance, the calculated 6.2 ms contrasts with the measured 5.5 ms in the test at 17.3 kA, 4.35 K and 10.08 T. In the second test, 13 kA, 4.35 K and 9.96 T, a measured delay of 10 ms compares with a computed one of 9 ms. At last, the best agreement is found in Figure 3-14 (17.45 kA, 1.9 K and 8.49 T), with a experimental value of 10.6 ms and a simulated result of 10.4 ms.

The normal zone propagation velocity can also be plotted as function of the magnetic field as shown in the plots of Fig. 3-15. In the preparation of this Figure, the model was used to simulate most of the cases available experimentally. The simulation

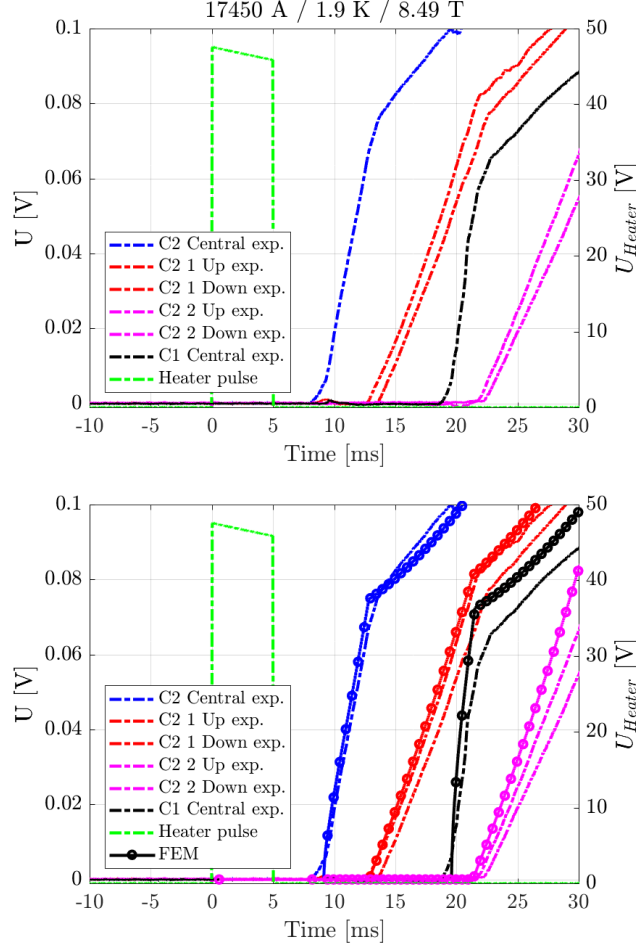


Figure 3-14: Comparison for the signals obtained from the third experiment at 17.45 kA, 1.9 K and 8.49 T.

fits within 4% the results at high magnetic field and 4.35 K, while it overestimates by $\approx 13\%$ the velocity for the same range at 1.9 K. The reason behind this difference is thought to be the change in material properties. For some materials, they need to be extrapolated from 4.35 K to 1.9 K.

Comparison with Analytical Formulas

The results have also been compared to simplified analytical predictions of the normal zone propagation velocity (NZPV) in LTS superconductors. These predictions are derived assuming that the transition boundary during a quench can be described by a translating coordinate system moving at NZPV in adiabatic conditions.

One of the most popular analytical expressions, firstly proposed by Whetstone-

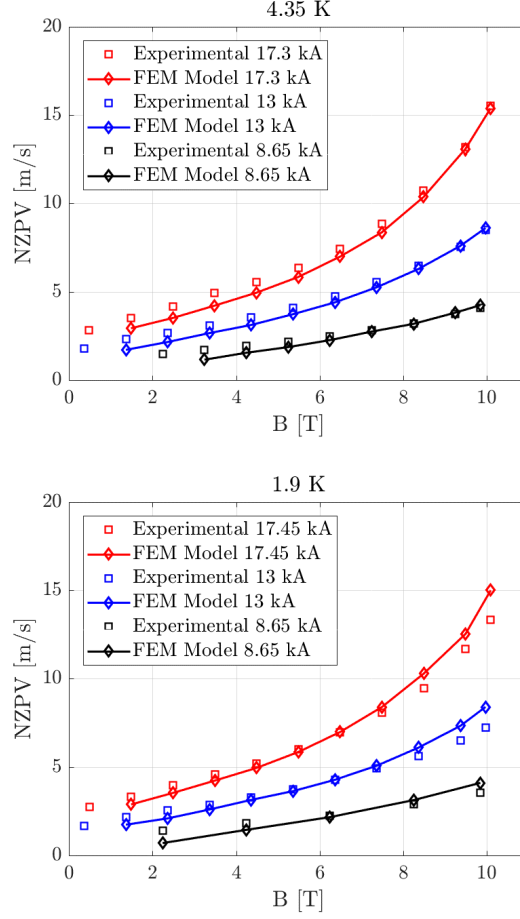


Figure 3-15: Quench propagation velocity as a function of the magnetic field. As for the previous plots, the experimental data has been extracted from [192].

Roos [193], can be modified for stabilized conductors to take into account current sharing effects [194]. Additionally, it is further simplified by neglecting the temperature dependance of the properties. Assuming Wiedemann-Franz law for ρ and κ [195] and considering a homogenized conductor's C_p , it can be written as follows:

$$NZPV \approx J \left[\frac{L_o T_t}{\int_{T_{Op}}^{T_t} C_{av} dT \left(C_{av}(T_t) - \frac{1}{T_t} \int_{T_{Op}}^{T_t} C_{av} dT \right)} \right]^{1/2}$$

On the other hand, in his book [2], Wilson proposes to evaluate C_p as an average value in the range T_{op} - T_c , resulting in:

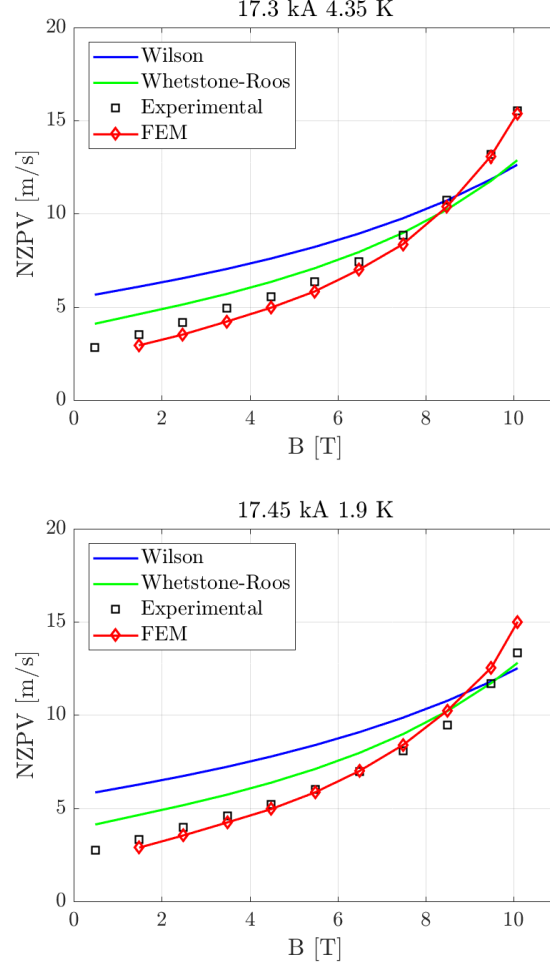


Figure 3-16: Comparison of test results with the FE model and analytical formulas. The average magnetic field takes into account the cable self-field component.

$$NZPV \approx \frac{J}{C} \left[\frac{\rho \kappa}{T_t - T_{Op}} \right]^{1/2}$$

The values obtained from both approximations are compared to the FE model using the tests performed at 17kA. As it can be seen in Figure 3-16, the simplified formulas give a higher NZPV at low fields, corresponding to a purely adiabatic unidirectional propagation. The finite element model simulations account for the important evolution of the material properties with temperature and the transverse heat propagation, describing better the experimental results. At high magnetic fields, the FEM simulation predicts higher propagation velocities, also in good agreement with

the test outcome.

Conclusions

Based on the results obtained, it is possible to conclude that the proposed simulation scheme is able to reproduce accurately (within 13 %) the three-dimensional quench propagation in Nb_3Sn superconductors. The model provides a very informative 3D description of the different magnitudes of interest along the cables (Temperature, voltages, delays to quench, propagation velocities, etc).

3.3.2 The MQXF short model magnet test campaign

The second stage for the validation of our simulation strategy corresponds to the comparison of the model results with experimental data from the cold test of MQXF model magnets. Compared to our previous exercise of pure quench propagation, the full magnet discharges add to the computation the effects of the varying electromagnetic field and the magnet electric circuit.

Three different FEM geometries were constructed for the simulation of the MQXF short models (Figures 3-17 to 3-18):

- (i) **3D octant model for the magnet straight section:** The geometrical description of the magnet profits from the main symmetry planes to reproduce just a fraction of the total cross-section (one octant). In addition, the coil end effects are neglected and only a part of the straight section is modelled (the model expands over 1 mm in longitudinal direction, thus becoming very similar to other 2D approaches available in literature). The total magnet resistance is obtained by scaling the results using the total length of each conductor. Longitudinal quench propagation is therefore not taken into account, which results in a fair assumption for the simulation of quench events where the protection system is able to quench rapidly the complete cable turn¹⁰. Regarding the protection, the

¹⁰The protection system has been designed for the MQXF magnet in order to quench the full conductor segment between two heater stations in less than 5 ms at nominal current. As a result, the magnet discharge can be well approximated by neglecting the longitudinal quench propagation component.

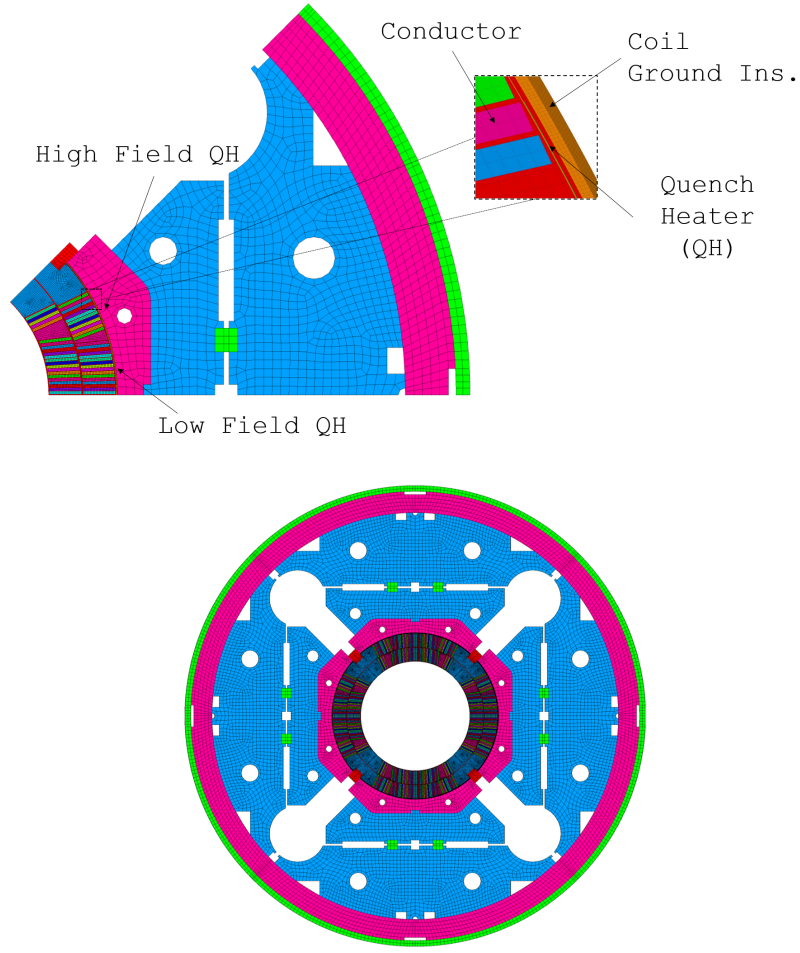


Figure 3-17: MQXF Thermal-Electric FE models for the magnet straight section. The position of both quench heater stations is indicated, corresponding to the high field and low field outer layer conductor blocks.

quench heater system is included in the model. Specifically, the heating stations are meshed as stainless steel volumes and powered using the relevant quench heater circuit parameters (See Table 2.6 in Chapter 2).

(ii) **3D full model for the magnet straight section:** This second model presents identical features as the previous one, but accounts for the full magnet cross section. Unlike the octant model, it can be used to simulate non-symmetric quench and failure cases.

(iii) **3D octant model for the superconducting coil:** At last, the complete

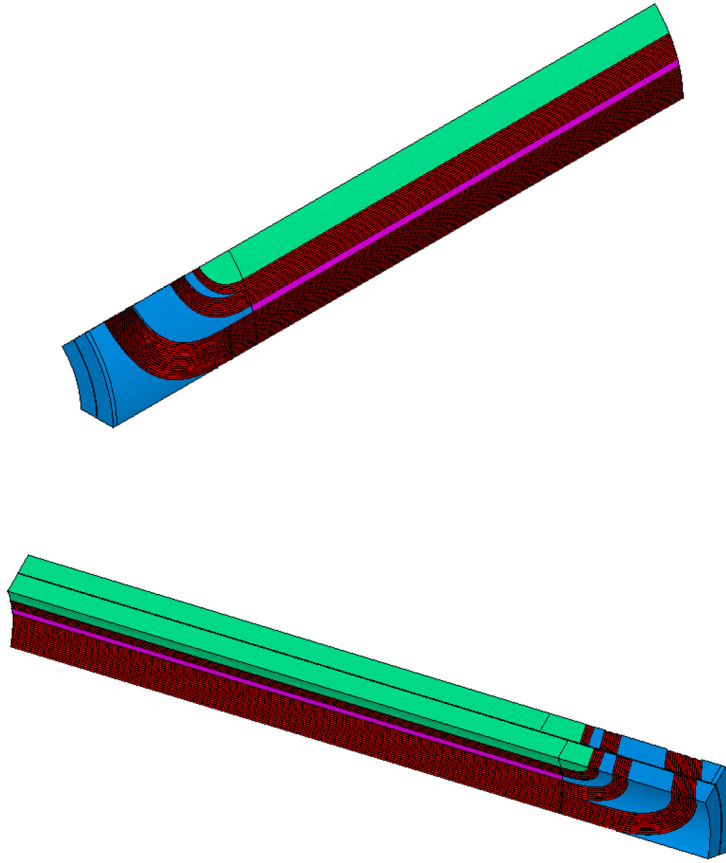


Figure 3-18: MQXF Thermal-Electric FE octant model for the full coil. Volumes are shown. The insulation has been hidden for visualization purposes. The division between the conductor straight section and the curve ends is visible in both layers.

three-dimensional model covering the full length is built for the coil geometry. The reasons behind omitting the magnet structure will become clear along next lines. This particular model incorporates the effects of longitudinal quench propagation and provides all the necessary 3D information for the magnet mechanical model.

Outcome of the validation campaign

For the validation of the different MQXF models, the so-called magnet protection tests are employed. These experiments, which are usually performed during the project

development phase, aim at studying the detailed magnet response during a quench. They encompass for example, the study of the magnet quench integral at different levels of current or, for quench heater protected magnets, the study of the time delays needed by the quench heater system to quench the coils.

All together, the magnet protection data offers a perfect set of information for the validation of a quench simulation code. It should be emphasized that in this type of experiments, the quench transient is provoked by manually triggering the magnet protection. Thus, the signals obtained are clean and uncertainties as the quench location are removed from the study (in comparison with a standard magnet quench).

The comparison between simulated and experimental results can be split in two different groups:

1. Regarding the validation of the magnet thermal behavior, the model can be compared to measurements in terms of quench heater delays. The delays are defined as the time needed for the quench heater system to quench the first turn below the stations, and provide a good estimation of how accurately heat conduction across the insulation is captured by the model. The real test procedure is simple: first, a ramp in the magnet current is performed up to the desired level, and second, a single quench heater strip is manually fired (while the rest of the protection is kept at nominal parameters). The resulting thermal transient originates the quench in the conductors below the strip, and the time delays can be clearly studied with the magnet voltage signals.
2. On the other hand, for the validation of the complete electromagnetic transient, the simulations can be compared to the experimental signals measured during the quench integral (QI) tests. This last comparison not only provides important information on how the model matches the quench propagation from turn to turn, but also confirms the validity of the chosen material properties. The test procedure is equivalent to that of the previous case, with the exception that in QI tests all the quench heater circuits are manually fired. Straightforwardly,

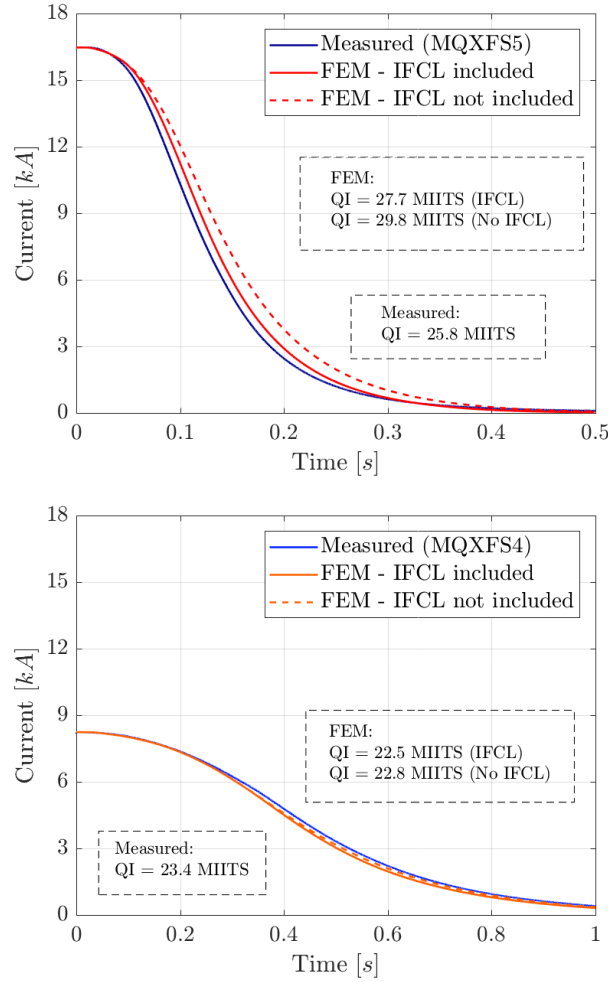


Figure 3-19: Measured and computed current decay in two different quench integral tests performed for MQXFS5 and MQXFS4 magnet. The effect of the IFCL in the current discharge is clearly visible at high current.

the quench integral obtained from this exercise differs from a real quench in the initial quench propagation. The latter occurs in the magnet before the quench is detected and the protection system activated. In first order, knowing the results of the QI tests, one can add the additional MIITs happening at constant current to obtain a conservative estimation of the quench load in a standard event. The aim of these tests is therefore to evaluate the performance of the magnet protection and to prove the correct behavior of the system in case of a quench.

If we neglect the small differences in the conductor properties between the four

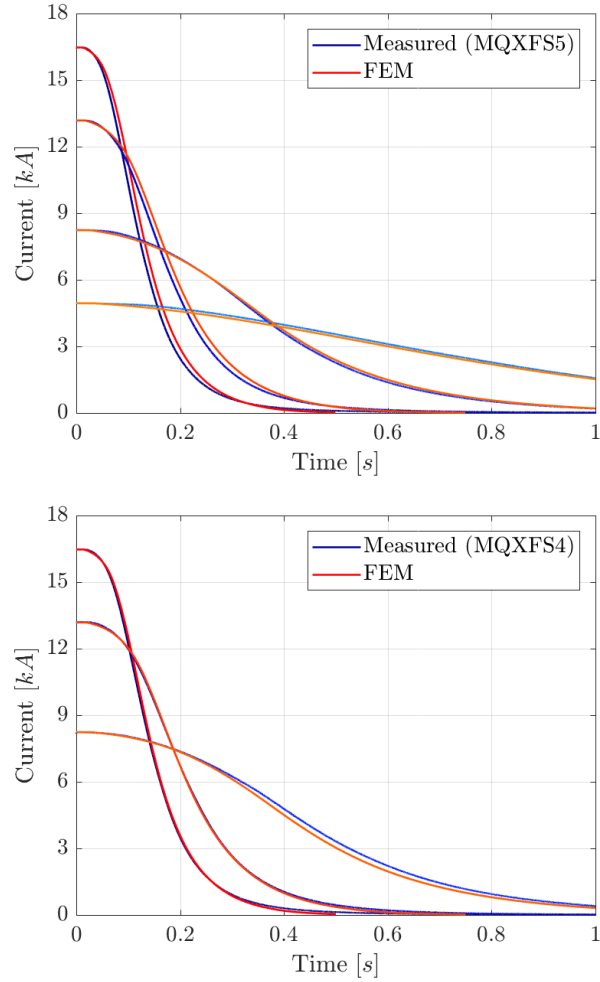


Figure 3-20: Comparison of the model predictions with experimental signals for different controlled quench discharges in two MQXFS magnets. The top plot shows the case of MQXFS5, while the bottom plot shows the one of MQXFS4.

coils, the simulation of the QI tests is clearly a symmetric case. Similarly, since only the quench delay is of interest, the first type of test can be also simulated in a magnet octant. Therefore, all the results shown next are obtained with the 3D octant model of the MQXF straight section.

Figure 3-19 shows the simulation results for two different quench integral events in MQXFS5 and MQXFS4 magnets. The bottom picture depicts a 8 kA discharge in the model magnet MQXFS4, using the outer layer quench heaters. Two simulation results (including interfilament coupling losses and neglecting them) are reported to highlight the importance of transitory effects in the magnet. Due to the slow rate of

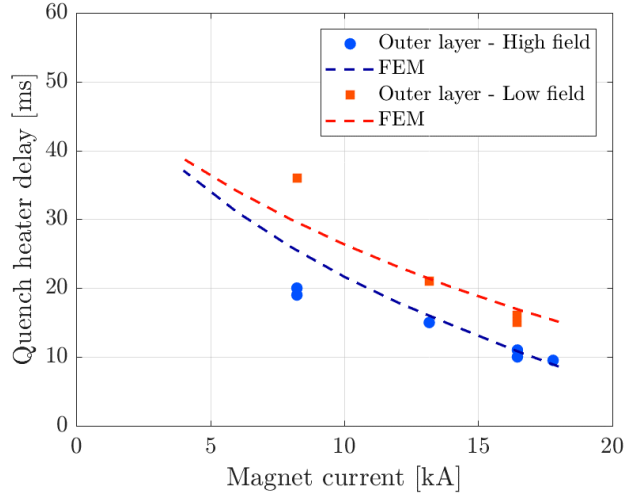


Figure 3-21: Measured and calculated delays when firing the OL-HF and OL-LF units respectively. Markers show the experimental values obtained during the test of MQXFS4 magnet. The dashed line represents the results obtained with the Finite Element Model.

change of the magnetic field, the impact of IFCL is negligible for the 8 kA discharge. The computation predicts a quench integral of 22 MIITS compared to a measured value of 23.4 MIITS. The difference can be explained in terms of longitudinal quench propagation. The code neglects propagation and assumes the full conductor length quenched at the same time, leading to a 6 % underestimation of the quench integral. On the contrary, Figure 3-19 - Top illustrates a 16 kA quench integral test for a magnet with a low RRR value (< 90). The fast current decay and quench propagation implies a fast varying magnetic field, and the effect of IFCL is clearly matched by the model. The simulation output shows a quench integral of 27.7 MIITS with respect to the measured value of 25.8 MIITS, i.e. 7 % difference. Consistently, very similar results to the ones of the finite element model have been obtained with other quench simulation codes at CERN, where none of them were able to reproduce the low measured value of quench integral [196]. Variability between the conductor properties of the four coils, or uncertainties in the critical surface fit may be the cause behind this discrepancy. The full set of simulations for MQXFS5 and MQXFS4 magnets are shown in Figure 3-20, confirming that the model is capable of reproducing the measured results with a precision of 8 %.

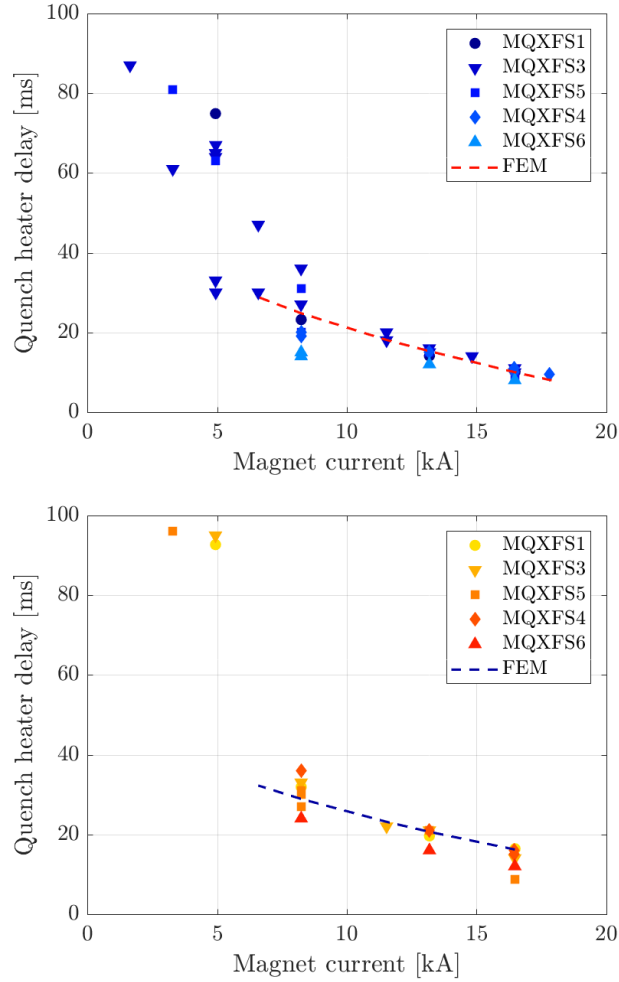


Figure 3-22: Delays to quench measured from the moment the heaters are powered. Markers show the experimental results, and the dashed lines depict the values obtained from the simulation. The bottom plot corresponds to tests performed in the low-field block, the top to the high-field one.

In the case of the tests focused on the quench heaters efficiency, the model also catches the test response. Here, the experiments are performed both for the high-field and low-field quench heater strips. As an example, the results from the MQXFS4 magnet test campaign are reported in Figure 3-21. The best agreement between the model prediction and the measurements is found at nominal levels of current, both for the high field and low field cases. The computation is able to predict the delay to quench in this range of current with a precision of 1 ms. When the margin to quench is bigger, the validity of the adiabatic approximation is reduced, and longitudinal heat

transfer greatly increases the delays to quench. This is not captured by the model due to its reduced longitudinal dimensions. It is also important to note that determining the experimental delays is usually a subjective exercise, and error margins of the order of some ms can be expected when delays are reported. In case low current levels can be of interest, the model could ultimately be extruded to cover a full quench heater period.

Continuing with the quench heater delays, the results from all the tests performed up to the date are summarized in Figure 3-22. Since the various short model magnets account for slightly different conductor properties that fluctuate around the reference project values, the results are compared with a simulation performed using the reference MQXF conductor. The simulation maintains the prediction of the delays within a discrepancy of 1 ms at operational current (high-field QH), and confirms that the code is able to reproduce the relevant thermal physics in the magnet.

Adiabatic conditions for resin impregnated coils

All the results presented have been obtained, as already mentioned, considering adiabatic boundary conditions in all external surfaces of the model. The validity of this approximation could be also confirmed thanks to the quench integral tests performed in the short model magnets.

Figure 3-23 compares the average coil temperature, inferred from the QI tests through the copper resistivity, with a simple analytical adiabatic estimate [102]. The analytical curve reassembles to that shown in Figure 2-14 with inverted axis. Instead of Γ , the integral of C_p is evaluated as the conductor enthalpy. The good agreement between the experimental and predicted adiabatic values confirms that, for impregnated systems, the stored energy released during a quench protected with only QH is absorbed by the coil enthalpy in a closely adiabatic process.

Consistently with this finding, the last important outcome of the model validation is linked to the temperature of the magnet structure. The simulations confirm that due to the fast nature of the transient and due to the coil insulation, the structure remains ultimately cold through the full quench discharge (See Figure 3-24, which

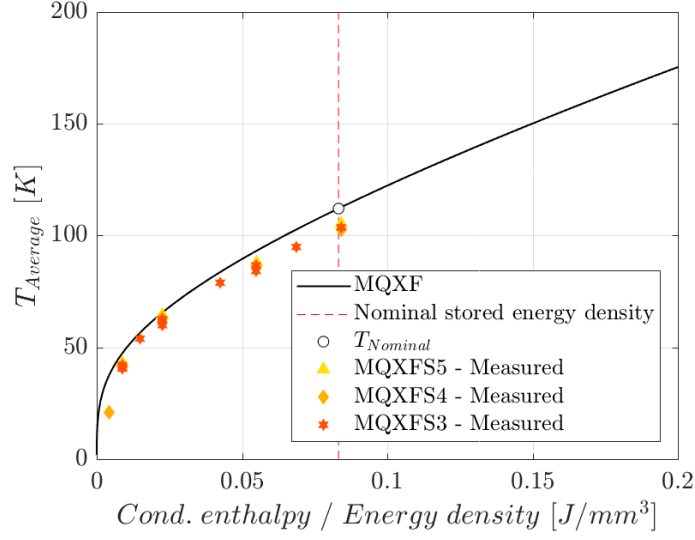


Figure 3-23: Average coil temperature as a function of the magnet stored energy density or conductor enthalpy. The solid line corresponds to the analytical adiabatic estimate and the markers show the experimental extracted values from quenches protected with only QH. The good agreement between both confirms that the released stored energy is, in practical terms, completely absorbed by the coil enthalpy.

shows a quench integral test at nominal current).

Including the 3D full coil geometry

Up to this point of the chapter, the complete 3D magnet geometry (including the coil ends) has not been yet employed. The use of the model, which is one of the novelties of our work, is finally introduced here profiting from the interesting result of last paragraph.

One of the main consequences of the adiabatic coil behavior is that it becomes possible to remove the magnet structure from the complete quench description. Attending to this simplification (which decreases remarkably the computational efforts required to solve the problem) the FEM domain can be just reduced to the superconducting coils. The reason by which only the 3D coil geometry was accounted in the third proposed model has now been unveiled.

Furthermore, the removal of the magnet structure is not the only modification with respect to the straight section models. To improve the mesh quality, the quench

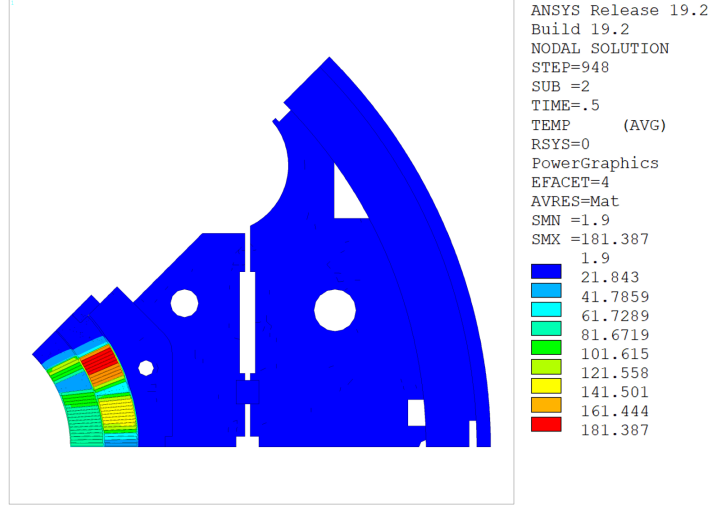


Figure 3-24: Coil and magnet structure temperature after a quench integral test at nominal current. As it can be seen, due to the fast nature of the transient and the coil insulation, the magnet structure remains cold through the complete discharge.

heater stations are also deleted from the 3D coil model. Although the stations could be included by adding thermal contacts in the coil to heater interface, it has been already proven that the impact of the quench propagation happening between stations is very low at high levels of current.

In absence of the meshed stations, the effects of the quench protection system is accounted by imposing the quench state of each conductor according to a given delay. Ultimately, the delays are obtained from the straight section model¹¹. Let's imagine, for instance, the conservative case where the simulation of the magnet quench integral at a certain current level is performed using the octant 1 mm-long model. The computation will predict the quench delays of each turn, in absence of initial quench propagation. We can then read this information in the full 3D coil model, prior to initiate a quench at the same current level. By fixing a detection + validation time ($t_{det} + t_{val}$) for the protection system, the quench will propagate initially without constraints. When the simulation time reaches the established quench delays for each cable (consistently shifted according to $t_{det} + t_{val}$), the conductor state is updated to

¹¹In practical terms, the 3D coil simulation requires two modelling steps. In the first, the quench is simulated using the straight section model (which incorporates the quench heater stations). The quench delays of each turn are hence obtained. In the second, the full 3D coil simulation reads this information and runs the final computation.

quenched along the complete length. This strategy, which is used for the simulations performed in this thesis, allows to include the effects of the protection system without modelling the 3D detailed heater geometry.

Additional validation of the 3D coil model

The results from the full 3D coil simulation and from the magnet straight section model are identical for the quench integral tests, as follows straightforwardly from the model development. An additional validation exercise can still be performed for the full coil model type. It corresponds to the verification of the model outcome regarding the initial quench propagation in a real magnet quench event.

With this objective, the model has been used to simulate the first milliseconds of a training quench occurred during the cold tests of the MQXFS4 short model. The quench was initiated in the straight section of one of the coils (C108), in the inner pole turn (captured by the voltage taps segment I7I8) and propagated through the layer jump (Segment I8O1) till the protection system was triggered.

Figure 3-25 compares the simulation results and the experimental signals for this specific case. During the initial propagation, the magnet current is almost constant, as it is the magnetic field, and hence, the resulting resistive voltages result to be very informative on the quench onset. The agreement between simulated and test curves can be quantified through the measured quench propagation velocity, found to be close to 18 m/s in both signals. The change in slope corresponds to the moment when one quench front exits the I7I8 segment, entering I8O1, and it is also well captured in the voltage derivative. The model also reveals the quench origin position. According to the computation, the quench starts 115 mm far away from the beginning of the layer jump segment, in the coil straight section.

Conclusions

Similarly to the cable experiment, the comparison of the model results with the experimental signals obtained from various MQXFS short models confirm that the simulation approach captures accurately the magnet behavior during the transient.

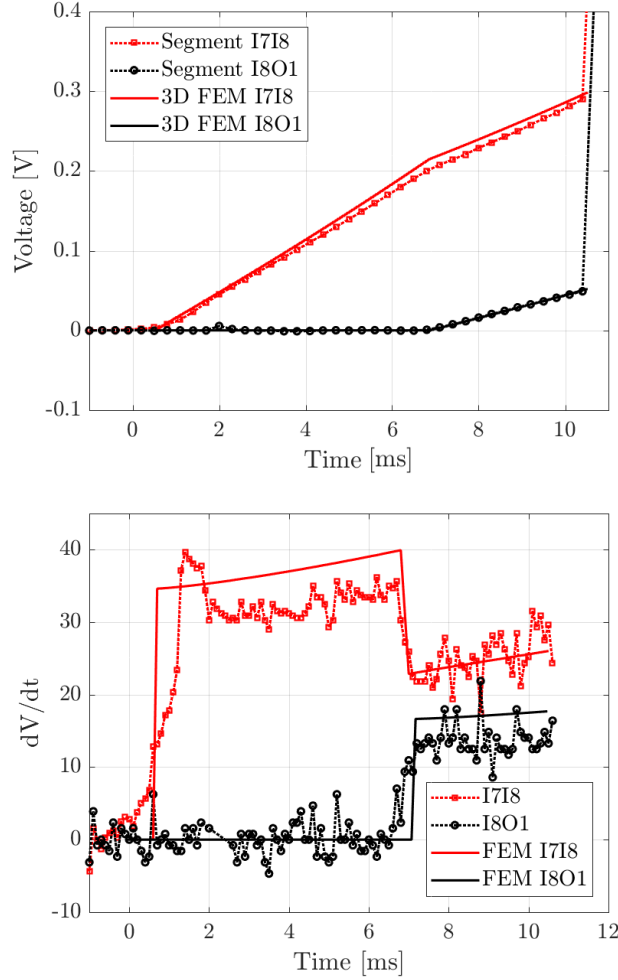


Figure 3-25: Top: Resistive voltage growth for pole turn (I7I8) and layer jump segments (I8O1). Dotted lines with markers show the measured data, solid lines represent the Finite Element Simulation. Bottom: Voltage derivative for both segments.

3.4 The reference nominal current quench case

We close this chapter by showing the 3D results of a MQXFS magnet quench happening at nominal powering conditions (with the exception that in our conservative case, the magnet is protected with only the quench heater system). Contrary to what was shown in the validation section, where specific MQXF magnets were simulated during protection tests, the analysis of this reference case focuses on the appearance of a spontaneous quench during operation. As a result, the full magnet simulation is required due to the non-symmetric nature of the event. While the 4-quadrant magnet domain is employed for the straight section analysis, only one half of a coil is mod-

elled using the complete 3D geometry. Such a choice will be clarified in next Chapter, based on the results of the mechanical model.

The goal of the study is to obtain the necessary description of the transient, for its later use in the mechanical model. The main simulation parameters are listed here:

- The conductor RRR is set to 150 and the copper to non-copper ratio to 1.2. The critical surface parameters are extracted from the reference the project specification.
- A total quench detection and validation time of 15 ms is considered for the quench heater system. This is in line with the response of the magnet protection observed during the short model test campaign.
- The quench initiates in the peak field conductor, thus resulting in the largest hot spot temperature. For the 3D full coil geometry, the origin is set to the magnet center.

The contour plots of Figure 3-26 present the temperature distribution at the end of the discharge using the straight section model (right) and the 3D coil model (left). Interestingly, the first computation shows a 6 K larger temperature for the hot-spot conductor. This is attributed to the presence of longitudinal heat propagation in the full coil, which is neglected in the thin simplification. The magnet is practically discharged after 515 ms, showing a maximum temperature at this point of around 285 K. Note that although the average temperature of the system is the largest, the peak value is slightly lower than the absolute maximum attained (due to heat conduction in the coil). The latter is close to 300 K and happens before the completion of the energy dump.

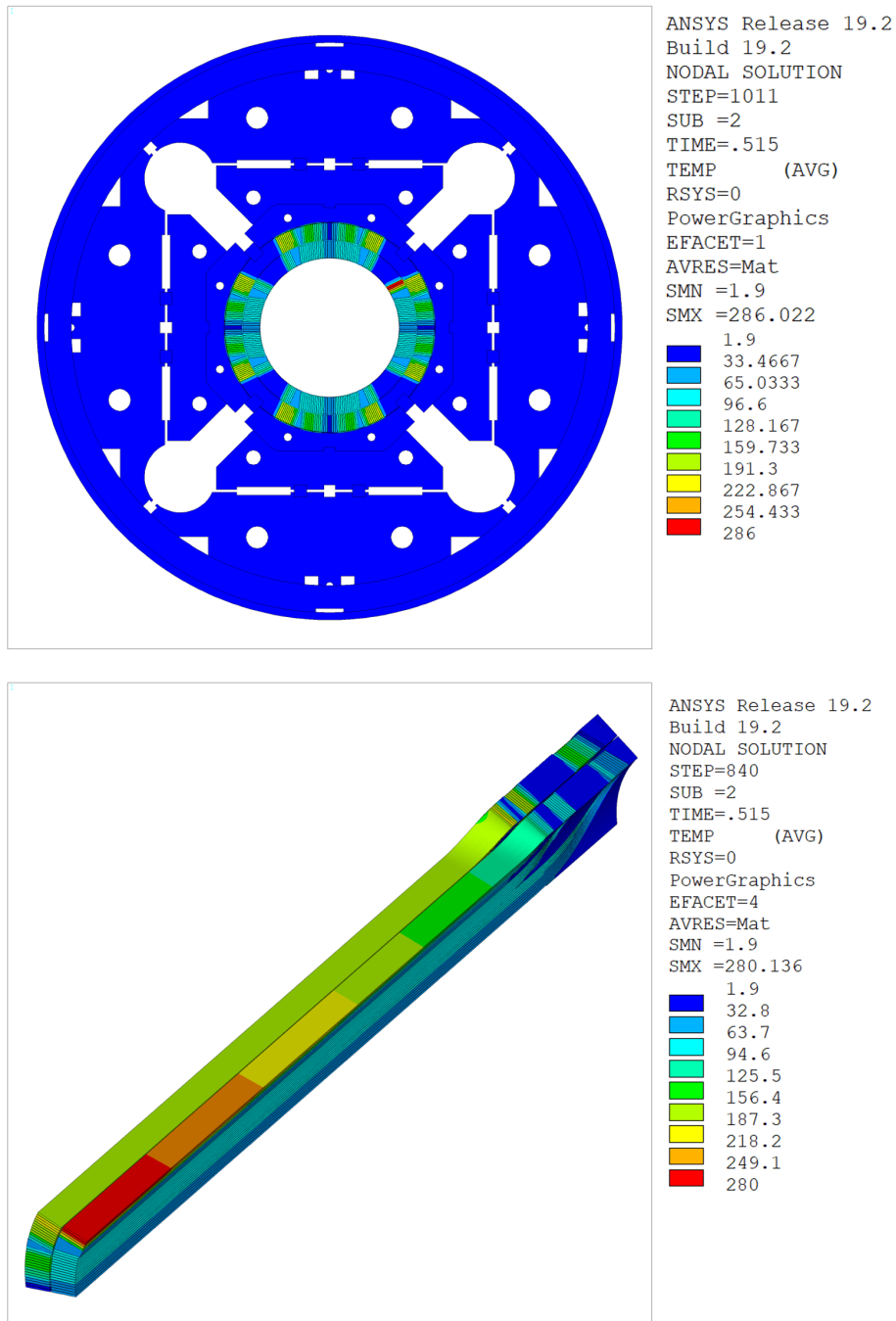


Figure 3-26: Temperature results from the simulation of a reference quench happening at nominal current. The bottom plot shows the 3D coil geometry, where the insulation and the winding pole are hidden for visualization purposes. The top plot shows the same simulation performed using the magnet straight section.

Chapter 4

Magnet mechanical behavior during a quench

In the following pages, we will analyze the two-dimensional and three-dimensional mechanical behavior of the MQXF magnet during a quench. The chapter begins with the description of the coupling mechanism that allows to incorporate the results from the quench simulation into the mechanical studies. After that short introduction, the 2D and 3D mechanical models will be described in detail. The results obtained concerning the simulation of the magnet mechanics during a quench will be validated next. To conclude, the reference quench case at nominal current is reviewed.

4.1 Load transfer

A multi-physics (or coupled-field) simulation can be defined as the combination of various analysis from different physics fields, which interact together to solve a global problem. As already explained for the case of quench transients, in this type of computations the solution of individual models depends on the results from their coupled counterparts.

In the same context, the study of the magnet mechanical behavior during a quench can be understood in terms of the coupling between a magnet structural model and a multi-physics quench analysis. Ultimately, the type of link between these two main

simulations will determine how the information is shared among them.

The term "one-way" is used to describe the coupling mechanism on which the mutual interaction between the two entangled fields is not needed. In other words, the results from a certain model are used as an input on its coupled simulation; however, no information is shared back to the first analysis. An example of this behavior is found in the well-known thermal stress problem. The temperature distribution in a structure induces a thermal strain field, whose study needs to be carried out using a mechanical model. However, the resulting mechanical strain does not affect the thermal solution and thus, no secondary iteration between the mechanical and thermal calculations is necessary.

Disregarding the effect of mechanical strain in the critical surface of the superconductor and neglecting potential frictional heat released in the magnet structure, the analysis of the transient stresses happening during a quench can be performed using this particular strategy. In consequence, it will be the approach adopted in this thesis. The same coupling method is widely employed in the superconducting magnet community for the transfer of the e.m. forces (computed with a magnetic simulation) to the mechanical model, as it will be explained later.

Taking a closer look to the uni-directional communication process between the multi-physics quench model and the mechanical one, this will be done based on the so-called load-transfer method. The latter relies on the separate solution of both FEM simulations, where the results of the quench part will be applied as external loads in the mechanical study¹. Since the two analyses are performed in the same software, the transfer procedure is handled by a simple read command between the model databases. Table 4.1 summarizes the main characteristics of our coupling strategy. The temperature degree-of-freedom and the magnet current will be the two variables taking part in the coupling process.

¹See Appendix C for an overview on the coupling strategies available in ANSYS APDL

Table 4.1: Coupling strategy for the structural analysis

Coupling type	One-way
Coupling method	Load-transfer
Detailed proc.	Read process between model databases

4.2 MQXF - Mechanical models description

Fully parametric two-dimensional and three-dimensional FE mechanical models have been built for the MQXF magnet in ANSYS APDL. Unlike the multi-physics quench simulation, which was completely constructed in the framework of our work, the magnet structural models were already developed at the moment of starting this thesis.

The same principles of the Finite Element Method (See Chapter 3 and Appendix B) apply to the analysis of the magnet mechanics. However, some important considerations (or modelling assumptions) need to be highlighted before further immersion into the structural problem:

- **Static structural analysis:** The mechanical simulation of the MQXF magnet will be performed based on a steady-state² analysis type. Any time-dependent load will be therefore approximated as a series of static loads, which will be applied through multiple (chronologically ordered) solution steps. Inertia and damping effects are disregarded in the computation.
- **Structural non-linearities:** In addition to the possible non-linear material behavior, the solution of the structural problem incorporates new causes for non-linearities (NLs), for instance, those coming from geometric effects (geometric non-linearities) or from the boundary conditions. On this topic, it should be noted that the presence of so-called contact elements in the MQXF finite element model will be one of the most important sources for NLs linked to our work.

These elements will be briefly covered in this introduction.

²A steady-state, or static, analysis calculates the effects of steady loading conditions on a structure, while ignoring inertia and damping effects (as those caused by time-varying loads).

Henceforth, as it has been done for the thermal-electric problem, we profit in the following subsections to present the theory fundamentals (including the just cited contact technology) and the main characteristics of both 2D and 3D structural models. Upon this general presentation, the simulation's architecture and the main load steps will be described. Lastly, few words covering the model validation are provided.

4.2.1 Theory basis

To understand the theoretical background behind the FEM application to structural studies, it results particularly enriching to review first the strong form of the 3D solid mechanics problem. To do so, we will limit ourselves to the simplest three-dimensional case available in most graduate-level textbooks. It corresponds to the example of an homogeneous, isotropic, linear elastic and static structural problem³.

Strong form

Let us imagine a three-dimensional solid on which certain boundary conditions are prescribed (and remind that through the next lines, our previous assumptions must be fulfilled). The deformation of the body can be determined by a displacement field $\{u\}$, which is a function of the position. Assuming a Cartesian reference frame, the displacement at any location can be expressed on its vectorial form,

$$\{u\} = \{u_x \ u_y \ u_z\} \quad (4.1)$$

where the components of $\{u\}$ are as well functions of the position.

The mechanical response of the solid is described, in addition to the displacement, by the stress and strain fields. On the one hand, the stress at a certain point of the solid can be defined as the force per unit area acting on the boundary faces of a small cubic element, which is centered at the point location. Distinguishing between the

³These assumptions imply that the Hooke's law is applicable for linear elasticity, the deformation is small enough such that one can assume a linear relationship between displacement and strains, there are no changes of topology, and that the slow nature of the deformation allows to neglect dynamic effects.

components in normal (σ) and shear (τ) directions to each of the faces, the stress state is defined by:

$$\{\sigma\} = \{\sigma_{xx} \ \sigma_{yy} \ \sigma_{zz} \ \tau_{xy} \ \tau_{yz} \ \tau_{zx}\} \quad (4.2)$$

where those components in opposite faces have the same magnitude and inverse direction, as follows from the force equilibrium in the element.

In what regards the strain, the latter is a geometrical definition which relates the displacement of a point relative to its neighbours. Similarly to the above-mentioned stress case, the strain state is determined by,

$$\{\varepsilon\} = \{\varepsilon_{xx} \ \varepsilon_{yy} \ \varepsilon_{zz} \ \gamma_{xy} \ \gamma_{yz} \ \gamma_{zx}\} \quad (4.3)$$

where ε_{ij} denotes the normal strain components and γ_{ij} the shear ones.

Equations 4.1 - 4.3 bring a total of 15 variables that can be used to represent the mechanical response of the solid at any point. Thus, an equal number of equations (governing equations) are required to obtain a well-posed solution to the problem.

Three of these equations correspond to the pertinent equilibrium condition ($\sum F_x = 0$, $\sum F_y = 0$ and $\sum F_z = 0$). Inside the structural body:

$$\frac{\partial \sigma_x}{\partial x} + \frac{\partial \tau_{xy}}{\partial y} + \frac{\partial \tau_{zx}}{\partial z} + b_x = 0 \quad (4.4)$$

$$\frac{\partial \tau_{yx}}{\partial x} + \frac{\partial \sigma_y}{\partial y} + \frac{\partial \tau_{yz}}{\partial z} + b_y = 0 \quad (4.5)$$

$$\frac{\partial \tau_{zx}}{\partial x} + \frac{\partial \tau_{zy}}{\partial y} + \frac{\partial \sigma_z}{\partial z} + b_z = 0 \quad (4.6)$$

Or on the boundary:

$$\sigma_x n_x + \tau_{xy} n_y + \tau_{zx} n_z + S_x = 0 \quad (4.7)$$

$$\tau_{yx} n_x + \sigma_y n_y + \tau_{yz} n_z + S_y = 0 \quad (4.8)$$

$$\tau_{zx} n_x + \tau_{zy} n_y + \sigma_z n_z + S_z = 0 \quad (4.9)$$

where b are the components of body forces, S the components of surface forces and n_i are the direction cosines of the unit vector n relative to the employed coordinate system.

Additional constitutive equations relate the stress and strain. In the simplest case of linear isotropic materials:

$$\varepsilon_x = \frac{\sigma_x}{E} - \nu \frac{\sigma_y}{E} - \nu \frac{\sigma_z}{E} + \alpha \Delta T \quad (4.10)$$

$$\varepsilon_y = \frac{\sigma_y}{E} - \nu \frac{\sigma_x}{E} - \nu \frac{\sigma_z}{E} + \alpha \Delta T \quad (4.11)$$

$$\varepsilon_z = \frac{\sigma_z}{E} - \nu \frac{\sigma_x}{E} - \nu \frac{\sigma_y}{E} + \alpha \Delta T \quad (4.12)$$

$$\gamma_{xy} = \frac{\tau_{xy}}{G}; \quad \gamma_{yz} = \frac{\tau_{yz}}{G}; \quad \gamma_{zx} = \frac{\tau_{zx}}{G} \quad (4.13)$$

In these equations, $G = E/2(1+\nu)$ stands for the shear modulus, ν for the Poisson ratio and α for the thermal expansion coefficient.

At last, kinematic relations account for the strain-displacement correspondence. Under the assumption of small deformations, the geometrical relationship remains linear:

$$\varepsilon_x = \frac{\partial u_x}{\partial x}; \quad \varepsilon_y = \frac{\partial u_y}{\partial x}; \quad \varepsilon_z = \frac{\partial u_z}{\partial x} \quad (4.14)$$

$$\gamma_{xy} = \frac{\partial u_x}{\partial y} + \frac{\partial u_y}{\partial x}; \quad \gamma_{yz} = \frac{\partial u_y}{\partial z} + \frac{\partial u_z}{\partial y}; \quad \gamma_{zx} = \frac{\partial u_z}{\partial x} + \frac{\partial u_x}{\partial z} \quad (4.15)$$

The set of 15 equations together with the boundary conditions of the solid define completely the state of stress, strain and deformation in the domain. Interestingly

enough, only few examples can be solved analytically using them, and hence, numerical approximations shall be pursued. The application of the FEM method to practical engineering cases is succinctly introduced in the next paragraphs.

FEM solution

The FEM solution to the structural problem relies (for most of commercial softwares, as ANSYS APDL) on the Principle of Virtual Work (PVW). The PVW is an expression of the energy conservation, which states that a virtual (very small) change of the internal strain energy must be offset by an identical change in external work due to the applied loads:

$$EVW = \int_V \{\delta \varepsilon^T\} \{\sigma\} dV \quad (4.16)$$

where EVW is the acronym of External Virtual Work and the right hand-side of the equation corresponds to the internal virtual work; $\{\sigma\}$ is the stress vector, $\{\varepsilon\}$ is the strain vector, δ is the virtual operator and V the element volume.

Equation 4.16 can be further developed, in part using the constitutive equations from last paragraphs, to achieve the FEM formulation in matrix form. Note that a comprehensive treatment of the derivation of the structural matrices is out of the scope of this section, however it can be easily identified that the principle of virtual work results in a set of equations that resembles to those seen in the weak integral form reviewed in the Appendix. The complete theoretical foundations for the ANSYS APDL model are found in the software's theory reference [180].

Contact technology

Contact elements⁴ are used to describe the mechanical interaction between two different regions of the problem domain. As their name indicates, these elements are placed in the surfaces (or lines for 2D problems) where two or more components of the model are expected to be in geometrical contact at any time during the simulation.

⁴This description is restricted to mechanical contacts. However, note that contact elements exist as well for many other physics fields.

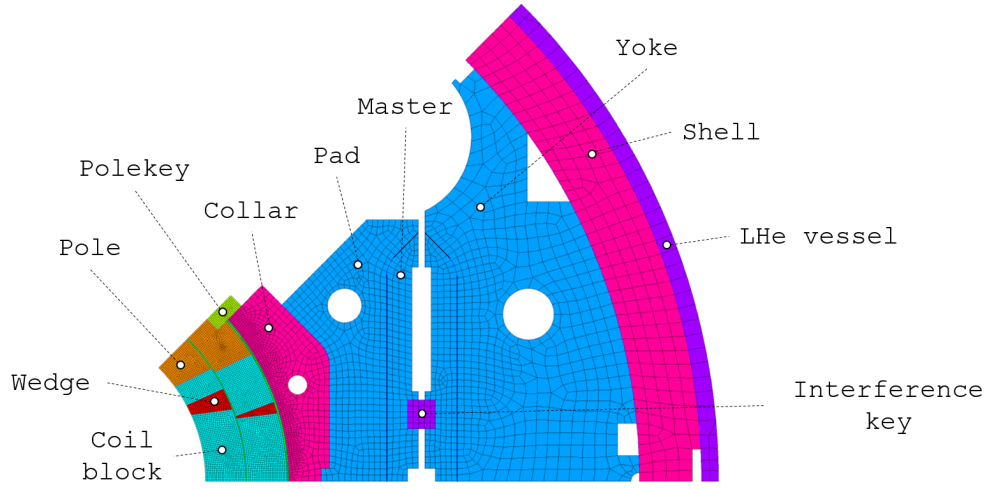


Figure 4-1: 2D octant mechanical model.

In structural analysis, contacts are widely employed to include frictional effects in the component interfaces, but also for the application of special boundary conditions. For instance, to simulate interference fits by introducing an offset displacement in the contact pairs. Restricting ourselves to the MQXF magnet models, both 2D and 3D contacts employ the Augmented Lagrangian formulation, whose main characteristics can be retrieved in [180].

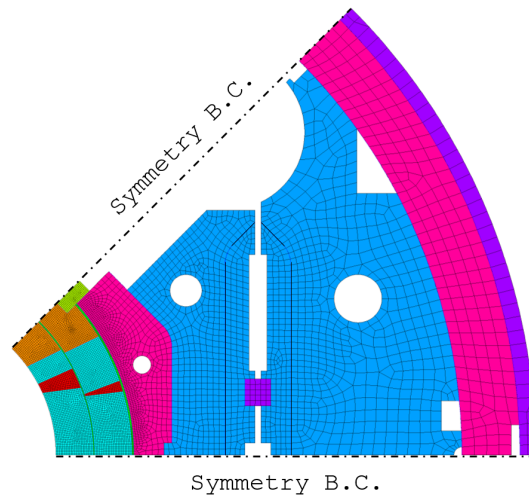


Figure 4-2: Boundary conditions for the 2D octant mechanical model.

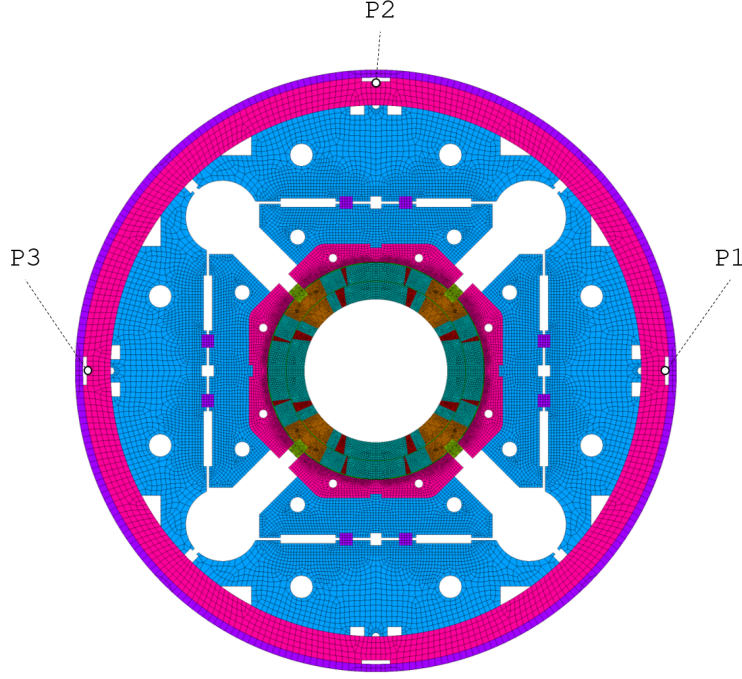


Figure 4-3: Full two-dimensional magnet model. The vertical displacement is constraint in P1, the horizontal in P2 and the rotation in P3.

4.2.2 Model geometries

As for the thermal-electric model, three different magnet geometries have been reproduced:

- (i) **2D octant model:** The reference two-dimensional model is octant symmetric azimuthally, as shown in Figure 4-1 and Figure 4-2. Boundary conditions apply to the main planes at 0° and 45° , where the displacement in normal direction is constraint. Thanks to the reduction of the problem domain, the computational efforts required to solve this model are the lowest among the three geometries.
- (ii) **2D full model:** The two-dimensional full model (Figure 4-3), however, does not benefit from the magnet symmetry and reproduces the complete cross-section. The necessary constraints are applied at three nodes of the FE mesh (in the outer radius of the magnet), covering the zero displacement in horizontal (P2) and vertical (P1) directions and the zero rotation (P3) conditions. Contrary to

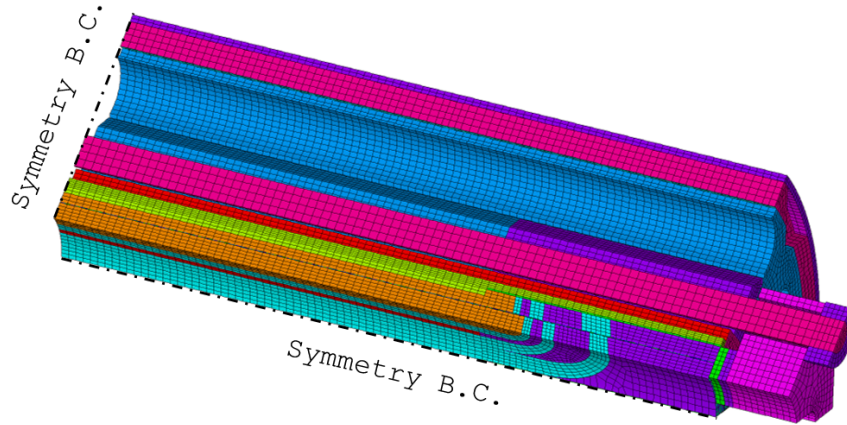


Figure 4-4: 3D octant mechanical model.

the previous model, the full geometry allows for the solution of non-symmetric load conditions.

- (iii) **3D octant model:** Finally, the 3D version of the mechanical model relies once again in the recurrent octant geometry (Figure 4-4). In the three-dimensional space, the reduction of the problem domain becomes critical to decrease the computational times. Normal displacement constraints apply in the planes at 0° and 45° , but also in the boundary face at $z = 0$.

For all the models, the conductor regions inside the coil are modelled as homogeneous solids of average material properties. These volumes, which include the superconducting cables, the impregnated fiberglass and pure epoxy regions, receive the name of coil blocks (see Figure 4-1).

4.2.3 2D Mechanical models

Both two-dimensional models (Figure 4-1 and Figure 4-2) use high order 8-node structural elements (PLANE183) for the mesh construction. In the interfaces between the different magnet parts, the ANSYS contact elements (CONTA172 and TARGE169) are introduced.

The presence of the contacts has the important role of incorporating the frictional behavior in between the magnet constituents, but also of allowing for the simulation of the pre-loading sequence (using the previously suggested interference offsets). The next definitions apply:

1. Frictional contacts with $\mu = 0.2$ are set for all the interfaces, excluding those inside the coil.
2. As a general approach, the coil components are assumed to remain bonded due to their impregnated state. The connection between the coil blocks and the winding pole is an exception, treated in detail in Subsection 4.3.4. Otherwise specified, all results refer to the case where the coil and the pole are considered bonded.

4.2.4 3D Mechanical models

The 3D model (Figure 4-4) uses instead higher order 20-node elements (SOLID186) for the mesh creation. Contact and target elements (CONTA174 and TARGE170) are also included at the corresponding coincident surfaces between components, where the same 2D contact definitions are respected.

As follows from the magnetic design (Chapter 2), the pad is divided in two parts along its longitudinal direction. Iron covers the majority of the straight section and stainless steel extends through the coil ends. With respect to the two-dimensional model, the 3D adds the important contribution of the longitudinal support system and the ends effects to the magnet simulation.

4.2.5 Material properties

Isotropic material properties as a function of the temperature are assumed for the different model constituents, including for the coils [198]. The values at cryogenic and room temperature for the structure materials are summarized in Table 4.2. The corresponding coil ones, which have been set according to the findings published in [129], are shown in Table 4.3.

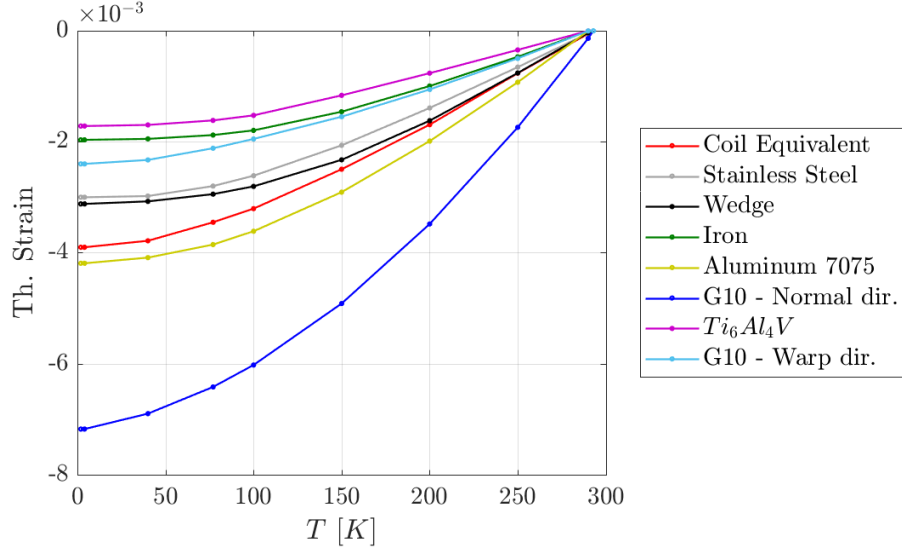


Figure 4-5: Thermal strain as a function of the temperature for the model materials.

Table 4.2: MQXF - Material properties used in the mech. model

Material	E (293 K)	E (1.9 K)	Unit	ν
ODS copper (Wedge)	120	120	GPa	0.3
Aluminum	70	79		0.34
Iron	210	224		0.28
G11 (Normal dir.)	7	7		0.3
G11 (Warp dir.)	30	30		0.3
Nitronic 40	210	225		0.28
Stainless Steel	193	210		0.28
Titanium	130	130		0.3

Due to its importance in the analysis of the induced stresses during a quench, the thermal contraction of each material as a function of the temperature is depicted in Figure 4-5.

4.2.6 The MQXF standard modelling steps

The complete simulation of the MQXF magnet mechanical behavior accounts for the various stages of the magnet assembly and operation. Preserving the static analysis approach, this is done by solving the corresponding load steps in sequential order. They comprise:

Table 4.3: Coil block mechanical properties [129]

		Unit
Material Model	Linear Isotropic	
Young's Modulus	20	GPa
Poisson's ratio	0.3	
Thermal Strain (293 K - 1.9 K)	$3.9 \cdot 10^{-3}$	

- **Load step 1 - Bladders inflated:** The inflation of the bladders using pressurized water is modelled by applying a pressure load in the bladder slot (both for 2D and 3D models). The load level should result in the opening of a gap, which allows to insert the desired key thickness with an additional clearance of 100 μm .
- **Load step 2 - Key insertion:** The insertion of the loading keys and the bladder deflation is instead reproduced by incorporating the key thickness offset to the contact element pair defined by the interference key and the pad master.
- **Load step 3 - Axial rods pre-tension:** The application of the mechanical preload in axial direction is performed by pre-tensioning the rods up to the target level. A displacement condition in the rod's end region reproduces this behavior.
- **Load step 4 - LHe vessel welding:** In the fourth step of the magnet assembly, a new offset (or interference) is applied to the shell - yoke contact. The interference seeks to mimic the effect of the stainless steel vessel welding around the aluminum shell envelope.
- **Load step 5 - Cool-down:** The cool-down of the magnet is modelled at its final stage, where the complete domain is set to cryogenic temperature (1.9 K for our studies).
- **Load step 6 and beyond - Operation:** At last, the analysis of the magnet mechanics during operation adds the effects of the electromagnetic forces to the

simulation. A magnetic model built in the same geometry provides the force values, which are incorporated in the structural analysis by direct load-transfer. For the 2D studies, the magnetic model is built in ANSYS APDL and thus, the process of transferring the nodal forces is just a simple reading operation between databases. OPERA [197] is instead used for the same objective in the three-dimensional version. In this case, the forces are imported in ANSYS and distributed across the coil nodes. For simplicity, none of these models (of standard architecture) are discussed in this work.

It is nonetheless important to note that some of the above-mentioned load steps may be removed from specific simulations. For instance, the LHe vessel welding is not taken into account when the MQXFS short models are studied. The main reason behind it is that short models are tested in dedicated cryostats, where the preparation of the magnet cold mass is not required. Furthermore, if the stresses during bladder operation are not of interest, the simulation can start directly at the key insertion stage.

The last modelling consideration deals with the simulation of the magnet ramp up to the nominal operation level. Due to the slow ramp rate of accelerator magnets, it can be reproduced by the computation of subsequent steps on which the forces are gradually increased as a function of the transport current.

4.2.7 MQXF - Mechanical models validation

The MQXF magnet mechanical models have been extensively validated in the past, under steady state conditions [129] - [138].

The so-called "Mechanical transfer function" (TF) provides perhaps the clearest overview about the accuracy of the model predictions. It relates the azimuthal stress seen in the coil winding pole to the value measured in the aluminum shell. Since the design of the magnet structure relies in that all the azimuthal force is provided by the shell, the TF determines which level of compressive stress is reached in the pole at the expense of the corresponding shell tension.

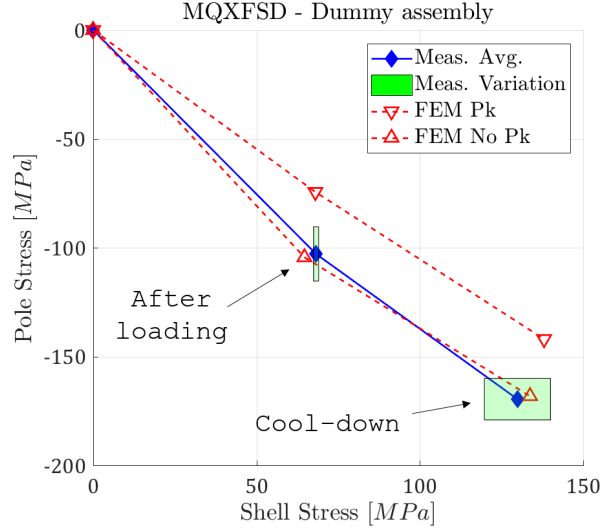


Figure 4-6: Pole azimuthal stress as a function of the shell stress for a dummy magnet assembly. The red lines show the results of the 3D model, while the green boxes depict the measured variation from the different strain sensors. Figure courtesy of G. Vallone.

Figure 4-6 shows the measured and computed transfer function for a MQXFSD dummy magnet assembly, where the superconducting coils were replaced by precisely machined aluminum blocks (thus, reducing the uncertainties on coil dimensional variability and material properties). It can be seen that the computation matches correctly the measured response of the structure, considering that no alignment polekey was installed in the assembly. In this regard, two 3D mechanical simulations are depicted: i) the "Pk" FEM corresponds to the situation on which the G11 alignment polekey is in contact with the aluminum collars, thus intercepting part of the force coming from the shell, and ii) instead, the "No Pk" model removes this interaction from the simulation and hence, the azimuthal stress in the pole increases for an equal shell stress.

The same comparison is performed in Figure 4-7 for the first MQXFSD short model magnet tested. In this case, the contact between the polekey and the aluminum collars was ensured by the presence of shimming. The plot shows that the mechanical model maintains its good prediction capabilities when the real impregnated coils are included in the simulation.

A complimentary piece of information linked to the model validation is unveiled by

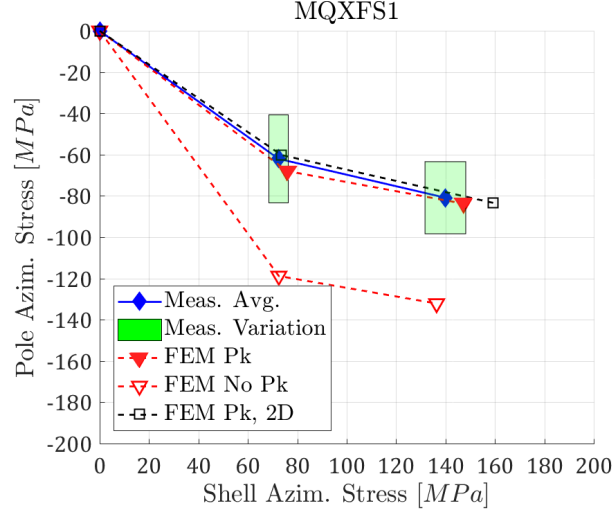


Figure 4-7: Mechanical transfer function plot for MQXFS1 model magnet. The red lines show the results of the 3D model and the black one corresponds to the 2D simulation. As for the previous plot, the green boxes indicate the measured variation from the different strain sensors. Figure courtesy of G. Vallone.

the strain measurements performed during magnet powering. The graph in Figure 4-8 compares the real delta in azimuthal stress experienced in the winding pole (during current ramping of MQXFS5 magnet) with the mechanical model results. Provided that this particular phenomenon will be described extensively in Subsection 4.3.4, here we just limit the interpretation of the figure to the direct comparison between curves. As for the case of the transfer function, the model catches the coil response during operation.

4.3 Magnet mechanical behavior during assembly and operation

Having introduced the MQXF structural FEM models (and the main simulation assumptions), the analysis of the magnet mechanical behavior during the assembly and operation phases will be presented next. Special emphasis is put in the review of the coil stresses, as they will be of particular importance for the rest of the chapter.

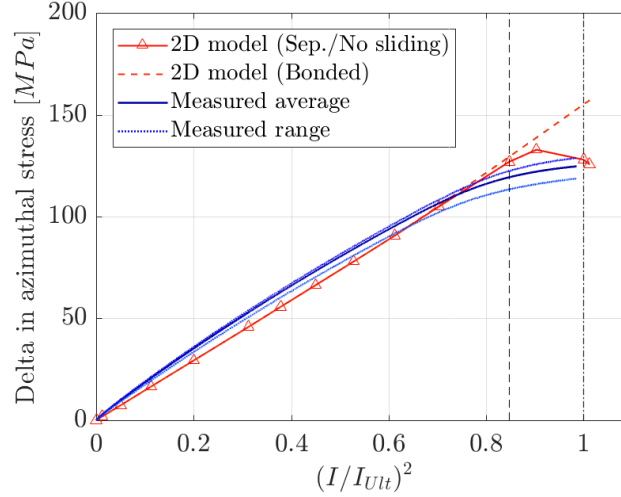


Figure 4-8: Delta in azimuthal pole stress during the MQXFS5 magnet ramp. Two red curves show the 2D model results: 1) the solid line with markers corresponds to the simulation where the coil can detach from the coil and 2) the dashed line represents instead the case where the interface is bonded. The black vertical lines indicate the nominal and ultimate current level.

4.3.1 Simulation parameters

The outcome from the structural analysis of the MQXF magnet is in a large extent determined by the level of mechanical preload applied. Thus, before further proceeding, it becomes essential to establish the main simulation parameters. These should be respected both in the 2D and 3D calculations. In our case, the MQXF baseline in terms of the magnet preload (defined in Chapter 2) will be employed for the solution of the models.

Recalling the numbers, the targets correspond to 110 MPa of azimuthal compression measured in the pole at cryogenic temperature and a total compressive axial force of 1.17 MN delivered by the rods after cool down⁵.

In the following, the results from the octant models will be shown, considering that for the reference case all loading conditions respect the magnet symmetry.

⁵The FEM simulations performed under the mentioned parameters will be commonly referred as the reference case for the mechanical analysis.

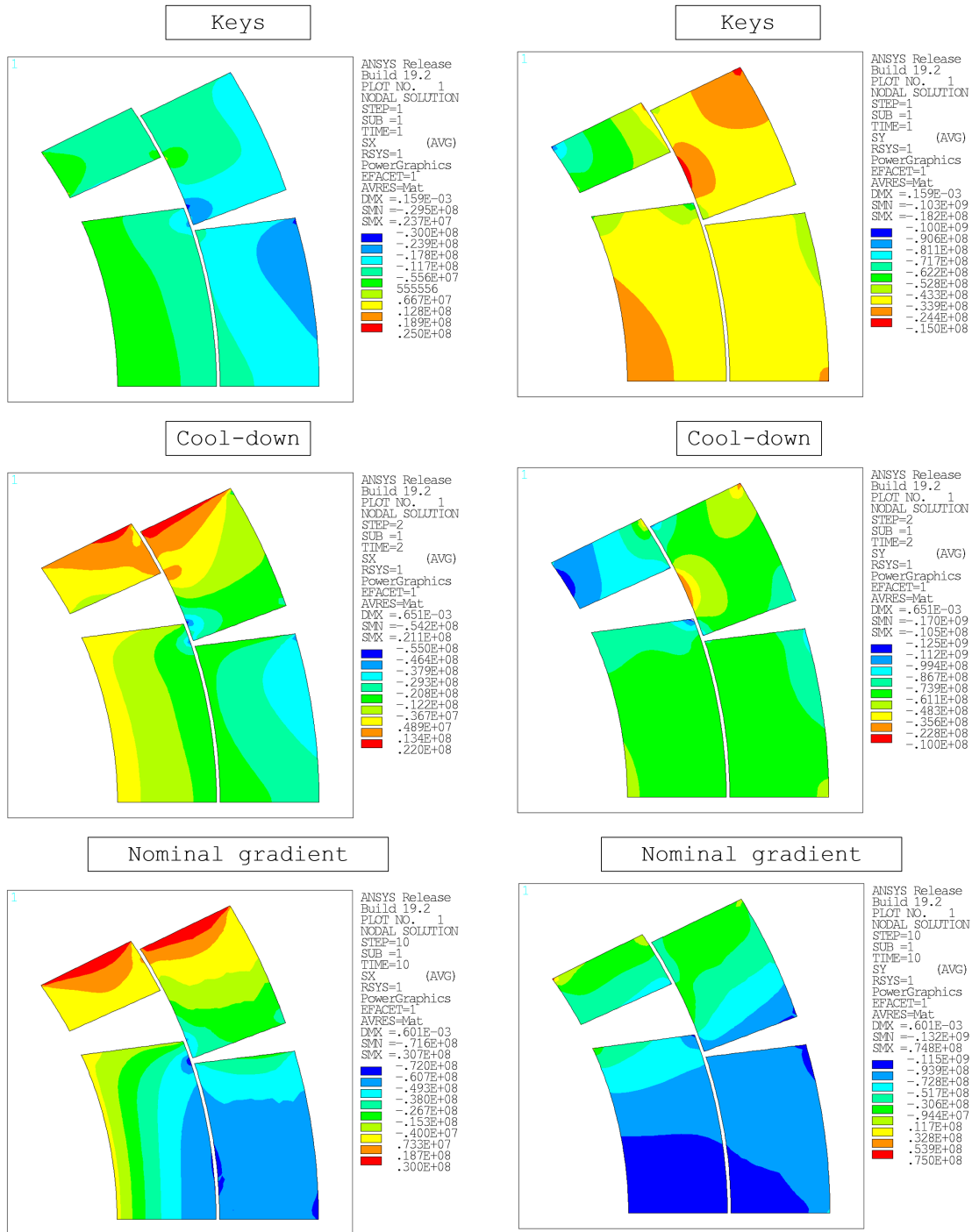


Figure 4-9: On the left, coil radial stresses during the three main simulation steps. On the right, corresponding azimuthal stresses. A peak stress of 125 MPa is found during cool-down in azimuthal direction.

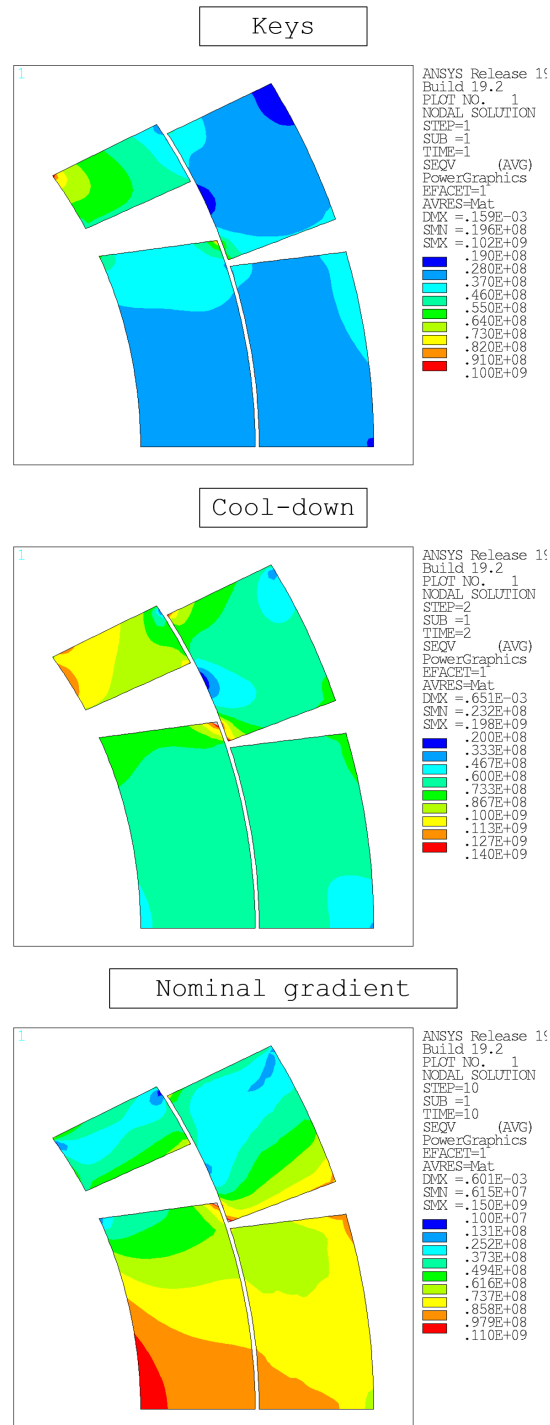


Figure 4-10: Coil Von Mises equivalent stress computed with the 2D octant model. The peak stress during operation equals 110 MPa and is located at the coil mid-plane.

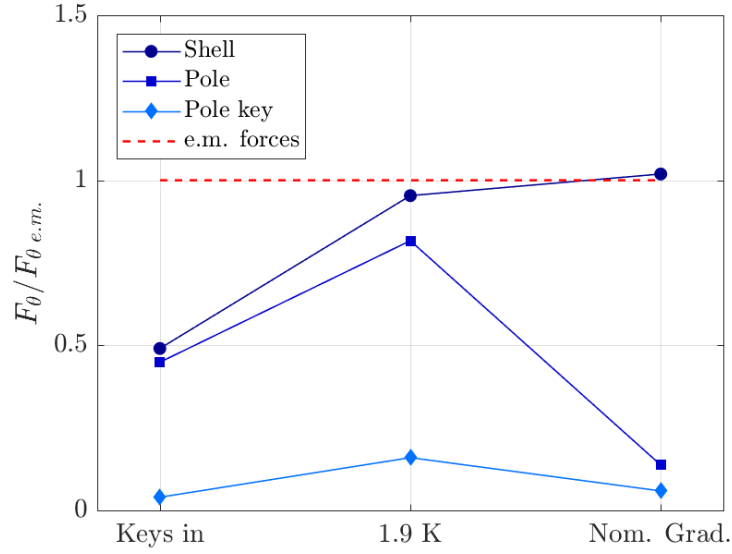


Figure 4-11: 2D force redistribution in the MQXF magnet structure for the reference case.

4.3.2 2D Mechanical analysis

Figure 4-9 plots the radial (left) and azimuthal (right) stress distribution obtained in the superconducting coil using the 2D model⁶ for the assembly, cool-down and operation steps. The maximum stress in azimuthal direction is found at cryogenic temperature and equals 125 MPa; it is located in the inner radius of the pole block conductor⁷. When the magnet is powered to nominal current, this maximum shifts in position towards the coil mid-planes due to the direction of the Lorentz forces. A peak value of 115 MPa is identified there. In terms of radial stresses, a small region showing 75 MPa (compression) can be seen at the nominal powering stage. The location corresponds to the outer radius of the coil.

⁶We recall that for this case, the contact between the winding pole and the superconducting coil blocks is considered as bonded.

⁷Practically, the az. stress level in the MQXF magnet coils is monitored using the mechanical instrumentation placed in the winding pole. In general, the stress values at the pole are representative of those seen in the pole turn (peak field) conductor [129]. As an example, at cryogenic temperature the azimuthal stress in the pole equals almost 115 MPa, which compares well with the coil peak value of 125 MPa.

The Von Mises equivalent stress distribution is instead shown in Figure 4-10 for the same load steps. Its peak value evolves from 100 MPa, after the magnet preload at room temperature, to 140 MPa when it is cooled down. As the coils are energized to nominal current, the peak stress decreases and shifts to the mid-plane of the coil. The highest stress foreseen by the model equals 110 MPa in this last case.

Finally, the force redistribution in the magnet structure (as computed by the model) is provided in Figure 4-11. For the reference case, the shell provides at cold an azimuthal force that is almost compensating the full e.m. load. The polekey, which is not in contact with the collars at the beginning of the magnet assembly⁸, closes the gap after magnet cool-down. Thanks to it, the alignment of the four coils is assured at the expense of intercepting part of the force at cold. Finally, when the magnet is energized to nominal current, the shell increases slightly its tension, while the pole is unloaded due to the action of the e.m. forces.

4.3.3 3D Mechanical analysis

The three-dimensional mechanical model confirms the stress levels predicted in 2D for the magnet straight section and adds the essential information on the coil ends and on the stress components in z-direction.

Figure 4-12 summarizes the coil stress state in terms of the azimuthal component (left) and of the Von Mises equivalent stress (right). Consistently with the two-dimensional results, the maximum coil az. stress (compressive) evolves from less than 100 MPa after the magnet preload at room temperature to 115 MPa at nominal operation. Regarding the equivalent stress, the 3D simulation reveals that a similar peak value to that computed in 2D is also located in the coil ends during the cool-down and operation steps; it corresponds to 140 MPa.

⁸In practical terms, this is done by imposing a certain gap during the assembly of the coilpack.

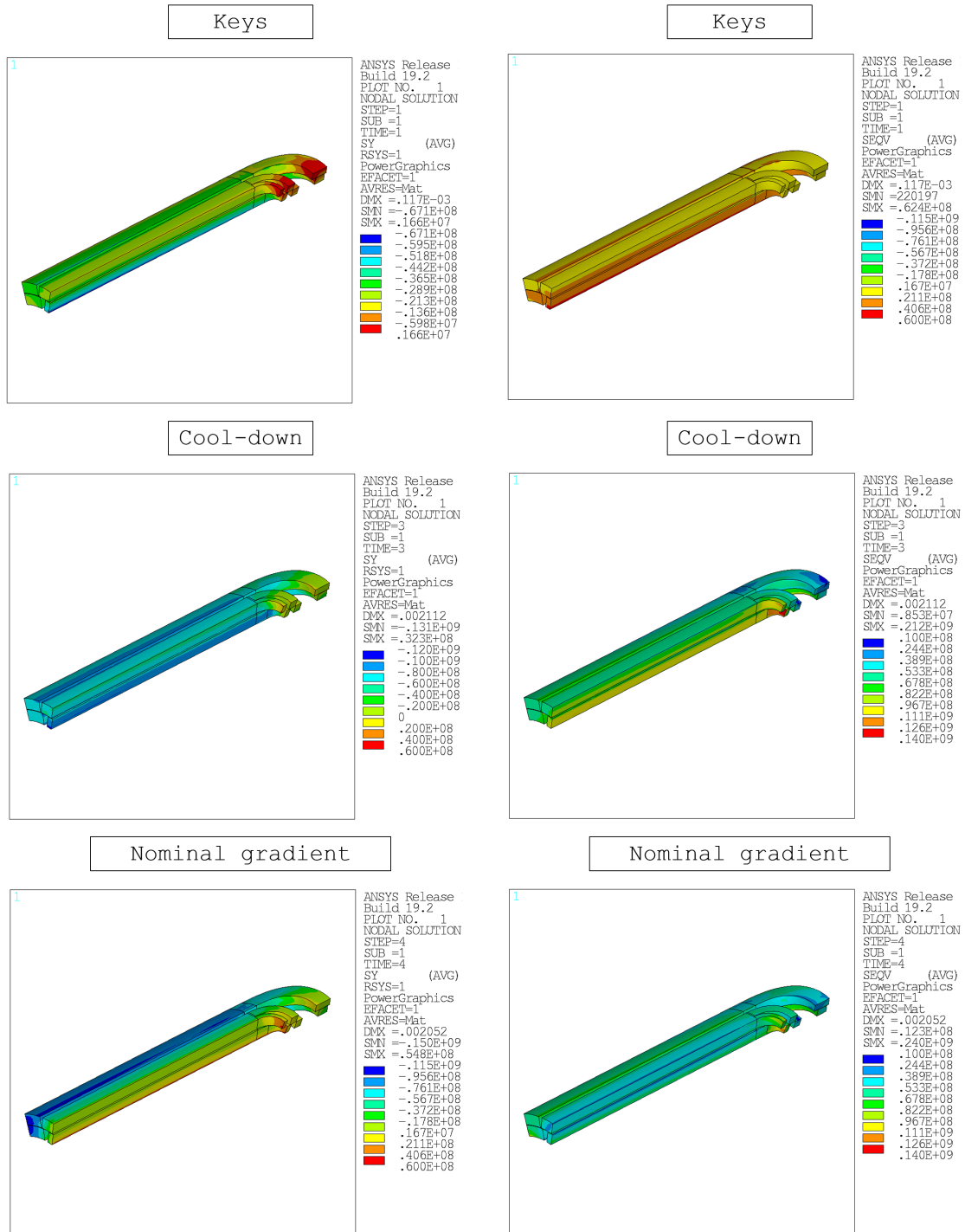


Figure 4-12: In the left, coil azimuthal stress computed in the three-dimensional model. In the right, Von Mises equivalent stress distribution.

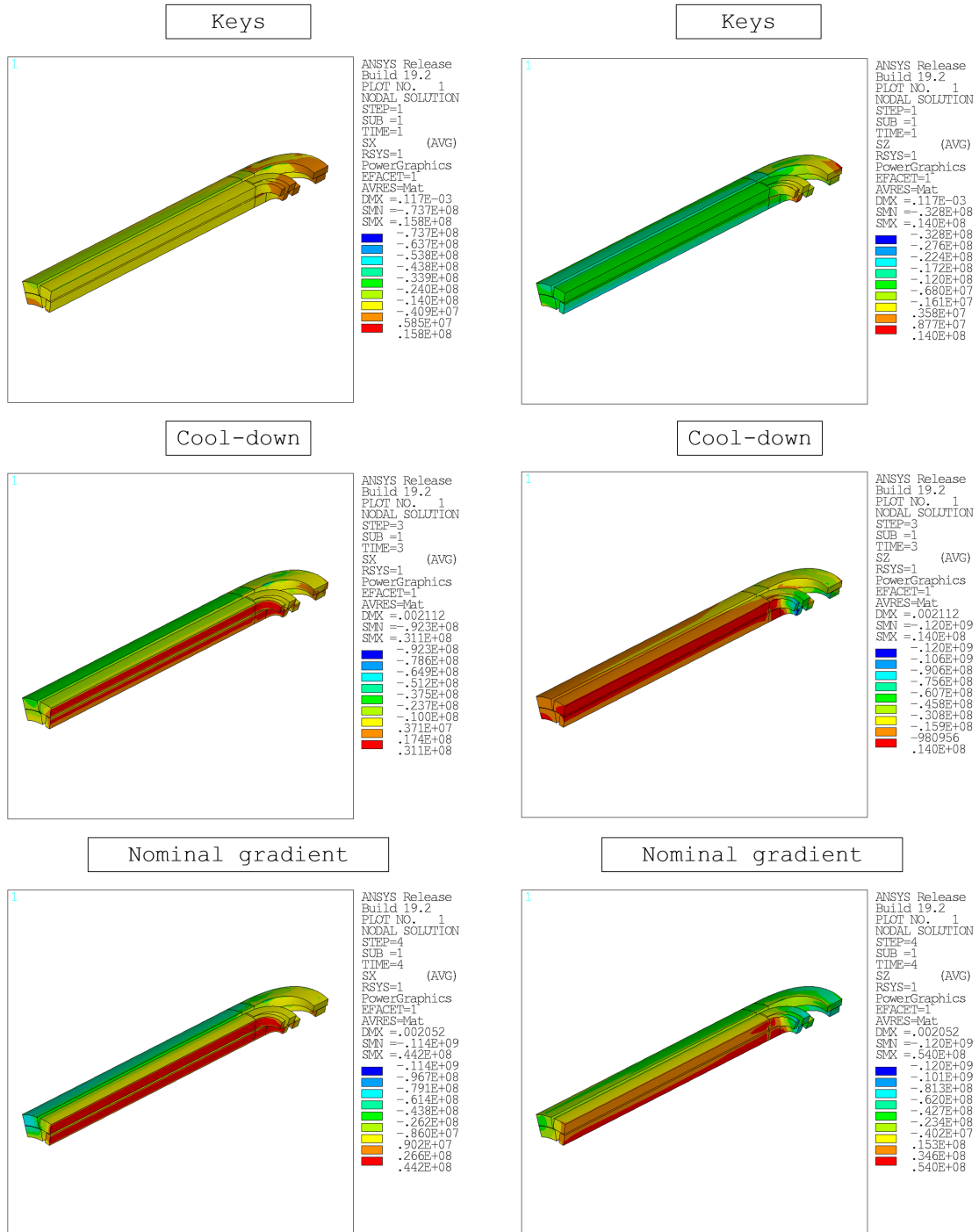


Figure 4-13: Coil radial stress (left) and coil longitudinal stress (right) distributions for the main load steps.

The radial and longitudinal stress distributions are depicted in Figure 4-13. For the peak value in z-direction, the model predicts a maximum of 120 MPa located in the coil ends at cryogenic temperature (compression). This level of transverse stress can be understood in terms of the longitudinal support system, which compresses the end turns with the total expected e.m. force level. The maximum radial stress is as well consistent with the 2D computation, bringing a value of 70 MPa in the coil outer radius during operation (in the magnet straight section).

Although not shown, the stress level in the structure parts are maintained within their corresponding limits. For ductile material like aluminum, stainless steel or titanium, the so-called Von Mises yield criterion (maximum distortion energy theory) is employed (see Table 4.4); for brittle materials like iron, the maximum principal stresses are verified. Detailed results for the structure can be found in [199] for the MQXFA magnet version.

Table 4.4: MQXF - Material limits

Material	Yield strength		Unit
	293 K	1.9 K	
Aluminum (Al 7075 T6)	420	555	MPa
Stainless Steel 316 L	289	375	
Nitronic 50	520	-	
ARMCO Steel	230	-	
Titanium 6Al4V	830	1500	

4.3.4 The pole unloading mechanism

Up to this point of the chapter, all results have been obtained considering the different components inside the coil as bonded. Nevertheless, it should be stressed that the connection between the titanium pole and the coil merits a special treatment.

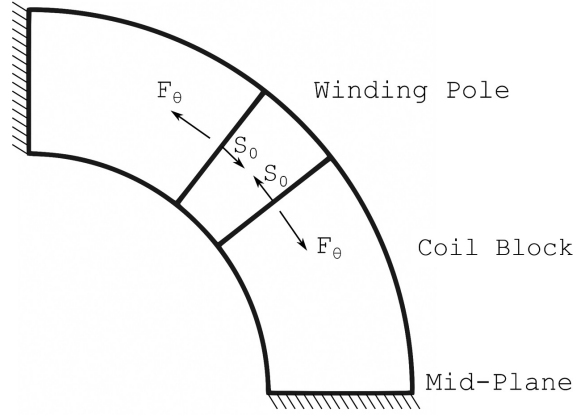


Figure 4-14: Schematic showing the azimuthal prestress (S_0) and the azimuthal electromagnetic force (F_θ) acting on a $\cos(\theta)$ coil.

When a $\cos(\theta)$ magnet-type is energized, the e.m. forces pull the coil away from the winding pole, progressively counteracting the applied magnet preload (Figure 4-14). This effect, which has been already shown by the stress plots and by the force redistribution graph, can be proven experimentally if mechanical instrumentation is placed in the winding pole. The strain sensors, as for the case of MQXF, will show a reduction of the azimuthal stress (σ_θ) with the increase of the current. Setting the az. stress value to zero before the ramping of the current, the obtained delta in σ_θ defines the loss in azimuthal compression (see Figure 4-8).

The most common strategy when modelling epoxy-impregnated superconducting coils is to assume a bonded or glued contact between the coil and the pole, as introduced. Though, the previously mentioned measurements show that the azimuthal stress in the pole may flatten at a certain level of current. This effect is understood as an indication that the coil is detaching from the pole, and consequently all the coil preload is lost at that level of current. In [135], it was demonstrated that such an experimental evidence can be accurately reproduced by modelling coil-pole debonding using cohesive elements at that particular interface.

A similar result can be obtained assuming a rough contact for the coil to pole surface, where separation is allowed and a high friction coefficient reproduces the capability of the epoxy to still carry tangential forces when it is broken (no sliding

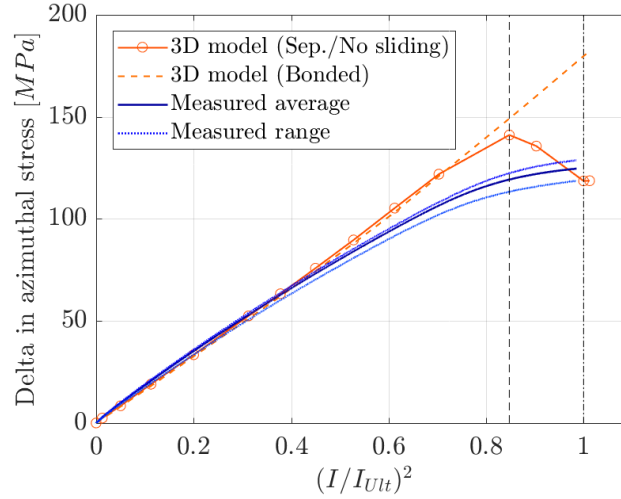


Figure 4-15: Comparison between the three-dimensional simulation of the current ramp in MQXFS5 magnet (orange) and the measured values. The delta in azimuthal stress seen in the winding pole is shown.

permitted). Figure 4-8 shows how the model is able to reproduce the pole unloading curve during powering, and compares the 2D FEM prediction to the experimental data obtained in the test of MQXFS5 model magnet. This feature will become important in the simulation of the quench mechanics, since it will determine the delta in stress seen by the titanium pole during a quench.

For comparison, the results from the 3D model are shown in Figure 4-15. Interestingly, the three-dimensional simulation shows a higher slope and hence a larger separation between the coil and the winding pole at high current.

4.4 Modelling of the mechanical response during a quench

To include the analysis of the magnet mechanics during a quench, additional load steps are incorporated to the model. These steps expand the structural analysis beyond the magnet operation at stable current, by adding new load cases corresponding to the quench discharge.

In practice, the explained load-transfer method is used to import the temperature

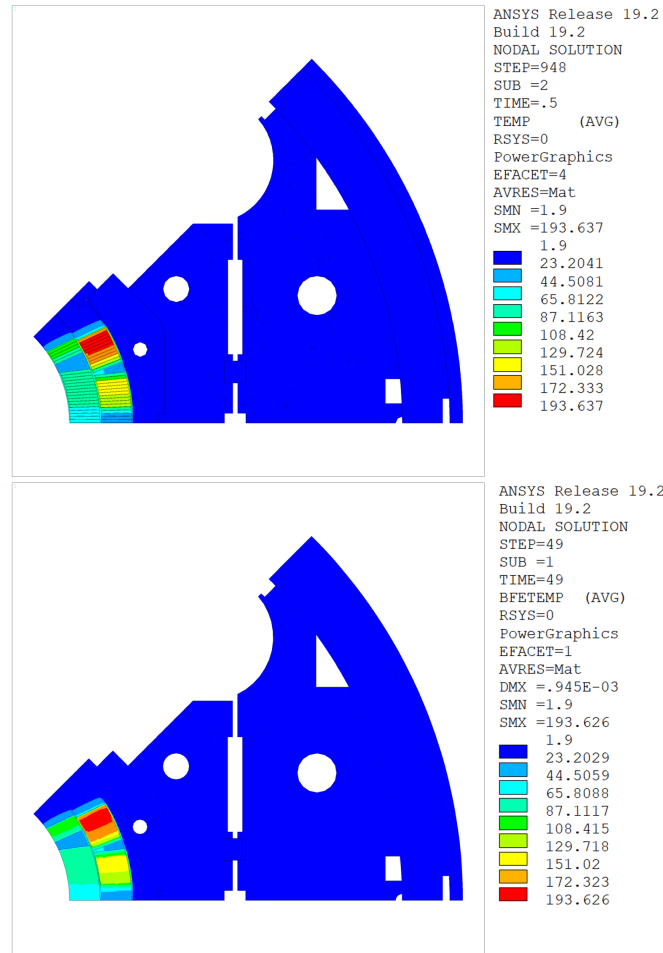


Figure 4-16: Top) Resulting temperature distribution from the 3D Thermal-electric FEM model. The division of the various conductors defines the coil domain. Bottom) Corresponding temperature field transferred to the mechanical model using mesh interpolation. In this case, the conductor is modelled as an homogeneous block. The temperature distribution is well preserved during load transfer, as well as the minimum and maximum values.

and the magnet current values into the mechanical model. The temperature distribution results in a thermal strain field, and thus in the appearance of complimentary mechanical stresses, while the transport current is used in the computation to scale the e.m. forces according to the current decay.

Each mechanical simulation starts with the solution of the required assembly steps and the cool-down to cryogenic temperature. Afterwards, a ramp is performed up to the quench current. To conclude, the steps corresponding to the magnet discharge

are solved.

Taking into consideration that the FE mesh from the various models differs (note that in the thermal-electric model each individual conductor is modelled), a mesh interpolation algorithm needs to be used for the inclusion of the temperature field. Thanks to the availability of a built-in function in ANSYS APDL, the process is performed inside the software. An example of the interpolation operation is shown in Figure 4-16, where the top plot depicts the results from one multi-physics calculation and the bottom contours show the resulting distribution in the mechanical model. As it can be seen, both the field distribution and the peak values are preserved.

4.4.1 References to similar analyses

Following similar guidelines to those presented above, the study of the magnet mechanical response during a quench has been tackled in a reduced number of references of which the authors are aware. Two-dimensional research works can be found in [182], [200]-[202]. Though, a full understanding of the magnet mechanics during a quench can be only obtained by means of a three-dimensional analysis, as already claimed in this book. In this regard, the complexity of the 3D problem has been approached in [181], [184], [203]-[204], where two different methods aimed at obtaining a first insight into the topic were presented. With our work, we try to complete finally this development and to provide clear answers about the mechanical implications of a quench event.

4.5 Mechanical behavior during a quench: Model validation

One of the main objectives of our research is to characterize the mechanical response of the MQXF magnet during a quench happening at nominal operation. Prior to such analysis, we will dedicate the following pages to the validation of the proposed methodology and the validation of the principal modelling assumptions. In tight

correspondence with what has been presented for the thermal-electric simulation, the mechanical analysis will be validated profiting from the test results of the MQXFS short models.

On this subject, it is worth to highlight that the good agreement obtained in terms of simulated and measured current decay (using the multi-physics quench model) does not necessarily imply that the predicted coil temperature is correct. If this is not the case, the outcome of the mechanical simulation may not provide reliable information on the quench transient.

Thus, to prove the correctness of our structural analysis, the results of the mechanical model will be compared to real experimental data. The latter has been acquired using the instrumentation installed in the magnets; in particular, we will concentrate in the study of controlled quench discharges (the well-known quench integral tests) done in MQXFS5 magnet.

The main particularities of the experimental measurements will be presented first, in an attempt to equip the reader with the necessary information for the understanding of the model results. Finally, the model predictions will be shown using both the two-dimensional and three-dimensional geometries.

4.5.1 Experimental mechanical readings during a quench

Validity of the measurements

It has been introduced in Chapter 2 that two different types of strain sensors were used along the tests of short model magnets: resistive strain gauges (SG) and fiber optic sensors based on Fiber Bragg Grating (FBG). The positions where these systems are installed correspond to the coil winding poles, the outer shell and the longitudinal tie rods (See Figure 2-3 in Chapter 2).

The strain measurements rely on active compensation schemes, which seek to remove from the readings the apparent effects derived from the magnet operation at cryogenic temperature, and from the presence of high magnetic fields (the latter, just applicable for the electrical strain gauges) [139]-[142]. With this objective, so-called

compensators⁹ are employed to eliminate the pure thermal strain component (thus, resulting into the measurement of mechanical strain) and the contribution coming from the change in electrical resistivity of the SG grid due to the magnetic field. This is done by direct subtraction between the signals of the active and the secondary gauges.

When compensated, the goal of the mechanical instrumentation is to analyze the magnet response during the assembly and powering phases under steady-state conditions. Nevertheless, if well-synchronized signals at high acquisition rates are available (we have found consistent results in the range of 100 Hz - 2 kHz), the analysis of the magnet mechanical behavior can be also expanded to quench events.

In this last case, it must be considered that the outcome from the measurements will be accurate just up to the first instants after the discharge. This statement is supported by the fact that for fast quench transients, the thermal gradients remain concentrated inside the coils during a short initial time frame. The structural parts (as proven by the thermal model) and the compensation system remain, meanwhile, almost at a constant temperature. In this regime, there is no imbalance between the main sensor and the compensator, and the readings are meaningful.

For larger time scales, the magnet enters into a slow transitory phase of re-cooling where the compensation strategy may result in false strain readings. Note that electromagnetic transients (including induced currents in the SG electrical grid) cannot be discarded, but they may only affect the electrical strain gauge results.

A crucial experimental evidence confirming what has been explained in last paragraphs is found in Figure 4-17 - Left. The top plot shows the typical response during a quench of an active SG sensor located in the MQXF coil winding pole¹⁰ (azimuthal direction). No compensation has been yet applied to the curves, where $t = 0$ s is set at the moment the quench is detected. The acquisition rate, which will be the same for the rest of cases presented in this section, is 100 Hz. According to the continuity

⁹The compensator is a second strain sensor of the same type than the active one, which is installed in a floating piece of the same material. It must be ensured that this secondary gauge experiences the same conditions than its active counterpart.

¹⁰Specifically, it corresponds to a measurement in one of the coils of the MQXFS5 magnet.

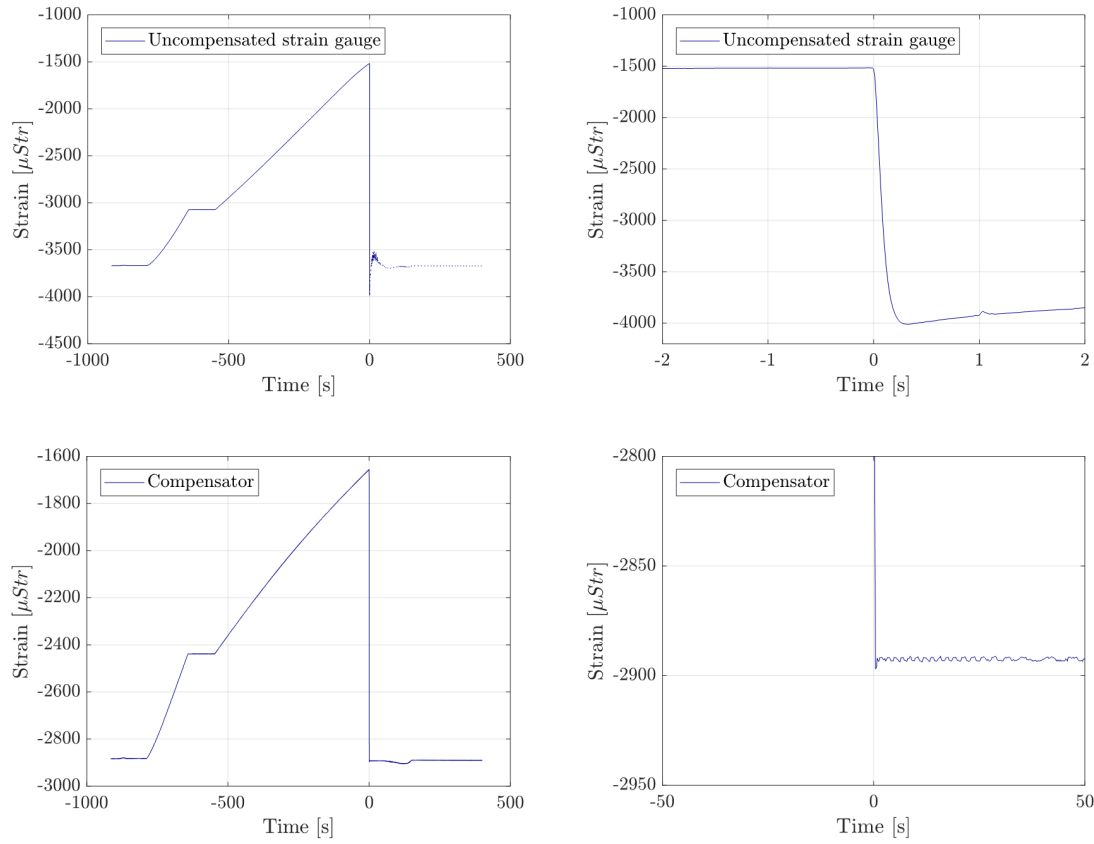


Figure 4-17: Raw strain readings from the active gauge and the compensator for a standard quench event (azimuthal strain for the active gauge). The left column provides an overview on the full event, while the right one plots a zoom in the region of interest.

of the measured curve in Figure 4-17 - Right, this sampling frequency ensures that the peak values are captured.

A very fast change in the strain is seen at the moment of the quench, reaching the minimum point at 0.45s. Part of this change is induced by the sudden variation in magnetic field (which follows the current profile in Figure 4-18), while the rest can be identified as thermal and mechanical strain.

The response of the compensator is instead shown in the bottom graphs of the same figure. As predicted, the second sensor does not react to the thermal transient that occurs during the first instants after the quench ($< 5 \mu str^{11}$), and it comes back

¹¹ μstr stands for micro-strain, $\mu m/m$.

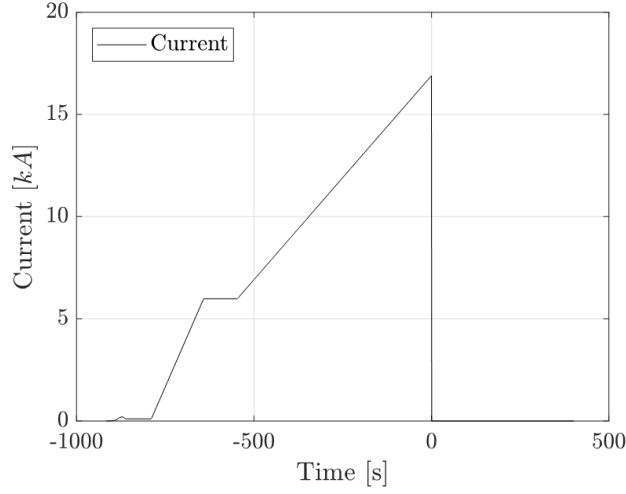


Figure 4-18: Current cycle for the quench whose measurements are depicted in Figure 4-17.

to its initial values. Therefore, one can confirm that there is no thermal impact on the compensator in our region of interest (only the magnetic effect is visible).

The second main concern for the interpretation of these measurements deals with the quantification of the thermal strain arising in the pole, due to the magnet warm-up process. Figure 4-19 plots the temperature in the widening pole predicted by the adiabatic model for a standard quench happening in the inner pole turn conductor (the closest to the measurements location). This conservative calculation¹² shows that the temperature at the position of the mechanical strain sensors stays below 7 K at the end of the discharge. Considering that almost the totality of the thermal contraction in the Titanium pole happens from ambient temperature to 50 K (See Figure 4-5), an increase from 1.9 K to 10 K has a negligible impact in the thermal strain measured.

Hence, discarding the initial impact of thermal strain in the compensator and in the active gauge, it becomes possible to define two separate regimes for the mechanical measurements during a quench: i) a meaningful first part, which accounts for the magnet powering and the first seconds of the discharge (highlighted in green in

¹²The presence of cooling by the He bath will likely decrease the temperature in the pole surface almost to the bath value.

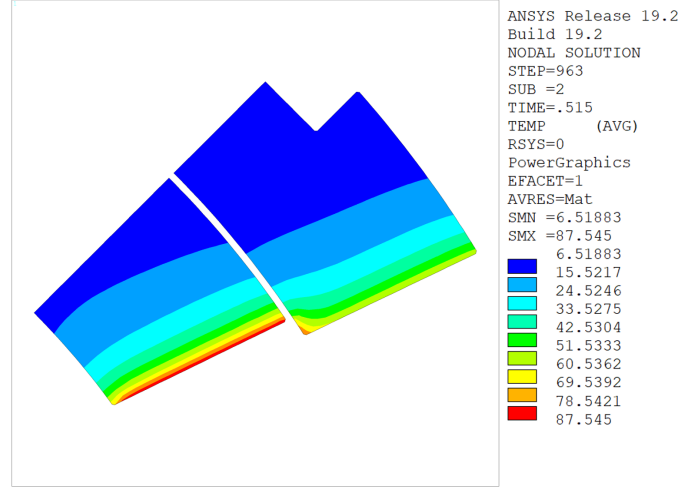


Figure 4-19: Two-dimensional computed temperature in the winding pole for the reference quench case at nominal current. The image shows that the hot-spot conductor is located next to the inner pole piece, thus resulting in the highest temperature at the position of the mechanical instrumentation. Note that adiabatic boundary conditions are assumed.

Figure 4-20) and ii) a rejected second half of the measurement that includes a larger transitory phase, where the strain signals may be unbalanced.

Unfortunately, it results difficult to establish clear thresholds in terms of time for each of the regimes, since they depend on the energy stored in the magnet and on the hydraulic transient happening in the cryogenic bath. For the tests shown in this thesis, we limit our analysis of the data up to a maximum threshold of 1.5 s after the quench is detected. As shown by the previous plots and by the temperature in the pole after the discharge, there exists a safety margin in this analysis.

FGB - SG agreement

Figure 4-20 shows the example of the azimuthal and longitudinal stress observed in the winding pole (one coil) during a training quench occurred in MQXFS5. In this magnet, SG and FBG systems were installed together to verify the agreement on their signals. In what regards the stress values¹³, they have been derived from the strain measurements assuming a bi-axial stress state. Moreover, for a better reading,

¹³Note that in this occasion, compensated values are used.

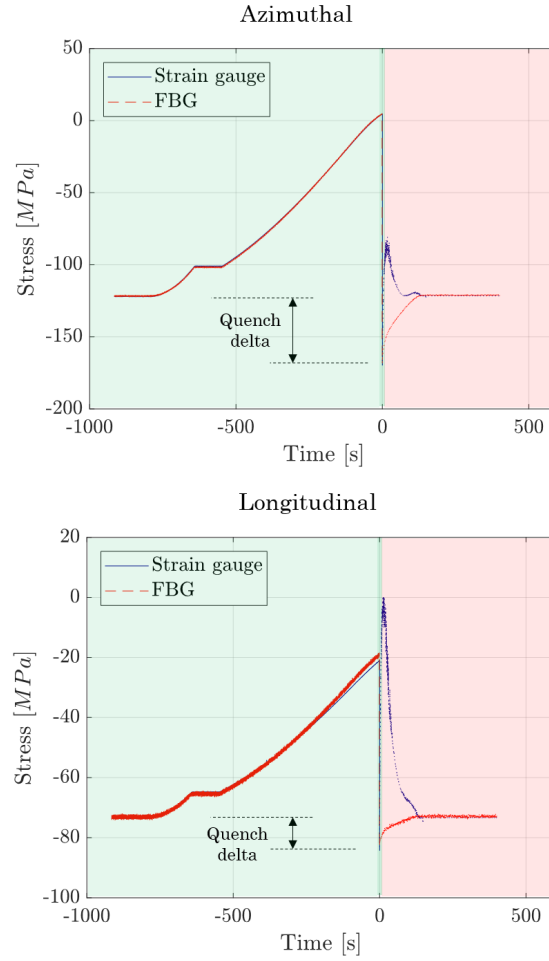


Figure 4-20: Azimuthal and longitudinal stress seen in the winding pole for a standard training quench in MQXF. The solid blue line depicts the strain gauge readings, and the dashed red line the corresponding FBG response. The "quench delta" is highlighted by the black arrow.

the time has been set to zero at the moment the quench protection is triggered ($t = 0$ s). The consistency in the results of both systems, whose readings are found to remain within a difference of ± 3 MPa for $t < 0$ s, provides a strong confidence in the measurements¹⁴.

It should be emphasized that the principles of strain analysis by electrical gauges and optic fibers are completely different, and each of the system is affected in a

¹⁴For the FBG sensors it is possible to identify a clear time constant during the magnet re-cooling phase. If this information is used to calculate the intersection with the ordinate axis, the same quench delta is obtained. The result proves that 100 Hz acquisition rate is enough to capture the magnet behavior.

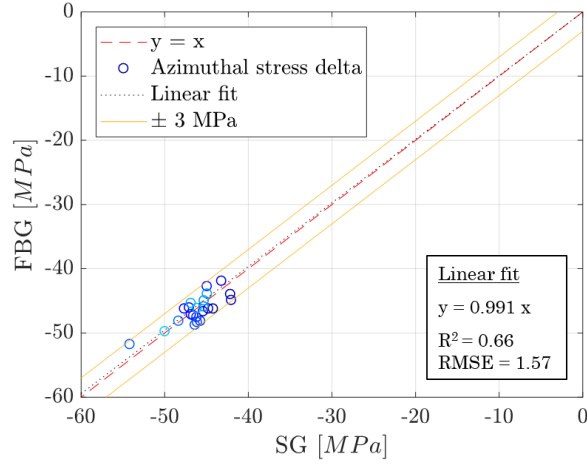


Figure 4-21: Comparison between the quench delta values measured by strain gauges and optic fibers for several quenches. The fit information is shown in the right box, where RMSE stands for "Root Mean Square Error".

different way from the relevant quench physics. Unlike the optic fibers, the strain gauges show a not physical response during the neglected re-cooling phase that has not been yet explained. The topic may deserve a separate investigation, which is not discussed here.

The quench delta definition

Regardless of the system used, we introduce the concept of the "Quench delta" as the stress difference between the beginning of the magnet powering and the end of the quench discharge (depicted by the black arrow in Figure 4-20). This delta can be understood as the additional stress that arises from the quench thermal gradients and the fast e.m. transient. Thereby, it can be used as a parameter to characterize the magnet mechanical response during a quench event.

Figure 4-21 shows the correlation between the quench delta values obtained with electrical strain gauges and optic fibers for quenches where both instruments were installed together. The plot, which shows a slope of 0.991 for the linear fit and a consistent range of 3 MPa, clearly supports the fact that the delta is a true physical effect and not an artifact of any of the systems.

4.5.2 Comparison with modelling results

After the analysis of the pure experimental results, we introduce in the following the corresponding model predictions for the controlled quench discharges.

The importance of the pole unloading

The measurement of the azimuthal and longitudinal strain in the winding pole constitutes the closest place to the conductor, where mechanical strain can be practically measured. Finite element simulations confirm that during the assembly and powering steps, the stress in the winding pole is representative of that of the pole turn cable. Due to the proximity to the thermal gradients generated during a quench, these measurements offer a great possibility to validate the model.

In the first instance, we will therefore center our attention on the values obtained for the quench delta in azimuthal pole stress (where the largest load is observed). Figure 4-22 shows the results from different quench integral discharges done in MQXFS5 magnet, and their corresponding 2D & 3D simulations. Two different modelling strategies are included:

- (a) In the first, displayed with empty markers, the connection between the coil and the pole is modelled as bonded.
- (b) The second, instead, is represented with full markers and assumes that the coil is allowed to separate from the pole.

Notably, a larger quench delta can be perceived for the second model, as soon as the current level is enough to cause the separation from the pole (this is seen in the plots at 16.47 kA, note that the two model results diverge.). The effect is stronger in the 3D model, which predicts a larger separation than the 2D as already shown in Figure 4-15.

The better agreement with the test results suggests that the de-bonding mechanism is present in the real magnet. This finding is indeed consistent with the unloading curve of MQXFS5 and with the experimental evidences cited in [135]. However,

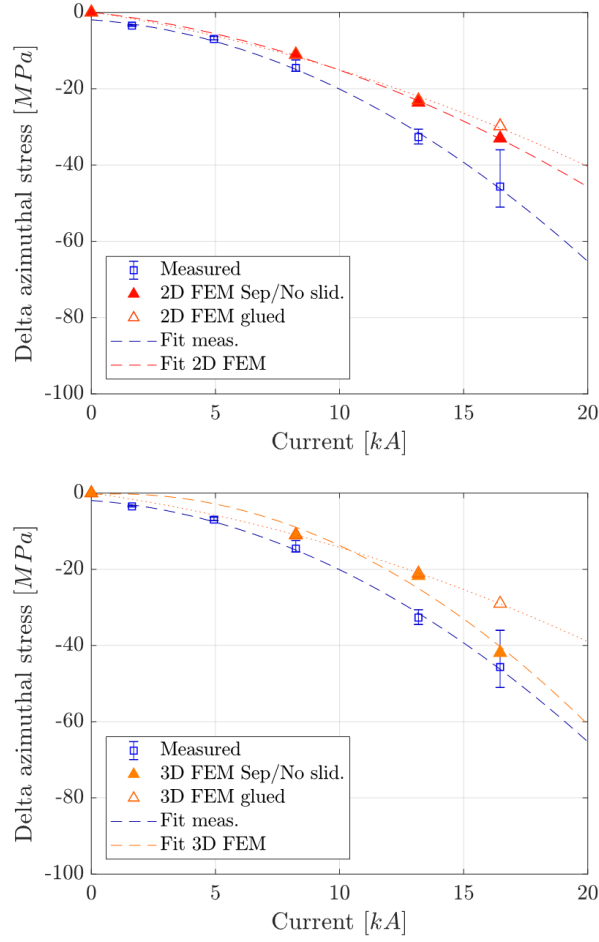


Figure 4-22: Comparison between experimental and simulated values for the quench delta in azimuthal pole stress. In the top plot the results from the two-dimensional simulation are shown. The bottom plot depicts instead the case of the three-dimensional model. For the experimental values, the range between the four coils is indicated. Dashed lines represent the quadratic fit of the data.

the measured delta hints that the de-bonding may happen following a more gradual mechanism, since the test data does not experience a sudden change at nominal current as the model shows.

In fact, the experimental curve can be better approximated by gradually increasing with the current the friction coefficient between the coil and the pole for the case where separation is allowed. The outcome from this exercise using the 2D simulation¹⁵ is

¹⁵The same exercise can be performed in the 3D model to match the results using a different friction coefficient.

shown in Figure 4-23. However, although not impossible, the change in this coefficient can be seen as a pure fitting parameter.

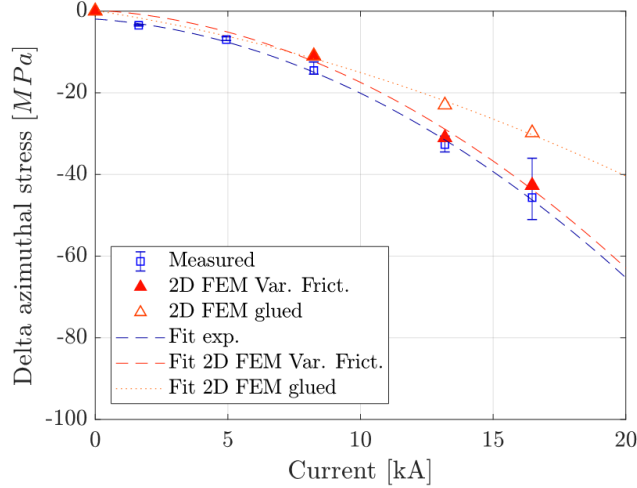


Figure 4-23: 2D simulation of the az. quench delta in MQXFS5 magnet (for quench integral discharges at various currents) using a variable friction coefficient between the coil blocks and the pole.

Centering the discussion on the physical meaning of the pole to coil connection, the standard bonded case can be understood as representative of a magnet with large enough mechanical preload to avoid the complete pole unloading. On the other side, the separation-allowed case would be instead indicative of a magnet that has underwent various cycles up to large current (above the preload compensation capabilities), thus resulting in the progressive debonding of the coil to pole interface.

In view of the complexity in simulating accurately this specific process (and the dependence of the phenomenon in the preload level of each magnet), we will for our final analysis exclusively focus on the results of the glued model. One should keep in mind that the final preload target chosen for the MQXF magnets (110 MPa at cold) is such that the pole unloading mechanism starts to be experimentally perceived at nominal current. Consequently, since the magnet is not expected to operate beyond this level, the glued assumption can be justified.

Furthermore, in terms of coil stress, the "sep./no-sliding" and the "glued" model results are very similar. The only difference lies in the large increase of stress that

is created in the corner node of the coil when the separation is allowed. Generally, such type of stress singularities can be disregarded in the analysis, since they vanish few nodes away from the corner. In our case, this stress concentration is originated by the no-sliding condition, which locks the coil node once it comes back in contact with the pole. This stress intensification increases the quench delta, due to the direct contact with the titanium pole. The coil az. stress contours for both simulated cases are shown in Figure 4-24. Note that the same phenomenon (of larger magnitude due to the larger separation expected) is seen in the 3D model.

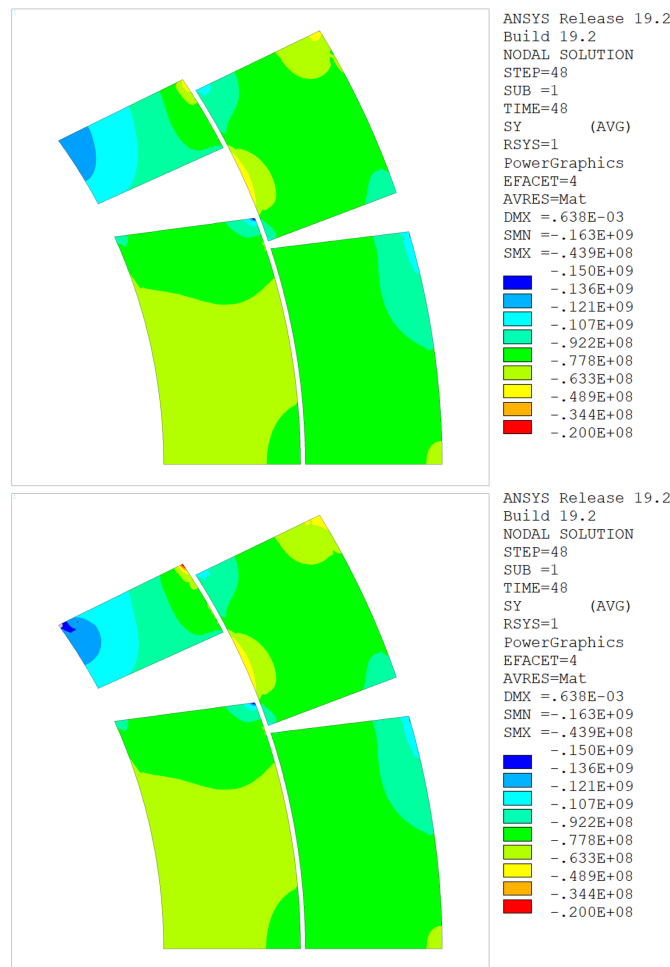


Figure 4-24: Coil azimuthal stress contours at the end of the 16.47 kA QI test (see Figure 4-22). The plots have been produced with the 2D octant geometry for: top) glued coil/pole interface and bottom) sep.no-sliding assumption. The only difference between both results is identified in the vicinity of the inner radius corner node, where the stress is intensified by 10 MPa in the separation case.

The bonded case and its energy dependence

For the bonded case, the simulation yields a very similar result both in 2D and 3D (top and bottom plots of Figure 4-22). As follows from the discussion on the pole unloading perceived in MQXFS5, a certain discrepancy is found between the computation and the test results, whose larger magnitude is found at nominal current. The glued simulation predicts a delta of 30 MPa, whereas the measured value in the four coils ranges from 36 MPa to 51 MPa. Apart from the contact assumption, the variability in coil geometry [137]-[138], in the coil material properties and the differences in preload between the four coils are also likely to cause part of the difference.

Figure 4-25 shows the results of a parametric analysis, on which the impact of the coil thermal expansion property (from 3.5 mm m^{-1} to 4.5 mm m^{-1} , as total contraction from ambient temperature to 1.9 K) and the magnet preload was assessed ($\pm 100 \text{ }\mu\text{m}$ variation in the interference key). Straightforwardly, the coil properties have a big impact in the results. However, provided that the two extreme values considered in the parametric study cannot be justified experimentally, they are just intended to be understood as theoretical limits of the model. On the contrary, the magnet preload has a very small impact on the quench delta for the bonded case. This result is consistent with the fact that the quench delta (maximum) happens at the end of the discharge, where the e.m. forces are almost zero and the additional mechanical load in the pole comes from the thermal expansion of the coil inside the structure. When the coil is bonded to the pole, the delta is just a function of the thermal expansion, which is the same for every preload level.

In general terms, the simulation reproduces correctly the observed magnet mechanical response. The model and the experiments show a quadratic dependence of the quench delta with the magnet current. This finding points out that the additional azimuthal stress seen in the winding pole is ultimately driven by the stored energy level. The highest the average coil temperature becomes, the larger quench delta is obtained due to the coil thermal expansion.

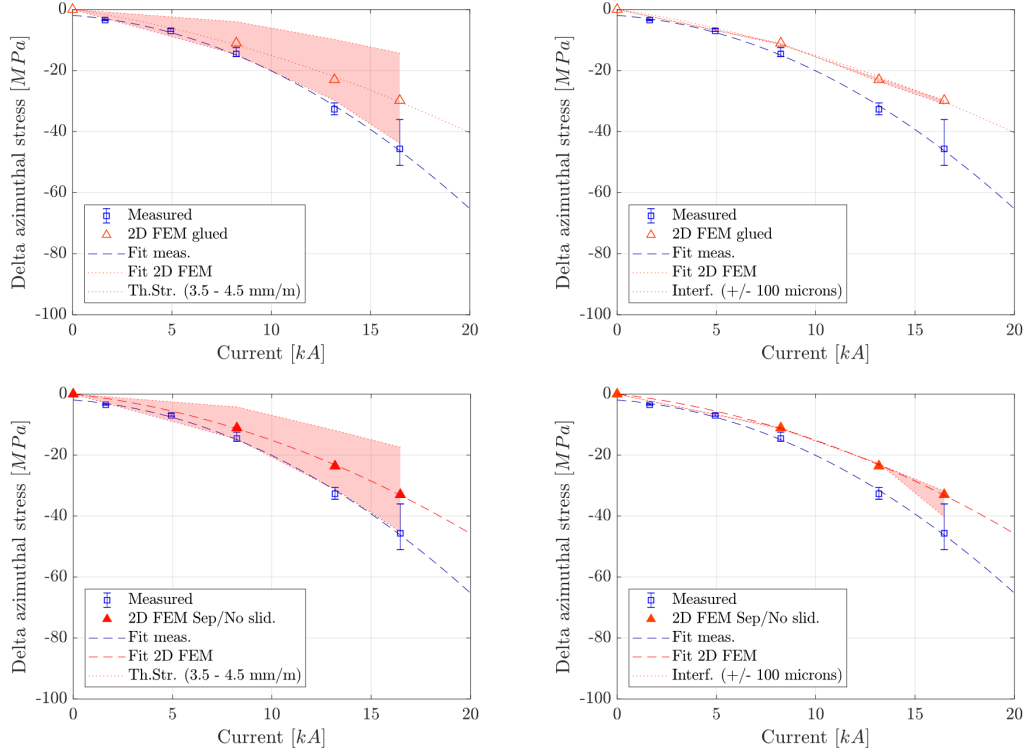


Figure 4-25: Parametric study performed with the 2D octant model. The left column shows the effects on the quench delta of the thermal expansion coefficient of the coil, while the right column shows the impact of the magnet preload. The first row depicts the results of the glued model and the second one, those from the sep./no-sliding approach.

Validation of all strain readings

Still limiting ourselves to the bonded assumption and to conclude the validation, we consider the analysis not only of the azimuthal pole stress but of all signals available in the MQXFS5 magnet¹⁶.

In Figures 4-26 to 4-29, the experimental results for the discharge at 16.47 kA are compared with their 3D simulation (3D octant model). The current profile for the quench is presented in Figure 4-26, while the rest of the plots show the stress variation from the moment the current is injected, in particular for the winding pole, shell and tie rods.

¹⁶Please note that the largest impact of the bonded condition is seen in the coil winding pole. Thus, the glued modelling approach still allows to validate the model in terms of the remaining strain sensors.

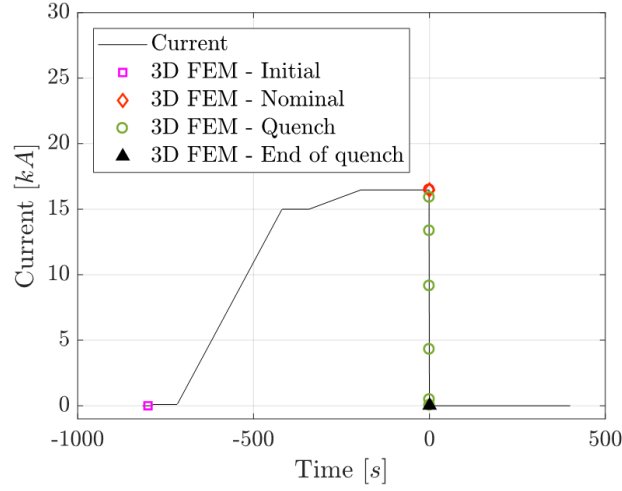


Figure 4-26: Measured and simulated magnet current. Magenta color is used to highlight the FEM initial condition, the red color indicates the value at nominal current and black shows the end of the quench discharge.

Magenta markers are used to highlight the beginning of the ramp in the model, the red color is used for the point at which nominal current is reached and the black marker depicts the end of the quench discharge.

Both, the measurements and the simulation, show a small stress impact in the shell and tie rods during the quench. The majority of the quench load is, as expected, seen by the superconducting coils. A detailed description of each particular case is found in the caption of the figures.

4.5.3 Coil stresses during the QI tests

The predicted coil stress evolution during three of the QI tests are plotted in the graphs of Figure 4-30. Regarding the azimuthal and Von Mises equivalent stress components, the values are extracted from the coil elements in contact with the winding pole. The aim is, thus, to show the increase in azimuthal compression that the pole turn block experiences due to the quench transient. The horizontal dashed lines indicate the value of stress (σ_θ and σ_{eqv}) at cryogenic temperature.

The reduction in the stored energy from nominal current to 8.24 kA results in a decrease in the induced quench stresses, as already indicated by the quench delta

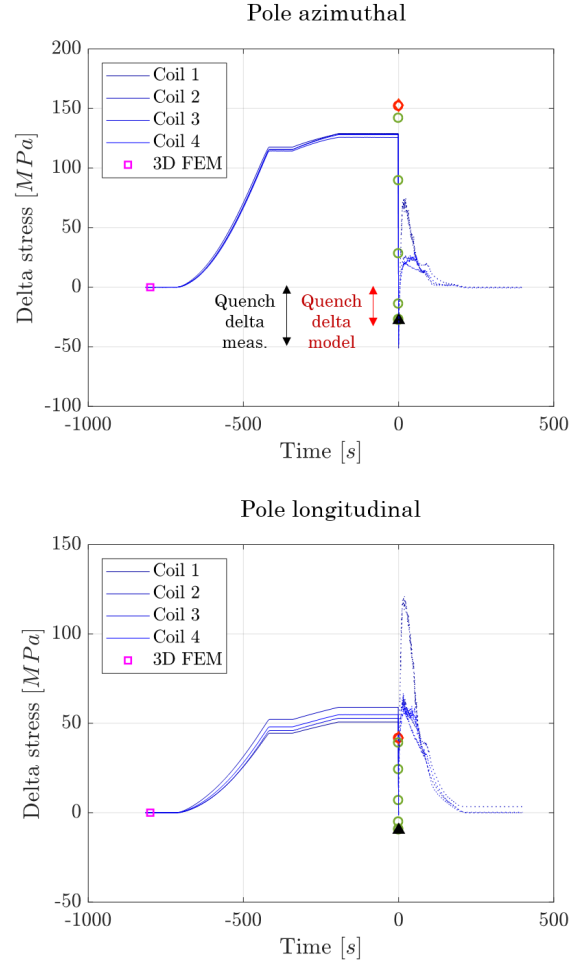


Figure 4-27: Delta in pole azimuthal and long. stress for the controlled quench discharge at nominal current. Blue lines show the experimental results for the four coils, while the markers depict the results from the simulation. The color code is consistent with Figure 4-26. Arrows show the FEM and measured delta. The effect of the quench is well captured in both directions. The glued contact assumption results in a larger stress at nominal current in az. direction and in a smaller quench delta. In longitudinal direction the relationship is inverted.

measured in the pole. For all the discharges, σ_θ and σ_{eqv} remain below 150 MPa. The radial and shear components are kept within 60 MPa in absolute value.

The plot of the coil azimuthal stress distribution for the QI at 16.47 kA, and the predicted temperature at the end of the discharge is shown in Figure 4-31. Even though the firing of the outer layer (OL) quench heaters translates into a larger temperature in the OL conductors, the largest compression is identified in the inner

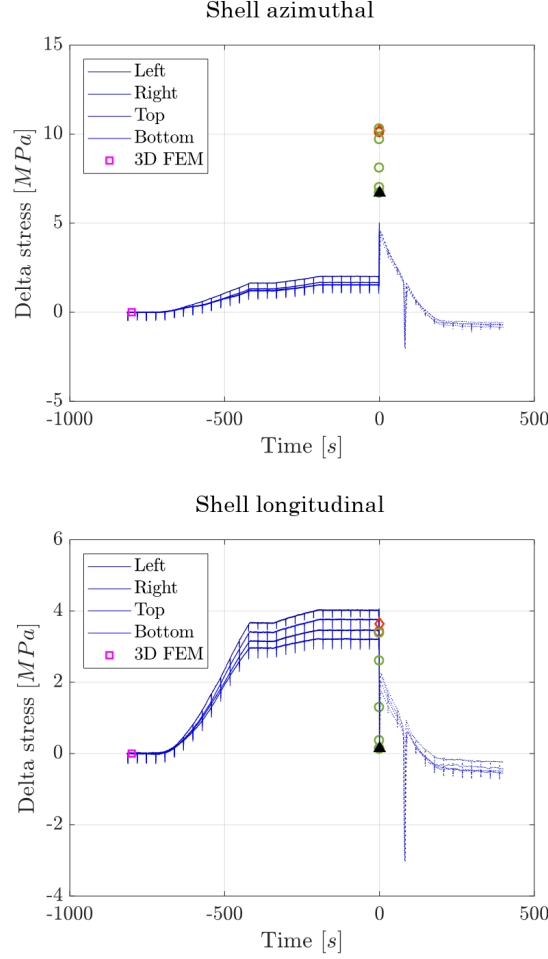


Figure 4-28: Delta in the shell azimuthal and long. stress for the same case presented in previous figure. In azimuthal direction, the model overestimates the delta due to powering. However it still predicts a very small stress increase during the first ms after the quench (first green marker above the red one), in line with the experimental evidences. On the contrary, the posterior decrease is not observed in the test results. In longitudinal direction, measured and computed signals agree.

radius of the pole turn conductor block, consistently with the magnet mechanical behavior during assembly.

4.5.4 Conclusions

The review of the mechanical measurements during a quench, and the comparison of those with the simulation results, has provided a clear understanding on the magnet response along the transient. Due to the fast nature of the quench, the temperature in

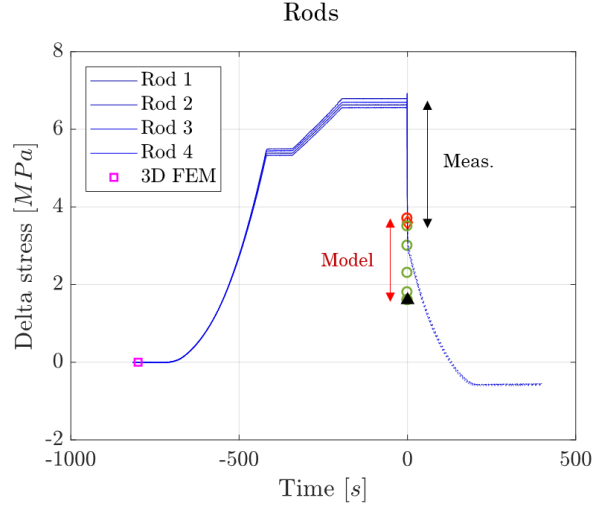


Figure 4-29: Delta in longitudinal stress measured at the tie rods. Although the model predicts a lower stress variation due to powering, the delta during a quench keeps the consistency with the measurements (arrows).

the coil increases rapidly through the discharge while the rest of the structure remains cold. Under these circumstances, the thermal expansion of the warm parts creates an additional mechanical load which adds on top of the magnet pre-stress at cryogenic temperature. The largest impact is identified at the end of the discharge, where the e.m. forces are zero and the coil average temperature is the highest. Such a behavior is well predicted by the model, and confirmed by the strain measurements.

Furthermore, the detailed revision of controlled quench integral tests has allowed us to validate the model both in terms of the 2D and 3D geometry. The winding pole experiences the biggest quench load, as follows straightforwardly from its position inside the coil. The simulation predicts an increase in azimuthal stress for the pole turn conductor of 24 MPa for the quench integral test at 16.43 kA, 18 MPa for the event at 13.18 kA and 8 MPa at 8.24 kA. Inversely, a very small impact can be seen in the rest of the magnet structure (aluminum shell and tie rods). For these last cases, stress variations in the order of less than 10 MPa are both measured and predicted¹⁷. In global terms, the model is able to catch the overall behavior of the coils and magnet

¹⁷Note that the reduced magnitude of these loads increases as well the difficulties in their comparison.

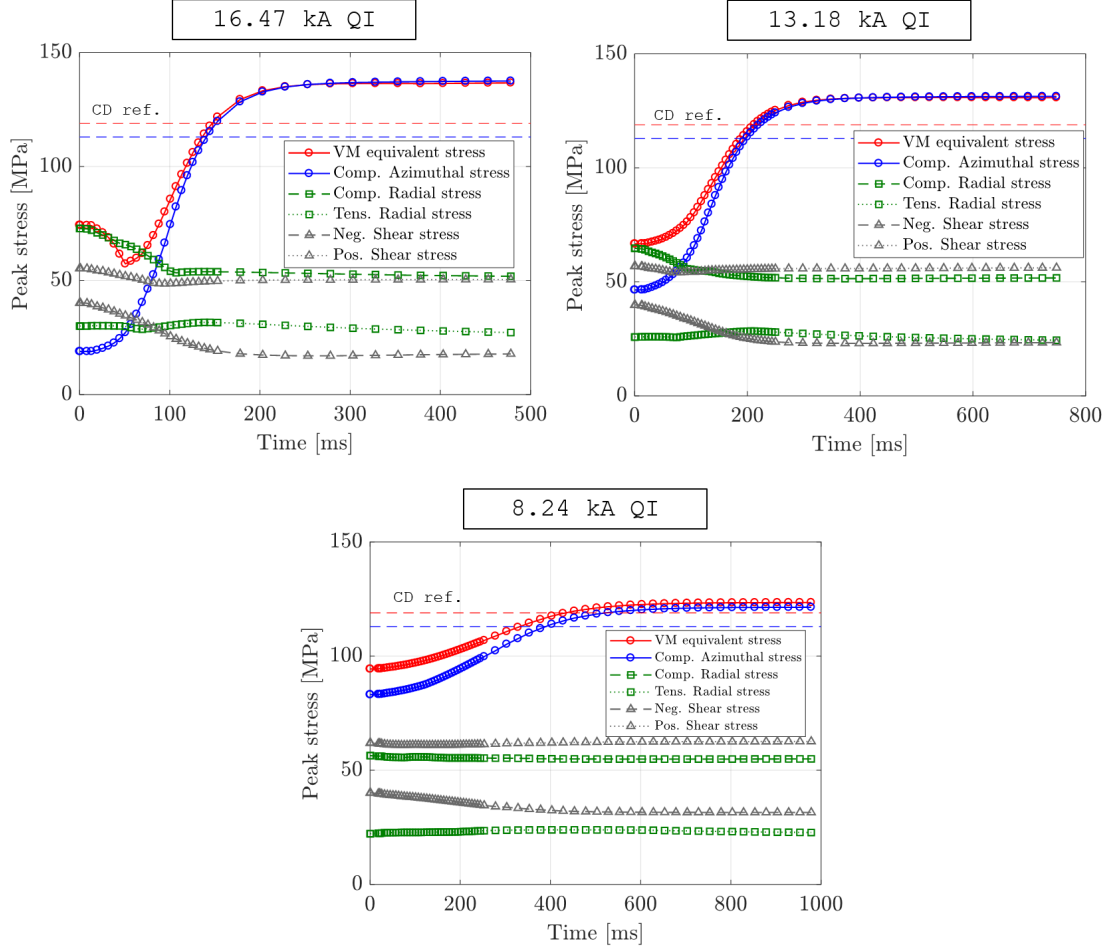


Figure 4-30: Coil peak stress evolution during three of the quench integral discharges simulated for MQXFS5. The dashed horizontal lines show the corresponding stress values after the cool-down to cryogenic temperature (red color is used for the VM equiv. stress and blue for the azimuthal stress). The azimuthal and VM equiv. stress values are extracted from the coil elements in contact with the winding pole.

structure.

4.6 Reference quench simulation

The validation exercises done for the thermal-electric and mechanical models create the necessary confidence for their use together. We are finally in position of combining the two simulations to analyze the MQXFS magnet mechanics during the reference quench at nominal operation. The study will be performed using both the 2D and

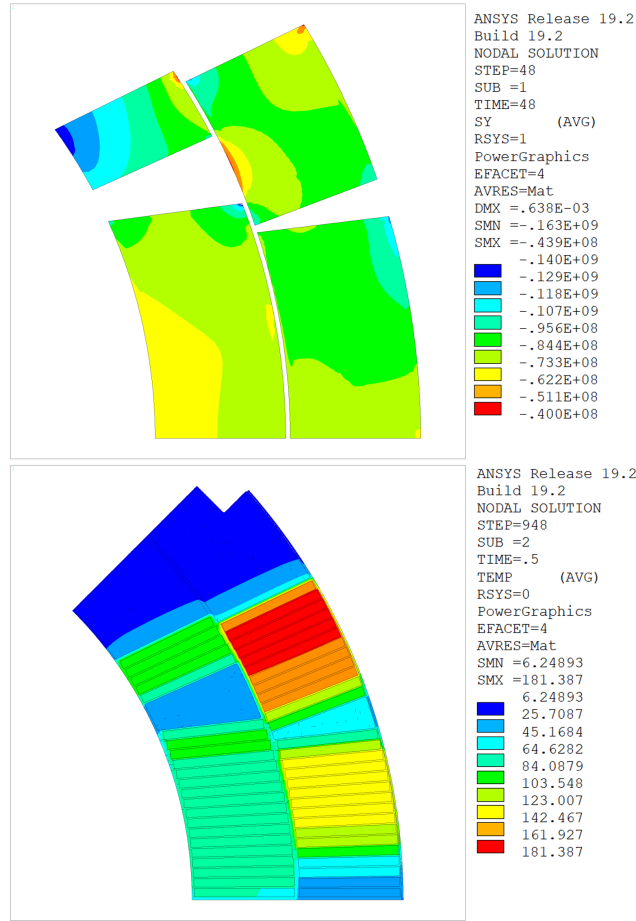


Figure 4-31: Coil azimuthal stress distribution at the end of the 16.47 kA QI discharge (top, reproduced from Figure 4-24) and the corresponding temperature distribution predicted by the multi-physics quench model (top).

3D models.

The main difference with the above-shown results is that for this case, the analysis will not focus on controlled magnet discharges, neither on a specific short model magnet. Instead, the quench will be triggered at nominal current in the peak field conductor (inner layer pole turn) and will propagate until it is detected and validated by the protection system (see Section 3.4). Reference mechanical preload targets and conductor parameters are respected here, in an attempt to simulate the most relevant case for the magnet design.

The simulation will be limited to the conservative scenario where the magnet protection relies only on the outer layer quench heaters. Considering that larger

thermal gradients are expected in the coils compared to the MQXF baseline strategy with CLIQ, the results are thus expected to prove the presence of enough mechanical margin in the magnet design.

4.6.1 The symmetric simulation approach

We start the analysis of the reference case with an important remark linked to the mechanical solution of the problem. The latter states that during the simulation of a non-provoked quench, symmetric boundary conditions are not anymore correct due to the presence of an initially quenched conductor in the coil. The appearance of such a singularity requires the use of full geometry models for the study of the transient.

Notwithstanding this strong condition, the reduced computational requirements of the octant models (specially in 3D) constitute a great advantage for the analysis that should be exploited. It is worth, then, to review the exact difference between full and symmetric approaches prior to carry out the analysis.

With this goal, the two-dimensional octant and full magnet geometries have been employed to study the impact of the boundary conditions in the coil stress results. The simulation of the reference quench has been performed in both models and stopped before the end of the discharge (at the point of maximum peak temperature). As shown in Figure 4-32, the general analysis of the case as symmetric leads to a small overestimation of the coil stresses during a quench, in the order of 10 MPa (or equivalently, 6 %) for the peak values. This is a particularly important result, since it proves that the fastest and simplest simulation lays in the conservative side.

The outcome of the check can be explained by the fact that for the octant case, the hot-spot results to be symmetrically expanded in the magnet, thus creating a larger mechanical load in all coils. We will limit ourselves to the study of such conservative symmetric cases, therefore allowing to perform the 3D calculation in the reduced octant geometry.

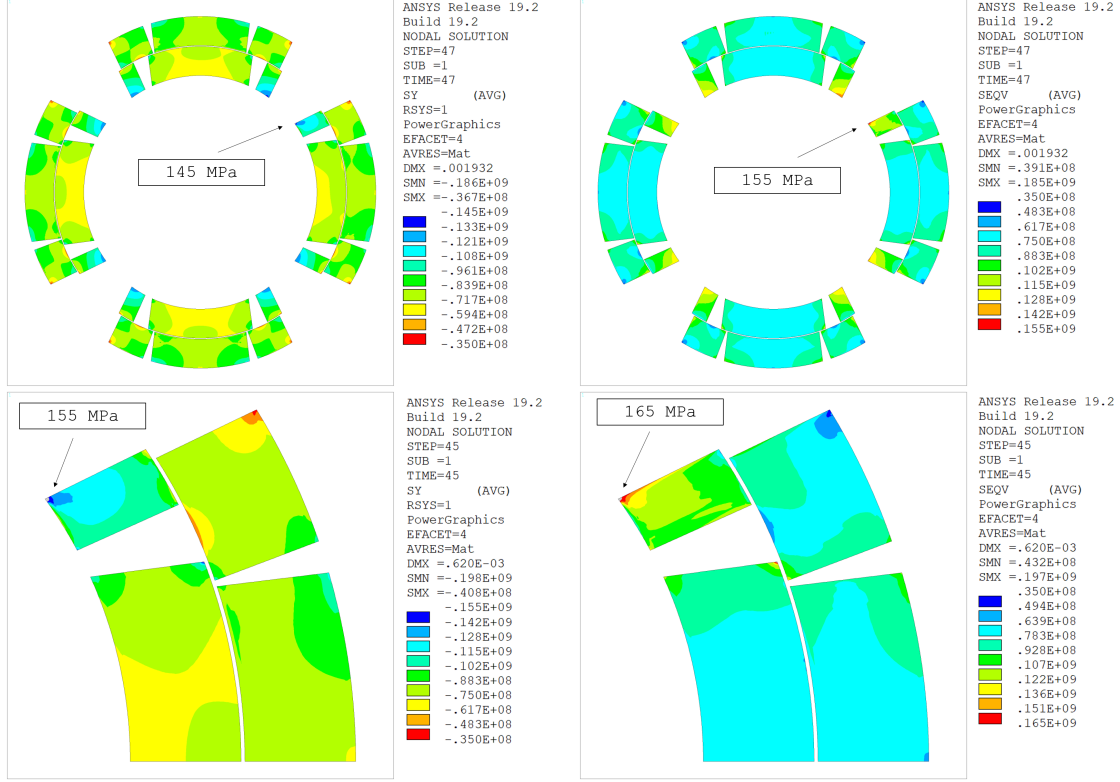


Figure 4-32: Azimuthal (left) and VM equiv. stress (right) contours using the full (top) and symmetric (bottom) magnet models. The plots show the results of the reference quench simulation at $t = 0.4$ s. The results prove the conservative nature of the symmetric approach.

4.6.2 2D Results

The first results shown in this section correspond to the 2D mechanical analysis of the reference quench case. The contour plots in Figure 4-33 show the coil stresses in the main spatial directions (using a cylindrical coordinate system) and the Von Mises (VM) equivalent stress distribution at the end of the discharge. At this last time step, which corresponds to the instant just before the re-cooling process starts, all the stored energy has been released by Joule effect within the coils and the largest stresses have been found.

In terms of the peak Von Mises stress, the simulation predicts a maximum value of 165 MPa located in the pole turn conductor block where the hot-spot is located (the temperature map is shown in Figure 3-26 of the previous chapter). A very

similar maximum value is obtained for the azimuthal component in the same location, corresponding to 155 MPa in the corner of the block. The radial stresses (σ_r) are also described in the same figure. There, the maximum stress is shared between one of the outer layer blocks of the coil (in the contact region with the collars) and the pole turn block: σ_r equals 60 MPa. Actually, the effect of the thermal expansion is clearly visible in the new stress distribution of the radial stress. An additional compression against the aluminum collars, not visible in the cool-down step, appears after the quench.

Whilst the just mentioned plots correspond to the end of the quench discharge, the stress evolution can be seen in Figure 4-34. As for the QI test graphs, the azimuthal and VM stress values are extracted from the coil elements in contact with the pole (those showing a larger compressive load). The curves highlight the importance of an extra delta of 40 MPa when the values are compared to the cool-down ones.

Such a stress increase becomes extremely important in terms of conductor degradation, where it should be always guaranteed that the peak is maintained below safe limits. Moreover, repetitive quench loads provoke a cycling effect in the coil that can be particularly dangerous if any type of cracks are present in the coil, for instance, at the conductor or the impregnation level.

To finish with the review of the coil peak stresses during a quench, it must be underlined that in the case of the azimuthal stress distribution, the dark blue contours of the plot encompass a stress level up to 135 MPa. The peak in the corner node (155 MPa) decreases sharply to a lower value few nodes away from the boundaries of the domain. We will see how very similar stress levels are obtained in the 3D simulation.

Parametric quench study

At this point of the 2D analysis, the model has already revealed important information about how the magnet behaves during a quench in nominal conditions. Still, some of the reference parameters can slightly change during the final magnet production due to variations in the conductor or differences in the magnet assembly.

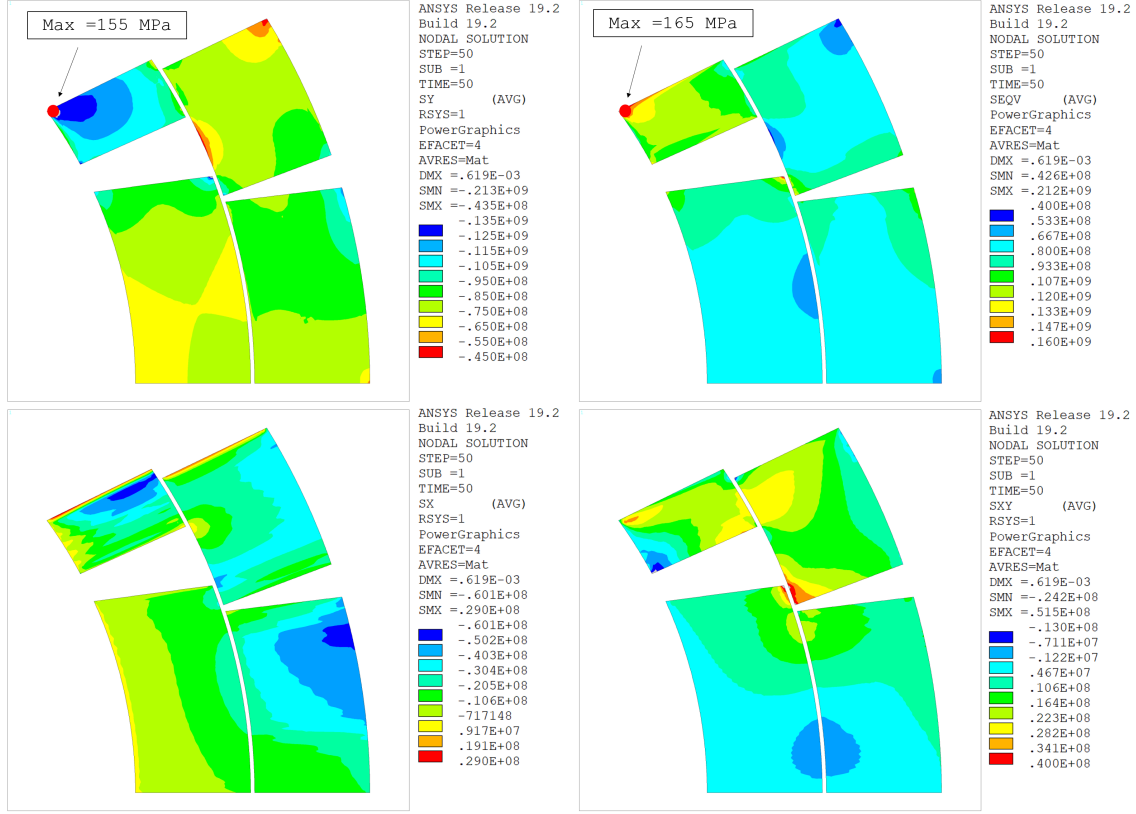


Figure 4-33: 2D stress contours at the end of the reference quench discharge. In the top row, the azimuthal (left) and VM equiv. stress (right) are shown. The bottom row depicts instead the radial (left) and shear stress (right) in the coil.

In an attempt to obtain a full characterization of the MQXF quadrupole mechanical response during a quench at I_{nom} , a detailed parametric study has been performed. The influence of the conductor parameters, the protection system reaction time, the preload level and the quench position is analyzed in next subsections.

Impact of conductor parameters in quench mechanics

The current discharge in a magnet protected with only quench heaters is largely impacted by the conductor parameters and the system's differential inductance. Variability in the strand and cable properties have therefore an effect on the quench integral obtained. The influence of the conductor Residual Resistivity Ratio (RRR) and copper to superconductor ratio (Cu:Non-Cu) in the quench peak stress is summarized in Figure 4-35. Both parameters have been modified according to the specifications

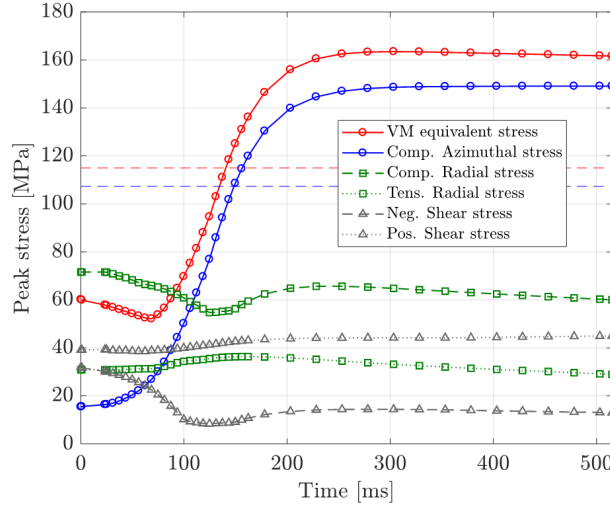


Figure 4-34: Peak stress evolution in the coil during the reference quench. $t = 0$ s corresponds to the moment the quench starts. As in the QI tests, the VM and az. stress values are extracted from the coil elements in contact with the pole. Red color is used for the VM equiv. stress, while blue is chosen for the azimuthal one. The same color code is kept for the dashed lines, which depict the corresponding constant value at cryogenic temperature (just after magnet cool-down).

established in Table 2.1, in particular: $100 \leq RRR \leq 200$ and $1 \leq (Cu : Non-Cu) \leq 1.4$.

As shown in the figure, the maximum Von Mises stress value remains almost constant despite a variation of the hot spot of about 30 K to 40 K. Further investigations showed that in terms of coil average temperature, all cases presented a constant value. Such a result is expected, and it is easy to prove that for the same dissipated energy, the final coil average temperature must be the same if the conductor specific heat is not modified. This is the case for example of a change in RRR, where the conductor C_p remains indeed unaltered. A very similar behavior is obtained when the Cu:Non-Cu ratio is modified. The change on the conductor specific heat remains as low as 2% along the temperature range of interest.

The results suggest that for the case of the homogenized conductor block, the mechanical response in terms of peak stress is mostly governed by a global/average temperature raise in the coil and not by the hot spot temperature. The same evidence was found in [200]. Additionally, the study shows that under the assumptions of the

analysis, there is not a big impact on the mechanical stresses during a quench if the design parameters (conductor specs.) are kept within the established tolerances. Variability in the conductor from magnet to magnet does not affect the mechanical response during a quench.

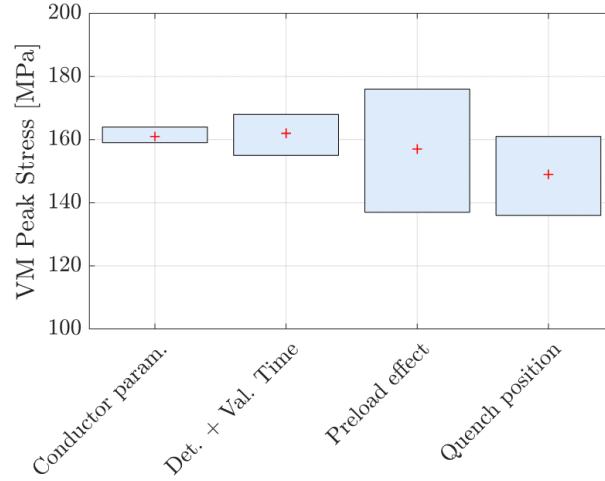


Figure 4-35: Impact of different assumptions on the VM peak stress during quench. Markers highlight the average value and blue boxes shows the range of variation.

Impact of detection and validation times in quench mechanics

How fast a quench protection system can react to a quench event is one of the most important parameters when protecting a magnet. The detection and validation time defines the time gap required by the system to identify the presence of a quench inside the magnet (t_{det}), validate the finding (t_{val}) and trigger the magnet protection. In order to prove its relevance, the reaction time has been modified in two steps of 5 ms (corresponding to 10 ms and 20 ms respectively). Results are again shown in Figure 4-35.

Since the conductor parameters are now constant, the detection and validation time alters the hot spot temperature but also modifies the average temperature in the block. This can be explained looking at the initial propagation as an external energy input coming from the power supply. An increment of ≈ 15 MPa is found for the modelled 10 ms difference.

Impact of magnet preload in quench mechanics

In a real magnet assembly, the preload applied to each superconducting coil may be different due to variations in coil geometry and due to tolerances in structural components. The experience acquired during the production of MQXF5 model magnets suggests a maximum variation of ± 20 MPa from coil to coil in a single magnet. The impact of this pre-stress variation on the quench mechanics is shown in Figure 4-35. A consistent total difference of 40 MPa is kept during the quench phase. In other words, the quench delta is not impacted by the variation of the magnet preload in the glued model (as already explained).

Impact of quench position in quench mechanics

Finally, it should be noted that during the magnet training phase, the quench location may change from quench to quench affecting the mechanical response of the system. A study on the impact of the quench position in the coil peak stresses is presented in Figure 4-36. The location has been moved ranging from both inner and outer layer conductor pole turns, to both sides of the coil wedges. The model suggests that for different quench locations inside the same coil layer, the peak stress variation remains within 12 %. A bigger difference, 23 %, can be however identified when comparing the inner layer to outer layer quenches. The largest peak stresses are reached for the events originated in the inner layer pole turn block (including IL Pole Turn and IL Wedge Top Turn, which become the worst case scenario in terms of the magnet mechanics).

The same effect is observed when reviewing the azimuthal stress seen in the winding pole, at the location of the mechanical instrumentation. The quench delta is also maximum for a quench happening at the previously mentioned location¹⁸. This is one of the reasons why the model validation was performed with provoked quench discharges, where no uncertainties on the quench position are present.

¹⁸As for the coil stress itself, there exists a certain dependence of the measurable quench delta with the quench position. A quench in the peak field conductor (inner layer pole turn) generates the larger delta, according to the model.

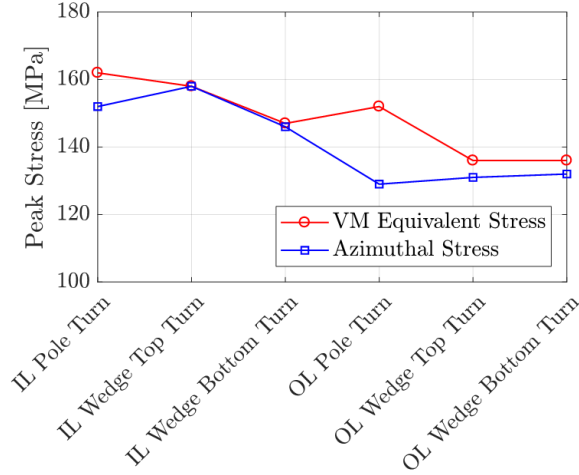


Figure 4-36: Peak stress quench position dependency.

4.6.3 3D Results

The results from the 3D analysis of the reference quench case will be shown next, bringing for the first time the necessary information on the magnet response in the 3D domain. Before going through the data, we can already anticipate that the obtained results confirm the 2D predictions for the magnet straight section. In addition, thanks to the incorporation of the third spatial component (z-axis), the simulation adds essential information on the longitudinal coil stresses and on the deformation of the magnet ends during the quench.

The section starts with the review of the coil stress state at the end of the discharge and will be closed with a summary of the main conclusions.

3D coil stress state

Figure 4-37 - Top shows the azimuthal stress distribution in the superconducting coil blocks at cryogenic temperature (left) and after the quench (right). The contour plots show a clear stress increase that becomes maximum in the position where the hot-spot temperature is located, $\sigma_\theta = 135$ MPa. Note that this peak value (located in the corner node) is reduced with respect to the 2D prediction, a fact that can be partially ascribed to the larger element size employed in the 3D model. Excluding that singularity, the 135 MPa predicted by the three-dimensional model agrees perfectly

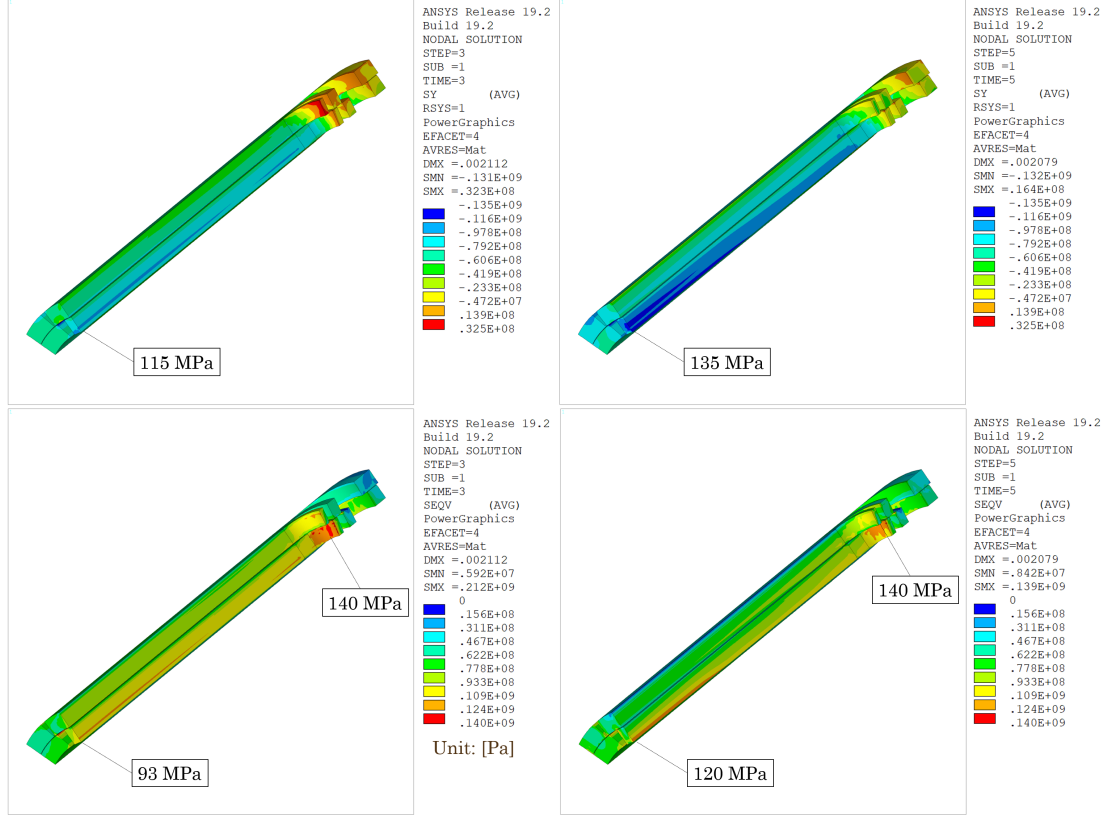


Figure 4-37: In the top: coil azimuthal stress distribution after magnet cool-down (left) and at the end of the quench discharge for the reference event at nominal current (right). The effect of longitudinal quench propagation is clearly visible in the plot. The location of the peak stress corresponds to the position where the temperature is higher. In the bottom: coil equivalent stress distribution. A peak stress close to 140 MPa is located in the coil ends.

with the 2D calculations, as previously suggested.

The increment in azimuthal compression is more visible when reviewing the results from the subtraction of the cool-down stresses to the quench case (Figure 4-38). For the conductor block in contact with the pole, the delta in stress is in the order of 20 MPa to 40 MPa, consistently with the predictions of the 2D model.

The peak Von-Mises equivalent stress obtained in the 3D computation is instead almost the same for the magnet cool-down and quench steps ($\sigma_{eqv} = 140$ MPa), and it is located in the coil ends. This time, the value is notably reduced with respect to the 2D results and not coincident in position. The three-dimensional nature of the problem is thought to be the source of the discrepancy. If the effect of the quench

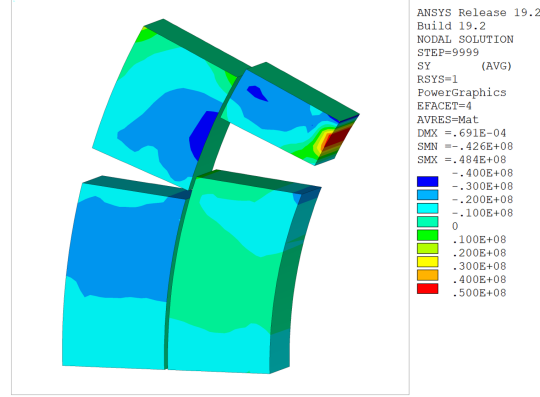


Figure 4-38: Azimuthal stress difference between the quench case and the cool-down values in the hot-spot region.

loads in σ_{eqv} is analyzed, a maximum increase ($\Delta\sigma_{eqv}$) of 27 MPa is found in the hot-spot region.

The radial stress distribution is represented in Figure 4-39 - Top. A compressive stress of 60 MPa is uniformly distributed in the coil outer layer due to its thermal expansion at the end of the discharge. The peak absolute value, 75 MPa, is found close to the position where the hot-spot is located. Similarly to the case of σ_θ , the stress delta due to quench is noticeable; $\Delta\sigma_r$ for the outer coil surface is in the order of 15 MPa - 50 MPa, while for the hot-spot location is of about 70 MPa. In both cases, the high values are motivated by the low stress levels after cool-down.

At last, with special emphasis in the coil ends, the study of longitudinal stresses is presented in Figure 4-39 - Bottom. In absolute terms, the peak stress after the quench is located in the coil ends, and equals 129 MPa (transverse to the cable broad's face). On the other hand, a compressive stress increase ($\Delta\sigma_z$) as large as 100 MPa is predicted by the model in specific points of the hot-spot region. It is however important to mention, first, that the stress at cryogenic temperature is again very low there. And second, that the isotropic coil properties assumed in the computation are likely to cause a certain error in the longitudinal results. The author is aware of different works where the anisotropic behavior of the coil material properties has been presented [198], [205]-[206]. This point needs to be further addressed in the future and complemented with experimental campaigns for the characterization of the coil

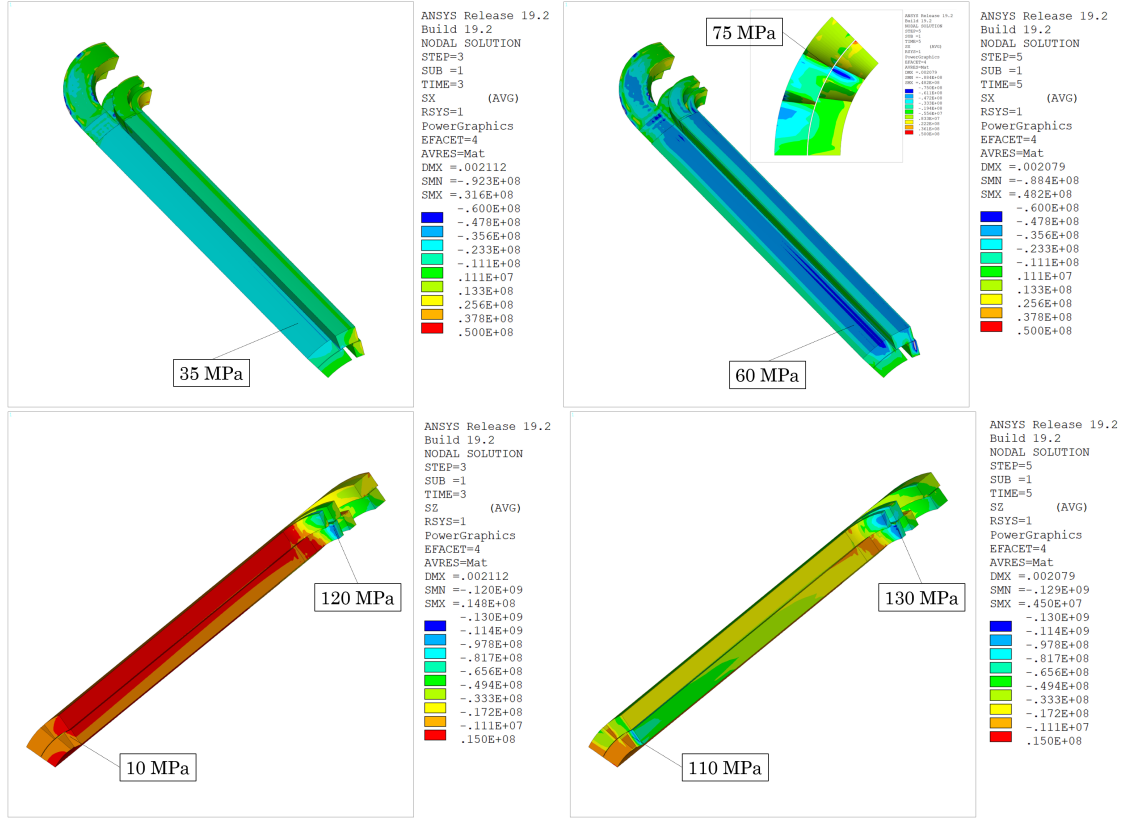


Figure 4-39: Top: Coil radial stress after magnet cool-down (left) and at the end of the quench (right). The frontal view highlights the peak value, found close to the hot-spot location. Bottom: Coil longitudinal stress contours.

material properties.

Stress gradients

In the three-dimensional space, the uneven temperature distribution originated by the initial quench propagation generates a remarkable stress variation along the coil (see for instance the σ_θ distribution in Figure 4-37). Such stress gradients can be easily put in evidence by plotting the values of the different components through a certain path (Figure 4-40).

The case of the coil straight section, where the temperature differences are larger, is particularly revealing here. The plots in Figure 4-41 show the azimuthal and longitudinal stresses along the z-axis of the magnet, for the inner and outer layer conductor blocks in contact with the pole (middle point of the block, see Figure 4-40

for the path definition). The simulation of the magnet assembly and powering steps are added for comparison. Furthermore, the corresponding 2D computation of the same quench is included by means of a red star marker.

There exists a 30 MPa gradual increase in azimuthal stress, which follows the quench propagation and coil temperature in the inner layer (Figure 4-41 - Top left). Still in this layer, far away from the hot-spot, the values become very similar to the cool-down phase. For the outer coil blocks, where the quench heaters are placed and the temperature is more homogeneous (no hot-spot), the thermal expansion of the coil rather results in a constant increase of about 20 MPa when compared to the values at cryogenic temperature.

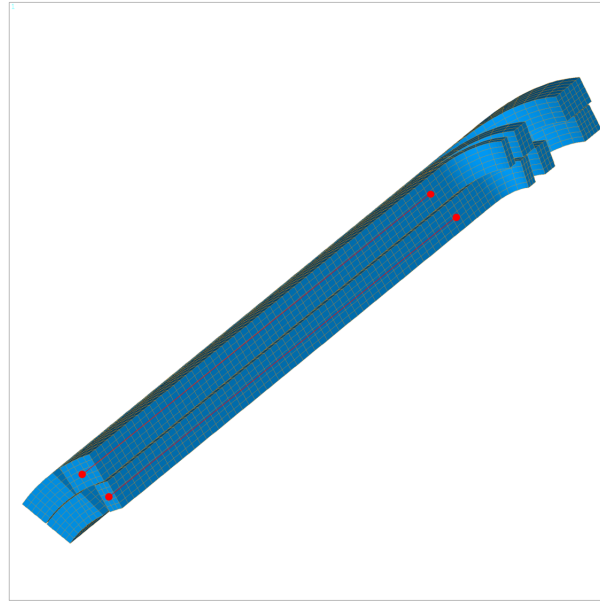


Figure 4-40: Definition of the two paths along the coil straight section.

The bottom row of Figure 4-41 shows the same type of analysis for the longitudinal stress. Despite the fact that the peak value is lower, the model unveils the largest gradient in longitudinal direction. Closely following the temperature distribution, a stress variation of 60 MPa is shown by the model. Such a behavior can be explained in a similar way to the azimuthal case. The coil experiences a relevant compression due to its high thermal expansion and due to the presence of the support structure, which

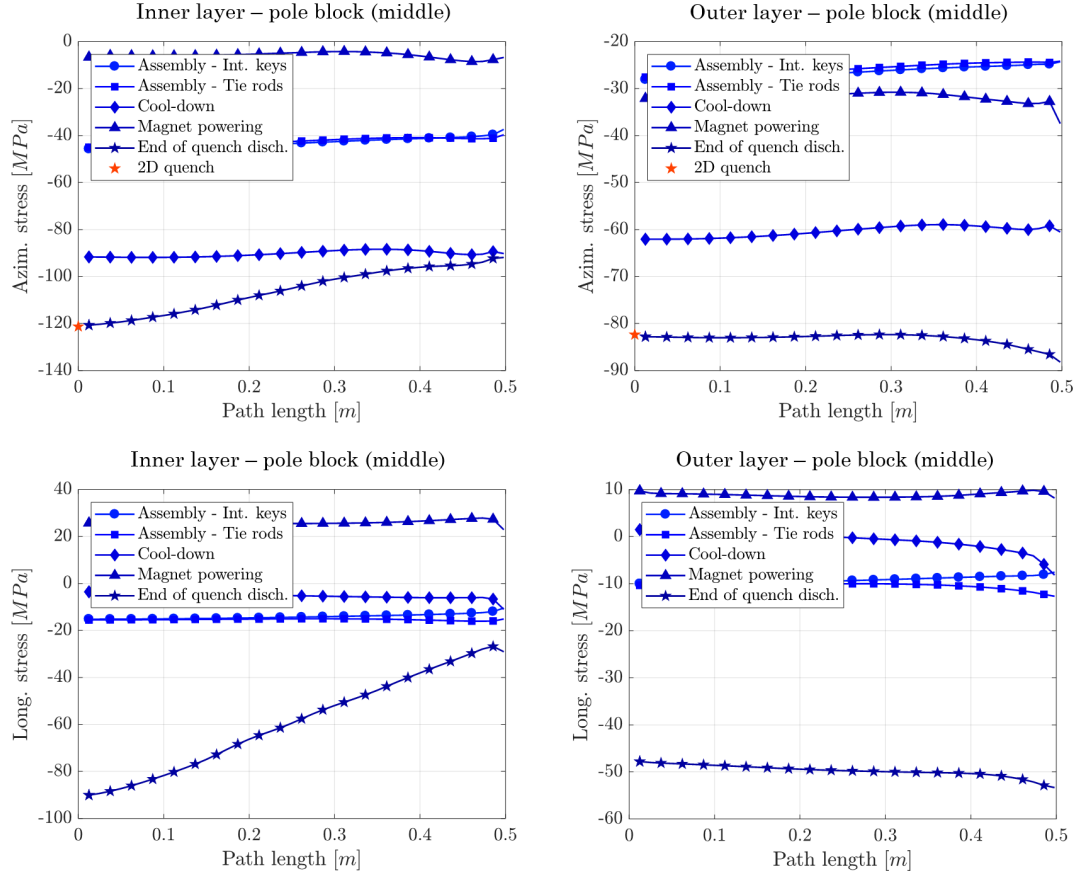


Figure 4-41: Top row) computed azimuthal stress along the two different paths. Bottom row) results for the longitudinal stress component along the same paths. The quench creates the largest stress gradient in long. direction. In addition, the delta with respect to the cool-down is maximum in the magnet centre (90 MPa), and differs slightly from the value reported in the text (100 MPa) due to the path position. The red marker adds the result from the 2D simulation.

remains cold at the end of the fast discharge. As mentioned, the results need to be carefully interpreted due to the mismatch in the longitudinal coil material properties.

Discussion

Based on the modelling assumptions, the 3D mechanical simulation confirms that new peak stresses appear in the superconducting coils during a quench. These stresses, which are consistent with the 2D predictions, are the result of the coil thermal expansion and the change in electro-magnetic forces.

As for the controlled discharges, the coil insulation and the extremely fast nature of the transient prevents the heat to diffuse into the magnet structure in the course of the first instants, and thus, the latter remains fundamentally cold. Under these circumstances, the maximum coil stresses are obtained at the end of the discharge, when the global coil temperature is the highest and the electro-magnetic forces are almost zero. In such regime, the quench loads act as a new contribution that adds to the magnet condition at cryogenic temperature.

For the investigated case at nominal current, the most relevant increase (reaching the largest coil stresses) happens in azimuthal direction and sums the contribution of 20 MPa to 40 MPa at the hot-spot location (16 % - 20 % of the magnet preload at cold).

On the other side, the presence of a temperature gradient in longitudinal direction translates into a corresponding stress variation, which cannot be predicted by the 2D model. Although this last model catches the response at the hot-spot location, the total stress difference along the length can be only studied in three dimensions. The stress variation lies in the order of 30 MPa for σ_θ in the inner layer pole block (middle of the turn), while the absolute maximum delta is of 60 MPa and identified for σ_z .

The ensemble of results have a profound impact in what regards the correct performance of MQXF magnets. We have mentioned in various occasions that the applied magnet preload defines the initial coil stress before operation. This in turn implies that if a large preload is chosen, the peak during a quench may approach dangerous limits. For instance, if the target is set to 140 MPa in the pole azimuthal stress at cryogenic temperature, the peak in the coil ($\sigma_{\theta \max.}$) at the end of the quench becomes 170 MPa. Special care needs to be taken if an iteration on the magnet preload is performed.

For the rest of the stress components the peak values have been found to stay below 100 MPa. Despite the fact that 3D shear stresses have not been explicitly mentioned, their maximum is contained below 40 MPa, with the exception of some corners reaching up to 70 MPa. The XY component computed in the three-dimensional model agrees perfectly (for the straight section) with the 2D plot shown in Figure 4-33. XZ

and YZ are instead kept below 30 MPa. These peaks are thus similar to the ones observed after magnet cool-down. According to preliminary measurements performed at 77 K [207], in general terms the shear stresses lie in the acceptable range.

To conclude, and as suggested by the small impact seen during the validation tests, the mechanical stresses in the magnet structure have been also found to remain within the corresponding limits.

4.7 The average temperature simplification

The coupling of the multi-physics quench simulation with the mechanical model has allowed us to perform a rigorous analysis of the magnet mechanics during a quench. For all the presented studies, the detailed temperature distribution in the coil has been transferred to the mechanical model through various load steps.

An interesting observation was however made during the revision of the 2D model results. Considering the conductor regions as homogenized coil blocks, it has been found that the global mechanical response of the magnet is not governed by the peak hot spot temperature. Rather than that, the average temperature of the blocks seems to define the mechanical problem. If this is true, the detailed temperature map could be replaced in the simulation by a simplified distribution, on which the average temperature of each component is imposed.

Figure 4-42 shows the stress results (for the reference quench) in a path that travels from 0° to 45° along the mid-radius of the coil inner layer. The copper wedges and the titanium pole are included. The blue curve corresponds to the detailed temperature map approach, while the orange one corresponds to the mentioned average simplification. As it can be seen, the problem solution is almost the same in both cases.

Although not applicable to the 3D geometry, the result has important implications. In the first instance, it allows to decrease the complexity of the analysis by reducing the modelling requirements to the average block temperature. Secondly, it permits to couple the mechanical FEM simulation in ANSYS to any other numerical quench

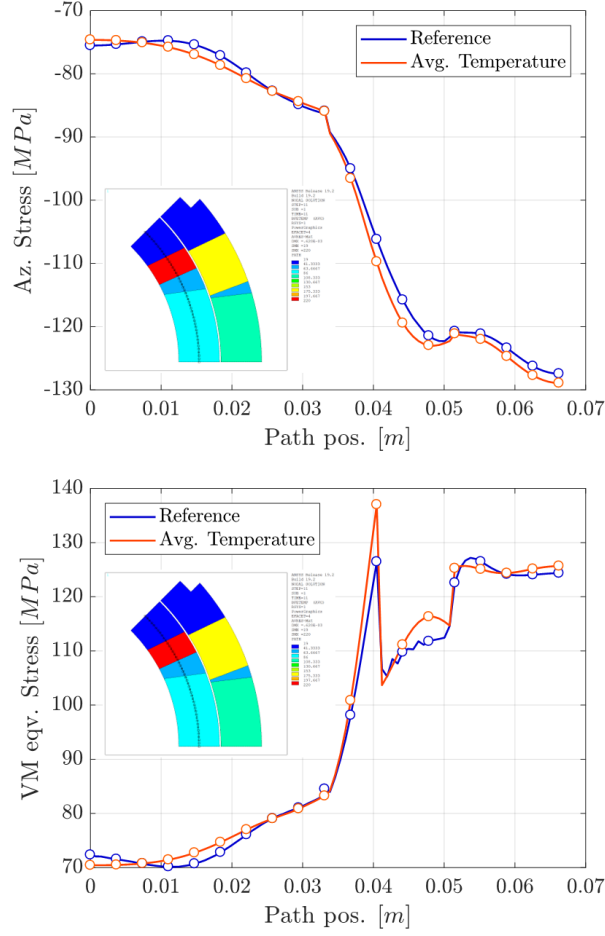


Figure 4-42: Azimuthal stress (top) and Von Mises equivalent stress (bottom) along a path that follows the mid-radius of the coil inner layer, from 0° to 45° . The 2D results from the detailed and average temperature approaches are depicted. Note that the cooper wedges and the titanium poles are included in the path.

code, which can provide a temperature output and the e.m. forces during the current decay.

4.8 A deeper insight into the link with the magnet stored energy

In the parametric study of Section 4.6.2, it was concluded that the conductor properties have a reduced impact in the magnet mechanical response during the quench

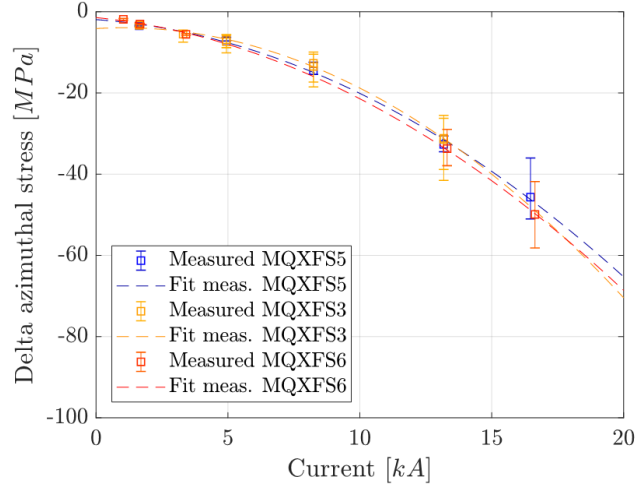


Figure 4-43: Quench delta measured for various controlled discharges in diverse MQXFS magnets. The average of the four coils is shown with markers, while the vertical lines define the measured variation between them. Dashed lines correspond to the quadratic fit of the avg. values.

(provided that the properties are kept within the project tolerances). Furthermore, it was also shown during the chapter that the effect of the magnet mechanical preload in the quench delta is negligible for the glued assumption, and small for the 2D separation-allowed case.

If these findings are applied to the tests of the MQXF short model magnets, one would expect to obtain a very similar trend for the quench delta measured in controlled discharges for all magnets. Note that during the production of the short models, the magnet units usually differ in the conductor properties and the mechanical preload.

Interestingly, Figure 4-43 shows the measured quench delta for all MQXFS experiments on which mechanical measurements were available. In perfect agreement with the simulation outcome, the discharges in the three magnets provide a very similar extra level of compression in the winding pole. The difference between the three can be indeed attributed to variations in the mechanical preload and in the exact geometry of the coils.

It is also worth mentioning that while the results of MQXFS3 and MQXFS5 are extracted from SG readings acquired at 100 Hz, the points from MQXFS6 correspond instead to FBG readings with a sampling rate of 2 kHz.

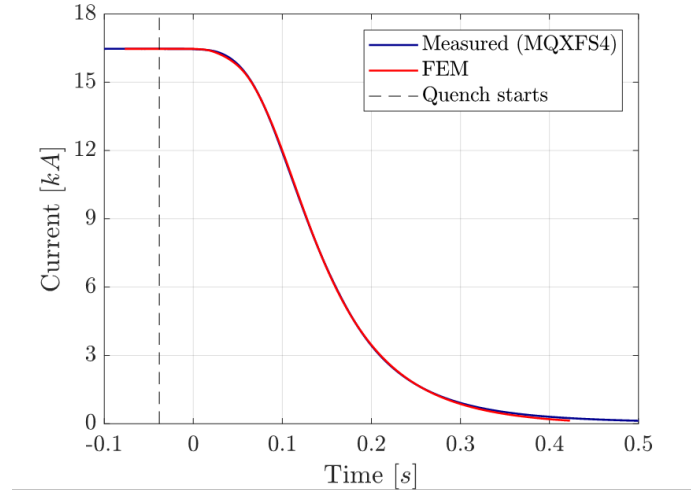


Figure 4-44: Computed and measured current discharge for the High MIITs test in MQXFS4.

All together, this new set of information confirms the important lessons learnt from the 2D and 3D mechanical simulations.

4.9 Application to High MIITs tests

The chapter is closed with the analysis of the so-called High MIITs tests performed in the framework of the MQXF magnet development phase. These tests provide extremely interesting information to our research, since they seek to prove the absence of performance degradation when the system is subjected to large quench integral values, i.e. to large temperatures.

In particular, the experiments consist in the manual firing of a single quench heater strip in the magnet (using the minimum energy required to initiate the quench). The discharge of the heater circuit provokes the quench in the corresponding coil block, in a similar fashion as a usual QH delay test. However, in this case, the magnet is protected using the rest of the quench heaters, whose triggering time is delayed.

The High MIITs tests were performed as a part of the protection campaign for the MQXFS4 magnet (considered as the reference short model, of proven successful performance). A high-field QH strip was chosen to initiate the quench at nominal

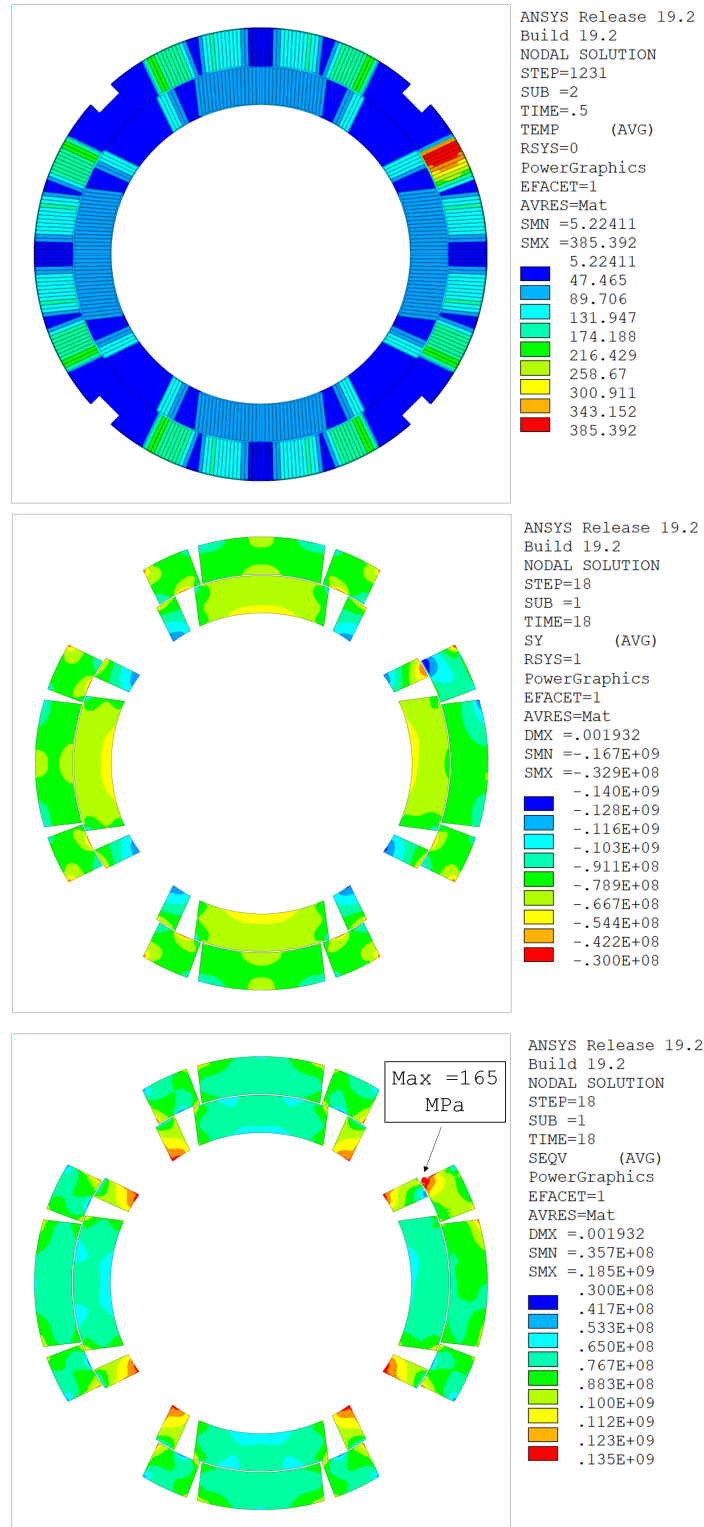


Figure 4-45: The first row shows the detailed temperature distribution at the end of the magnet discharge. In the middle, the azimuthal stress distribution is shown (also for the end of the discharge). The third row plots the VM. equiv. stress values.

current level, while the delay was defined in order to achieve a hot-spot temperature of 400 K. Upon the test, a verification quench was done and confirmed that the magnet did not suffer from any remarkable degradation on its performance.

Employing the 2D full magnet models (both for the multi-physics quench simulation and the mechanical one), the experiment has been reproduced using our FEM methodology. The exact delays of the protection and the minimum QH energy have been introduced in the computation.

The test and simulated results in terms of the magnet discharge are depicted in Figure 4-44. A total quench integral value (from the moment the quench starts in the coil) of 39.05 MIITs is obtained experimentally. The value compares perfectly with the simulated quench load of 39.77 MIITs. Moreover, the model confirms that the hot-spot temperature in the coil reaches almost 400 K as desired.

In what regards the corresponding mechanical analysis, the results for the coil azimuthal and VM stress are shown in Figure 4-45. The simulation predicts a $\sigma_{\theta_{max}}$ of 140 MPa, located in the outer layer block where the quench starts. Note that unlike the standard quench integral tests, the large temperature increment shifts the peak to the outer layer of the coil. The maximum VM equivalent stress is located in the same position and equals 165 MPa in the corner node. In the vicinity of this node, the stress decreases to 135 MPa as shown in the bottom plot.

Even though the electro-mechanical limits of the MQXF conductor will be assessed in next chapter, we can already anticipate that the obtained maximum values are compatible with the absence of performance degradation observed after the test. The appearance of other failure modes at higher temperature cannot be discarded (for example the damage of the conductor or impregnation due to excessive temperature), nevertheless, the model provides a powerful tool to distinguish which is the contribution of the induced thermal stresses.

It should be also retained that if the same hot-spot temperature is reached in the inner layer pole block, which experiences a larger compression at cryogenic temperature, the peak value could increase above the tolerable limits.

4.10 Conclusions

The coupling of the FE mechanical models with the multi-physics quench one has allowed us to present the two-dimensional and three-dimensional studies of the magnet mechanical behavior during a quench. The most important results were summarized in each of the chapter's sections.

The models show an increase in the coil stresses during the quench due to the rapid thermal expansion of the conductor blocks inside the cold structure. In general, the new peak values need to be carefully considered for new high-field magnets, in order to guarantee their sound performance.

In the case of the MQXFS quadrupole, conservative simulations and experimental data confirm that the nominal operation of the magnet is not compromised if the design parameters are preserved. An additional contribution to the most relevant transverse coil stresses (azimuthal direction) of 20 MPa - 40 MPa with respect to the values after magnet cool-down has been found for the examined reference case¹⁹. In longitudinal direction, a compressive stress increase ($\Delta\sigma_z$) as large as 100 MPa is predicted by the model in specific points of the hot-spot region, under the assumption of isotropic properties for the coil material. Of great importance, the stress delta has been found to be mainly dependent on the average coil block temperature. The latter is determined by the magnet stored energy, protection scheme and the additional energy input coming from the initial quench propagation.

¹⁹Such a stress delta is seen in the pole turn block, corresponding to the peak field region of the coil.

Chapter 5

Electro-mechanical characterization of Nb₃Sn superconducting wires

The last chapter of the thesis describes the experimental campaign carried out for the electro-mechanical characterization of Nb₃Sn superconducting wires. Fundamentally, the study focuses on the Niobium-Tin strand used for the production of the MQXF magnets. In the first instance, a short introduction to the strain sensitivity of practical superconductors is provided. Secondly, the experimental setup for the tests will be presented. Following the review of the main results and their analysis, we will closure our research by exploring the impact of the found conductor limits.

5.1 The importance of mechanical loads

The windings¹ of SC magnets are subjected to a wide variety of mechanical loads during their life-cycle, as anticipated in the introduction. In essence, the majority of these loads are a direct consequence of the large stored energies and electromagnetic forces that follow from the system's operation.

The magnet type, but also its particular design, will determine the dominant mechanical stresses to which the conductor will be exposed. In the case of accelerator magnets, an important piece of information on their behavior can be unveiled by re-

¹And by implication, the superconducting cables and/or wires of which they are composed.

viewing the case of the MQXF quadrupole. The three-dimensional structural analysis presented in last chapter shows high stress values during the assembly and powering phases, which are of special relevance in azimuthal and longitudinal directions. The coil peak stress was identified in transverse direction to the cables broad's face, and remained below 150 MPa. Apparently not compromising the good performance of the magnet, this stress level gives however a clear order of magnitude for the big loads experienced in Nb₃Sn designs. In addition, it should not be forgotten that new high field concepts (with design parameters beyond those of the MQXF magnet), entail maximum stresses in the coils that are in the order of 150 MPa to 200 MPa [100], henceforth approaching new limits for the technology.

Our work has also confirmed that exceptional peak loads may arise in the coils of accelerator magnets during fast quench transients. As it was shown, the multi-physics nature of the quench discharge translates into the largest mechanical loads being absorbed by the conductor, which experiences a rapid thermal expansion inside the cold restraining structure. If not carefully controlled, the maximum stresses in the coils could surpass the initial design targets.

Taking into consideration all the previous aspects, it is possible to conclude that the capability of the conductor to withstand elevate mechanical loads becomes a critical feature for the sound operation of new high-field units. In what concerns Nb₃Sn magnets, the complexity of the problem is straightforwardly accentuated by the strain sensitivity and brittleness of the material. For reasons that are now evident, the study of the superconductor response to high mechanical loads is an essential topic that needs to be thoroughly approached.

In the rest of the chapter, we will complement the valuable analysis of the magnet mechanics during a quench with an important study devoted to characterize the electro-mechanical properties of Nb₃Sn superconductors. The results from this investigation are expected to prove the presence of enough mechanical margin for the MQXF quadrupole, but also to have a profound impact in the design of future magnets.

5.2 The strain dependence of the superconducting properties

Mechanical strain has an influence on the critical properties of all superconducting materials. From a practical standpoint, the effects of mechanical loads in the conductor's performance can be usually grouped in two different regimes:

- In the first, the application of large loads results irretrievably into the damage of the superconductor. The main driving mechanism for the performance degradation is the appearance of cracks in the medium. As a matter of fact, all practical superconductors can be subjected to this type of failure, where the propensity to fracture will be determined by the mechanical properties of the material.
- In the second, the application of lower mechanical loads induces just a deformation of the superconducting phase. The latter can be understood in terms of its elastic and plastic components.

Disregarding for the moment the case on which the breakage occurs, it is well recognized today that the second regime affects differently the main practical superconductors. For some materials, such as the popular example of Nb-Ti, the effects of mechanical strain on the critical properties are marginal. For some others, like the Nb₃Sn A15 compound, the strain state strongly affects their superconducting performance.

The physical basis of the strain dependence is found in the deformation of the superconductor's crystal structure that happens when the load is applied. In other words, the load acts on the microstructure of the material introducing a distortion in the crystallographic lattice and thus, influencing the electron-phonon interactions². On the macroscopic scale, strain has an impact on the available critical current (or

²In the case of A15 superconductors, the strain effects can be understood in terms of the lattice distortion and in terms of the induced changes in the lattice vibration modes.

critical current density), this quantity being directly dependent on T_c and B_{c2} ; the critical temperature and the upper critical field.

To conclude this short introduction, it is important to emphasize that the investigation of the strain sensitivity phenomenon in practical superconductors, both theoretically and experimentally, is a topic largely covered in literature [38] - [77]. In next sections, we will restrict our development to the phenomenological description of the Nb₃Sn problem and to the experimental assessment of its consequences.

5.3 A closer look to Nb₃Sn

As mentioned in the introduction, Nb₃Sn is an A15 brittle intermetallic compound (showing a noteworthy propensity to fracture), whose sensitivity to mechanical strain has been already highlighted in the previous paragraphs. For the most relevant cases in magnet design, the action of mechanical loads translates into a reduction of the superconductor's performance, i.e. a reduction of its critical current (I_c).

Attending to the classification of the loads presented in Section 5.2, the decrease of the available I_c can be broken down to two well-distinguished categories. On one side, there is a reversible component of the reduction, which is fully recovered when the external load is removed, and is associated to elastic deformations in the superconducting phase. On the other side, there exists an irreversible and thus permanent component, which is ascribed to the plastic deformation of the stabilizing matrix³ and to the appearance of cracks in the superconductor [39].

The discriminant between both regimes can be conventionally defined using an irreversible stress (σ_{irr}) or strain (ε_{irr}) limit that depends on the load type and loading conditions. Tailored experiments, performed on single wires or full superconducting cables, are normally used to evaluate these important limits, which provide essential information for the magnet design. In addition, the same type of tests can be used as

³It has been explained in the introduction that practical superconductors include a normal stabilizing matrix that allows for stable operation. When loads are applied and then removed, the brittle Nb₃Sn (showing no plastic deformation) can be still subjected to mechanical strain due to the residual effects induced by the soft stabilizer (in the case of copper, annealed after the thermal heat treatment).

well to characterize the electro-mechanical response of the superconductor through a certain range of applied loads.

However, before going through the description of those experimental measurements, it is essential to recall first the main mathematical expressions describing the critical surface of Nb₃Sn superconductors. Although they were shown in the introduction, a deeper understanding of the equations is needed to approach the strain sensitivity problem.

The two next subsections present the theory fundamentals behind our adopted parametrization for Niobium-Tin superconductors, and the application of the equations to the fixed temperature regime.

5.3.1 The scaling law for J_c in practical Nb₃Sn superconductors

Significant efforts have been made in the past years to quantitatively understand the effects of mechanical strain in practical superconductors. These works comprise from tests devoted to the observation of the lattice deformations using high-energy X-ray diffraction methods [58], up to the more general (and previously mentioned) measurements of I_c in wires or cables subjected to mechanical loads.

Of special importance for our thesis are the last type of experiments, whose origins for the Nb₃Sn accelerator technology can be traced back to the early 1980's by Ekin and co-workers. In their research, the authors analyzed the response of the superconductor to mechanical loads by measuring the critical current of short wire sections subjected to uni-axial tensile loads [61].

Interestingly enough, since then, the study of axially loaded samples has led to the appearance of different parametrizations for the critical current of Niobium-Tin conductors [60]. Most of these parametrizations, presented by multiple groups during the past decades, can be understood as diverse attempts at finding a generic scaling of the Nb₃Sn critical surface⁴.

⁴The description of the so-called Unified Scaling Law (USL) in [60], provides a concise overview on this topic. Additionally, and although trivially included in the nature of the scaling law, it is

Our mathematical expressions introduced in Chapter 1 stem from the work of Botura and Bordini, who postulated their parametrization for the ITER Nb₃Sn strand production [77]. Therefore, for a complete understanding of the set of equations, the definition of the scaling principle is given hereinafter. The own words of the authors are reproduced [77]:

- *One of the most important results in the attempt to model the dependency of the critical current density of Nb₃Sn on field, temperature and strain was to recognize that J_c scales with these three variables. In simpler terms, this means that the shape of the dependency of J_c on one of the variables (B, T or ε) is maintained when the other variables are changed. The most useful form of scaling law for J_c is obtained making the additional assumption that the dependencies can be separated as follows:*

$$F_p = J_c(B, T, \varepsilon) \times B = Cg(\varepsilon)h(t)f_P(b) \quad (5.1)$$

where F_p is the pinning force density, B the magnetic flux density, C is a constant and the three functions $f_P(b)$, $h(t)$ and $g(\varepsilon)$ describe the dependencies on reduced field, reduced temperature and intrinsic strain⁵. This separable form is indeed practical, but it is just a mathematical statement to be supported by results. However, if substantiated, separation of functions allows the determination of the three dependencies with a reduced number of measurements.

Hence, the hitherto unknown significance of each term of the parametrization in Chapter 1 is now revealed using Equation 5.1:

highlighted here that the parametrizations are postulated for the reversible regime of the I_c vs strain curve, obtained from axial tests.

⁵The intrinsic strain is defined as $\varepsilon_0 = \varepsilon - \varepsilon_m$, where ε_m is the strain value at the I_c maximum (see Figure 5-1). Defined in this way, ε_0 corresponds roughly to the strain state of the superconducting material itself.

$$f_P = b^p(1 - b)^q \quad (5.2)$$

$$g(\varepsilon) = s(\varepsilon) \quad (5.3)$$

$$h(t) = (1 - t^{1.52})(1 - t^2) \quad (5.4)$$

where for simplicity, the Equations 1.5 - 1.6 are reproduced here⁶:

$$b = B/B_{c2}^*(T, \varepsilon)$$

$$t = T/T_c^*(0, \varepsilon)$$

$$B_{c2}^*(T, \varepsilon) = B_{c2}^*(0, 0)s(\varepsilon)(1 - t^{1.52})$$

$$T_c^*(B, \varepsilon) = T_{c0}^*(0, 0)[s(\varepsilon)]^{\frac{1}{3}} \left(1 - \frac{B}{B_{c2}^*(0, \varepsilon)}\right)^{\frac{1}{1.52}}$$

In the reversible strain region, the main parameter describing the Nb₃Sn strain dependence is the function $s(\varepsilon)$, also known as the strain function. The latter can be defined as the ratio at $T = 0$ K between the upper critical field at a certain strain state and the upper critical field at zero strain⁷ [60], [76]:

$$s(\varepsilon) = \frac{B_{c2}(0, \varepsilon)}{B_{c2}(0, 0)} \quad (5.5)$$

Several scaling laws [60] - [77] have been proposed for the strain function of Nb₃Sn wires under applied axial strain. Following the exponential form proposed in [76], $s(\varepsilon)$ is expressed in terms of the strain state of the superconductor as:

$$s(\varepsilon) = \frac{e^{(-C_1 \frac{J_2+3}{J_2+1} J_2)} + e^{(-C_1 \frac{I_2^2+3}{I_1^2+1} I_1^2)}}{2} \quad (5.6)$$

where C_1 is a constant depending on the particular strand used; I_1 is the first

⁶The star superscripts denote effective values, i.e. obtained extrapolating I_c data.

⁷Once again, the definitions apply to critical current measurements on axially loaded samples in the reversible (or moderate strain) regime.

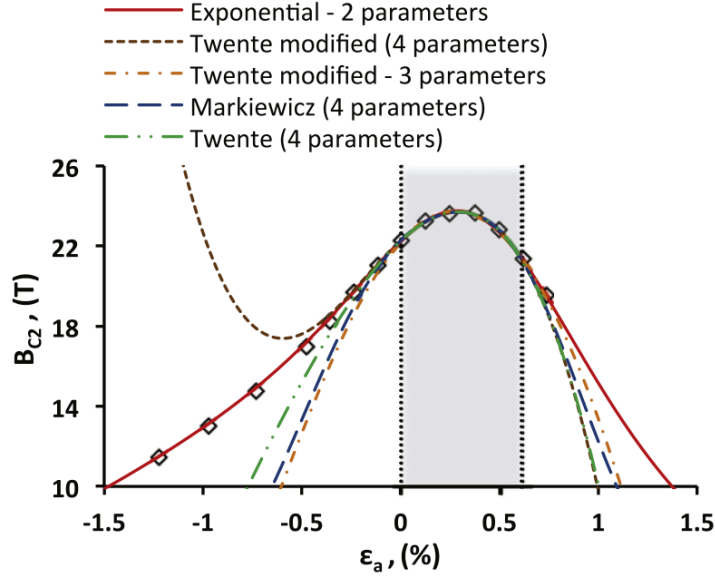


Figure 5-1: Example of the B_{c2} evolution for a Nb_3Sn bronze wire subjected to axial strain. The diamond markers represent the data derived from critical current measurements and the curves show the fits calculated using only six points around $B_{c2 \text{ Max}}$ (those limited by the vertical dashed lines) for different scaling equations. The exponential form described in the text is shown in red. Figure reproduced from [76].

invariant of the strain tensor and J_2 is the second invariant of its deviatoric part, which can be calculated as a function of the principal strains:

$$I_1 = (\varepsilon_1 + \varepsilon_2 + \varepsilon_3) \quad (5.7)$$

$$J_2 = \frac{1}{6} [(\varepsilon_1 - \varepsilon_2)^2 + (\varepsilon_2 - \varepsilon_3)^2 + (\varepsilon_3 - \varepsilon_1)^2] \quad (5.8)$$

Figure 5-1 shows an example of the upper critical field evolution as a function of the applied axial strain for a bronze Nb_3Sn wire, where the typical shape of the dependence can be clearly identified.

Furthermore, it is to be noted that although Equation 5.6 was postulated to predict the effects of mechanical strain in axially loaded wires, the function has been recently proven to hold as well for loads applied in transverse direction [211] - [213]. Due to its three-dimensional nature, it can be then used to analyze the impact of mechanical

strain in the critical current of the superconductor when the load (transverse or axial) is kept in the reversible regime.

The fixed temperature simplification

Since all the experimental tests done in the framework of our research have been performed at a fixed temperature, the most important information for our studies comes from the I_c analysis in such conditions.

A simplification of the previous equations can be obtained by just considering the strain scaling law. The resulting expression, which is only valid for a fixed temperature⁸ $T \ll T_c$ (see [60] for all the details.), reads:

$$F_p = J_c(B, \varepsilon) \times B = Cg(\varepsilon)f_P(b) \quad (5.9)$$

where f_P is the general pinning force function that can be conveniently parametrized using Equation 5.2, with $b = B/B_{c2}(\varepsilon)$. In the moderate strain region for Nb₃Sn multifilamentary superconductors $g(\varepsilon)$ takes the form [60]:

$$g(\varepsilon) = \frac{B_{c2}^*(\varepsilon)}{B_{c2}^*(0)} \quad (5.10)$$

Substituting in Equation 5.9, one gets:

$$F_p = J_c(B, \varepsilon) \times B = C \left(\frac{B_{c2}^*(\varepsilon)}{B_{c2}^*(0)} \right) b^p (1 - b)^q \quad (5.11)$$

Thus, solving for J_c ,

$$J_c(B, \varepsilon) = \left(\frac{C}{B_{c2}^*(0)} \right) \left(\frac{B_{c2}^*(\varepsilon)}{B} \right) b^p (1 - b)^q = C^* b^{p-1} (1 - b)^q \quad (5.12)$$

The terms in Equation 5.12 can be finally rearranged in a more convenient form, yielding:

$$J_c(B, \varepsilon) = C^* \left(\frac{B}{B_{c2}^*(\varepsilon)} \right)^{p-1} \left[1 - \left(\frac{B}{B_{c2}^*(\varepsilon)} \right) \right]^q \quad (5.13)$$

⁸For example at 4.2 K or below in Nb₃Sn.

This simplified equation determines the field and strain dependence of the Nb₃Sn critical current density in fixed temperature conditions, such as those applying during our experimental measurements. We will make an extensive use of this equation in later sections.

5.3.2 Distinguishing the sources of performance degradation

Important conclusions on the effects of mechanical loads in the Nb₃Sn superconductor performance can be already extracted at this point of the chapter. The most valuable information comes from the analysis of the previously presented mathematical expressions.

It follows from Equation 5.13 that in the so-called reversible regime and when considering the simplified operation at a fixed temperature well below the critical one, the effects of mechanical strain in the critical current of Nb₃Sn conductors can be attributed to a change in their strain dependent upper critical field, $B_{c2}(\varepsilon)$. In such a case, the superconductor's sensitivity to mechanical loads increases as the magnetic field approaches the upper critical one [$B \rightarrow B_{c2}(\varepsilon)$].

This observation holds for the effects of elastic strain in the Nb₃Sn material, but also for the effects of the residual strain induced by the plasticization of the copper matrix. Even if we emphasized the fact that the derivation of our equations was based on the absence of permanent effects, it should be clarified that in the case of plastic effects in the matrix, the physical mechanism behind the I_c reduction corresponds still to the deformation of the superconducting phase. In the same way as the elastic strain, the induced residual strain has an effect in $B_{c2}(\varepsilon)$.

A second and similar conclusion can be obtained from the analysis of the opposite scenario, i.e. the case where only the presence of cracks in the superconductor is considered. From a practical point of view, the effects of fractures in the superconducting wire should translate into a reduction of the available area for the current transport. However, the effect in $B_{c2}(\varepsilon)$ would be much smaller due to the less severe change in the strain state⁹. In simpler words, the sensitivity to mechanical strain should not

⁹Cracks generate a different strain state in the material. For example, strain release mechanisms

change with the magnetic field in this last case.

Though both previous statements may seem trivial, the application of these findings to the analysis of experimental results becomes a powerful strategy for distinguishing the dominant degradation mechanism. Let's imagine for instance that a permanent decrease in the superconductor performance is observed, but no variation in $B_{c2}(\varepsilon)$ is measured. Under these circumstances, it would be possible to conclude that the primary degradation mechanism corresponds to the appearance of filament cracks. We will tackle this subject deeply in Section 5.6.3.

5.4 Measurement strategies for practical superconductors

The need for experimental setups aimed at studying the response of practical superconductors to mechanical loads has been clearly motivated in the previous sections¹⁰. On this subject, we have briefly explained how critical current measurements on axially loaded wires have played a key role in the postulation of the main equations describing the Nb₃Sn critical surface. Following a more general approach, suchlike sub-scale tests (in wires or cables) are essential tools for the complete electro-mechanical characterization of the superconductors and for the definition of their safe operational limits.

When compared with full-scale tests, on which the conductor performance is analyzed as a part of the whole system (a magnet, for instance), the experiments on single wires or cables decrease the complexity of the study, the cost and the time needed to obtain useful results. Nevertheless, in order to be representative of the final configuration, they require the construction of special test setups reproducing the complex conditions experienced in the windings of superconducting magnets.

In fact, recreating the operational environment in sub-scale tests is a very involving activity due to the complexity of the system. We recall that as far as Nb₃Sn

in some components cannot be discarded.

¹⁰Please note that we restrict our description to the accelerator magnet domain.

accelerator magnet's coils are concerned, they are in a large extent wound using insulated Rutherford cables composed of many single wires transposed together. The cables are ultimately resin-impregnated, conforming a solid winding pack that is intended for an homogeneous distribution of the mechanical stresses inside the coils. At last, the conductor is normally subjected to the action of combined loads, which are far from the simplified scenarios reproduced in most laboratory-scale setups.

In the present days, the most common strategy for the design of Nb₃Sn experiments corresponds to the construction of test devices aimed at the application of uni-directional loads. Among these, and due to their large magnitude in new accelerator magnets, the study of the loads acting in transverse and longitudinal directions has become the center of interest for the community. In the same context, to determine the real impact of the magnet peak stresses arising during a quench, the analysis of the two load types becomes as well crucial for our research.

A careful description of the main setups used for the study of the transverse and longitudinal load effects in the Nb₃Sn superconductor behavior can be found in [58], [210]. The various experimental solutions range from critical current measurements performed in full cables down to those done in single wires [38]-[59].

5.5 Experimental campaign description

Our experimental campaign for the electro-mechanical characterization of Nb₃Sn superconductors has been carried out at the Group of Applied Superconductivity at University of Geneva. With regard to the specific type of tests performed, the campaign was based on the investigation of single superconducting wires subjected to the separate action of transverse and axial loads.

The scope of the study was to analyze the strain dependence and the irreversible reduction of the conductor critical current, when tested in conditions that reproduced their state inside the windings of the MQXF accelerator magnet.

On this last subject, and in order to extend the results from the single wires to the real magnet case, special emphasis was put in the reliable recreation of the magnet

operation conditions. Two aspects must be clearly pointed out:

- For the experiment on which the transverse loads are applied, the samples have been resin-impregnated to simulate their behavior as a part of the full coil. A 4-wall configuration for the application of the load was chosen, as shown in Subsection 5.5.4.
- Moreover, since during the production of the superconducting cables the wire geometry is usually distorted from its virgin shape, both round and 15 % rolled samples have been investigated as a part of the campaign¹¹.

In the next pages, we will introduce the characteristics of the investigated wires and the measurement devices used for each type of test. We will finish by providing a quick overview on the main results obtained at the group before our work.

5.5.1 Investigated wires

A set of identical RRP Nb₃Sn round and 15 % rolled wires were selected for the tests. The samples have been extracted from one of the production billets used for the MQXF project at CERN.

With a diameter of 0.85 mm, the conductor is composed of 108 sub-elements embedded in a high-purity Cu matrix following the 108/127 hexagonal restack (see the strand details in Chapter 2). A picture of the round wire cross-section before reaction is shown in Figure 5-2.

Reproducing what is done for MQXF superconducting coils, the samples have been reacted following the reaction heat treatment indicated in Table 5.1.

5.5.2 Measurement setup: I_c vs. axial strain

The critical current measurements under axial strain were performed using a Walters spring (WASP) device. In this instrument, proposed by Walters et al. [214], a one-piece sample holder with the geometry of a thick helical spring hosts the wire as shown

¹¹The rolled samples seek to mimic the deformation of the superconducting wires (which compose the windings of real accelerator magnets) due to cabling.

Table 5.1: RRP 0.85 mm 108/127 reaction heat treatment

Temperature Plateau	Ramp Rate
210 °C/48 h	25 °C/h
400 °C/48 h	50 °C/h
665 °C/50 h	50 °C/h
Cool down	100 °C/h

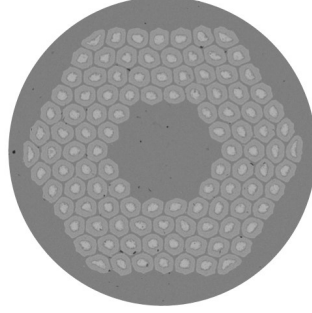


Figure 5-2: RRP 0.85 mm 108/127 investigated wire's cross section.

in Figure 5-3. The desired axial strain is applied to the sample as a result of the spring deformation, which is induced by a controlled rotation of one of its extremities. The rotation angle determines the strain level, while its direction specifies the compressive or tensile behavior [215].

Two copper terminals in the bottom and top of the WASP provide the means to a proper current injection in the samples. The lower terminal is firmly attached to the measurement shaft (described later), whereas the upper one has two degrees of freedom: rotational and axial, to accommodate for the deformations of the sample holder.

For an uniform application of the loads, the wires are soldered all along their length to the WASP support. The material selection for the latter must ensure the elastic behavior of the device through the complete strain range at which the measurements are performed. At University of Geneva, both Ti6Al4V and CuBe sample holders are used.

In parallel, the choice of the material has important consequences for the global mechanical behavior of the system and of the measured specimen. It should be

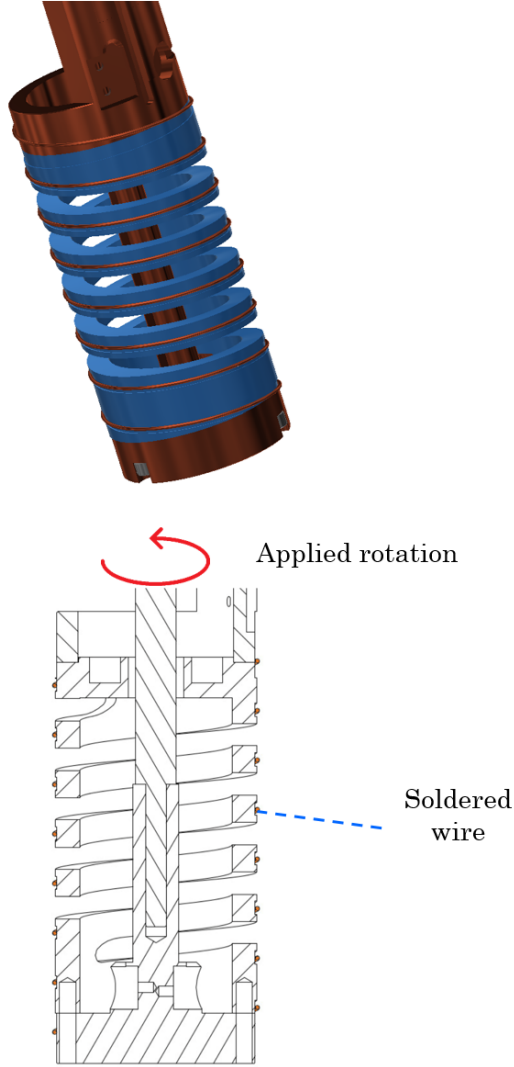


Figure 5-3: Principle of the Walters spring (WASP) design. The superconducting wire is soldered inside a rectangular-shaped groove. The axial strain is exerted by applying a torque to the spring's upper end.

noted that in this type of tests, there exists a certain difference between the thermal contraction of the support and the one from the wire. Thus, during the cool-down process to cryogenic temperature, an unknown mechanical load is always exerted to the sample. To evaluate the real value of the axial strain applied to the wire an additional measurement is needed, on which the so-called zero-applied axial strain state is identified. The exact details are explained in the next subsection¹².

¹²Precisely, the zero applied strain state corresponds to a test condition that ensures that no

With respect to the strain distribution in the specimen, an important consideration comes from the fact that the samples are subjected to a radial strain gradient due to the geometry of the support. The wire regions close to the spring surface see a lower axial strain than those lying on the opposite side of the wire. To account for this, the strain value is usually expressed at the center of the wire. The following correction is applied:

$$\varepsilon(R + r) = \varepsilon(R) \frac{R + r}{R} \quad (5.14)$$

where $\varepsilon(R + r)$ is the strain in the middle of the wire, $\varepsilon(R)$ the strain in the spring surface, R is the radius of the Walters spring and r is the radius of the sample. A thorough analysis of the mechanical behavior of the sample holder, including the study of the strain uniformity, can be found in [216].

On the other hand, for the execution of the tests, the WASP is attached to a larger measurement probe that accommodates all the necessary auxiliary components and provides the required structural support. This probe has been designed to fit in the variable temperature insert (VTI) of our main cryostat, which has a sample space diameter of 52 mm.

The rotation to the sample holder is applied via a DC-motor equipped with an opto-encoder, which is located at the top of the measurement shaft (outside the cryostat). A gearbox and two piezoelectric sensors for torque evaluation complete the system. The relationship between the applied rotation, the torque and the sample strain is established thanks to the device calibration using resistive strain-gauges.

Regarding the tests reported in this book, the axial WASP measurements were done at $15\text{ T} < B < 19\text{ T}$ and at 4.2 K where the magnetic field was always applied parallel to the spring axis and generated by a solenoid magnet. The superconducting to normal transition has been measured using voltage tap pairs, which were placed

external loads are exerted to the wire. Under these circumstances, the superconducting phase is just subjected to the mechanical strain arising from the thermal contraction mismatch (from the heat treatment reaction temperature to the cryogenic one) between the different materials of the composite wire.

along the spring path¹³. Each of them is separated by a distance equal to the length of a full turn (126 mm), thus, allowing to impose an stringent criterion for critical current determination: $0.1 \mu\text{V cm}^{-1}$. To conclude, the polarity of the current in the sample is selected so that the electromagnetic forces are exerted towards the center of the WASP.

The zero-applied axial strain measurement

The main goal of the "zero applied axial strain" measurement is to study the wire's critical current in a condition that ensures that no axial strain is exerted to the wire. By design, the Walters spring experiment for I_c determination has the drawback of presenting an unknown mechanical strain component in the sample, which is applied during the cool down process. As mentioned before, the unknown strain arises as a result of the differential thermal contraction between the support and the specimen, and makes difficult to define the real value of strain at which the wire is subjected.

To remove this contribution from the test, a special procedure was envisaged at University of Geneva. It is carried out as follows: 1) initially, the superconducting wire is soldered only to the Cu current terminals at the end of the spring, 2) before the cool down process, the spring is rotated at room temperature in the direction of compressive strain, resulting in the separation of the specimen from the support, 3) in this position, the sample is cooled down in a strain-free process, 4) finally, the critical current is measured gradually increasing the amount of tensile strain.

As long as the specimen remains in strain-free conditions, the critical current is not affected by the rotation angle applied. The point at which I_c starts to increase, indicates that the wire begins to be subjected to tensile strain. By observing the I_c plateau, one can determine the value of I_c at zero applied strain ($I_{c0 \varnothing strain}$).

Once $I_{c0 \varnothing strain}$ is set, the sample is warmed up and soldered all over the length. A final standard WASP measurement provides the rest of the information.

¹³Keithley nanovoltmeters are employed for the data monitoring.

5.5.3 Measurement setup: Stress vs. strain

Pure mechanical stress-strain measurements have been also performed as a part of our study. This type of tests provide the necessary information to transform, for example, the I_c vs. ε curve obtained from the WASP into the corresponding I_c vs. σ one.

The stress-strain experiment follows a standard procedure on which straight wire samples are subjected to uni-axial tensile loads at cryogenic temperature (4.2 K). Clamp extensometers with a gauge length of 12 mm are used for the displacement readings, whereas piezoelectric force sensors located near the sample are employed for the force readings.

5.5.4 Measurement setup: I_c vs. transverse stress

The I_c vs. transverse stress measurements were carried out using a modified Walters spring device, known as the Compressive-WASP (C-WASP) [39], [217]-[218].

In the C-WASP, the sample is placed inside a U-shaped groove (4-wall configuration) and impregnated using epoxy resin. This configuration creates a quasi-hydrostatic stress state, whose goal is to reproduce the conditions of the superconducting wires that operate inside the windings of accelerator magnets.

As it can be seen in Figure 5-4, which shows a three dimensional model of the test setup, the sample holder is composed of two parts: a movable lower part, able to translate axially, and a top part, which is fixed to the measuring probe. The transverse force is applied by means of a pulling rod that acts on the lower part of the sample holder, displacing it towards the top fixed anvil and applying the desired compression. The anvil extends over a length of 126 mm, defining the U-shaped cavity where the impregnated wire sits. The rest of the cavity's dimensions can be adjusted for different wires as function of their diameter. For our tests, the cavity remains 1.15 mm in width and 2 mm in height.

Two copper terminals (top and bottom) ensure the current injection in the sample

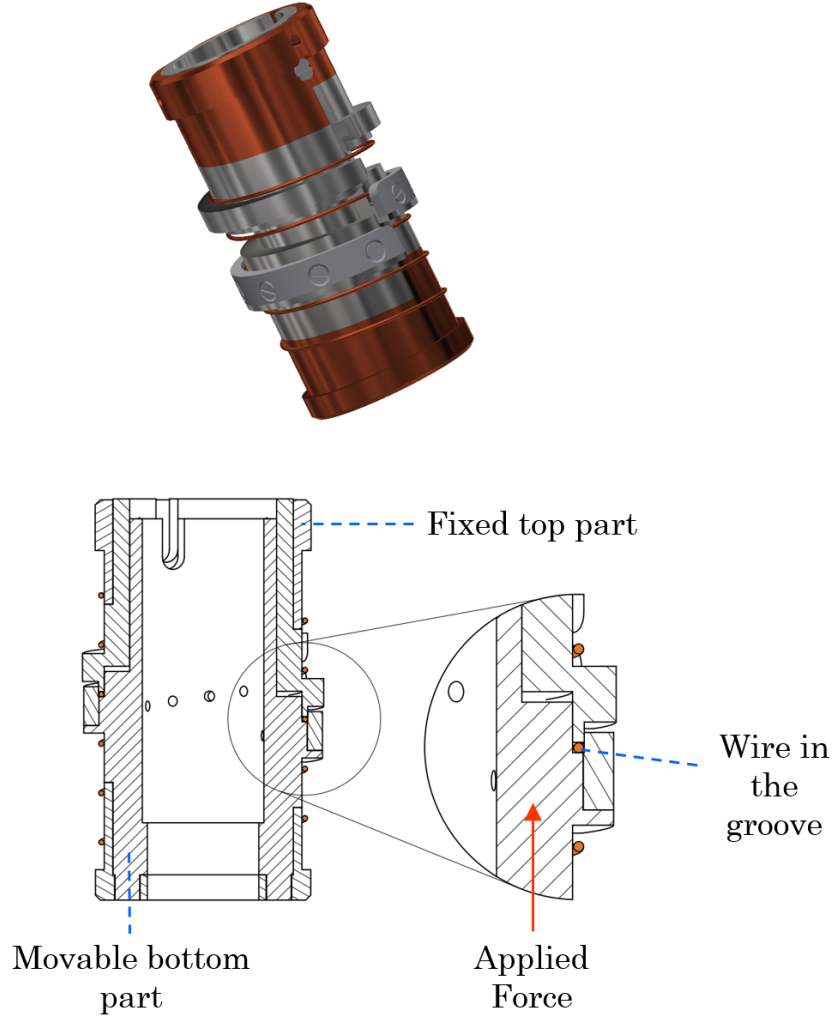


Figure 5-4: 3D model and cross-sectional cut of the C-WASP device conceived by Seeber et al. at University of Geneva. The sample holder is made of Stainless Steel 316L and Copper OFHC.

as in the case of the WASP device¹⁴. The central parts, on which the wire is just in contact, are made instead of stainless steel.

Regarding the sample impregnation, it has been performed using a de-gassed mixture of epoxy resin (type L + hardener L provided by R&G Faserverbundenwerkstoffe) and filler (thixotropic agent provided by SCS-Füllstoffe) in a 100:40:2 weight ratio. The choice for this impregnation system relies on its favorable characteristics for ma-

¹⁴The wire is soldered over 250 mm.

nipulation and on its similar measured elastic modulus when compared to the one of CDT-101K resin used for the impregnation of the MQXF Nb₃Sn coils [219]-[220].

As for the case of the axial experiment, the C-WASP is mounted in a larger measurement probe and the tests are performed under magnetic fields ranging from 16 T to 19 T. The force is applied by means of a DC-motor equipped with an opto-encoder, a gearbox and a precision ball screw. One sensor at room temperature (strain gauge based) and one sensor close to the sample at cryogenic temperature (piezoelectric) measure the applied load. The force exerted, indeed, is kept constant by a PID regulation loop that ensures a variation lower than 20 N. The main characteristics are summarized in Table 5.2.

Finally, the superconducting to normal transition is measured using voltage taps, placed at the entrance and exit of the groove. They are separated by the distance of a complete wire turn, 126 mm. The same criterion of the axial tests is adopted for the critical current determination, i.e. $0.1 \mu\text{V cm}^{-1}$. In addition, it is worth to highlight that the 126 mm distance is considerably longer than the filament twist pitch of usual Nb₃Sn wires. Hence, every filament is affected in the same manner and regardless of its position inside the wire.

Table 5.2: Technical specifications for the C-WASP test

Sample holder diameter	39 mm
Maximum force	40 kN
Maximum current	1000 A
Gauge length	126 mm

5.5.5 Background: The transverse tests

Prior to our work, several measurements on Nb₃Sn superconducting wires subjected to axial and transverse loads were performed at University of Geneva [39], [58], [59]. The tests cover the study of both RRP and PIT wires that were produced under different strand architectures and geometries.

Whilst the experiments on I_c vs axial strain are nowadays a well-established prac-

tice in the Nb₃Sn community, only a reduced number of references have investigated in detail the effects of transverse loads. Due to its direct influence on our research, it is then essential for us to review the main conclusions extracted from the previous C-WASP tests.

On this account, the results from diverse measurements on PIT and RRP samples [221] have confirmed that the electro-mechanical response of Nb₃Sn impregnated wires to transverse strain is determined by a combination of different factors, which are intrinsic and extrinsic to the wires. They include:

- The way the mechanical loads are applied to the wire and the wire's size: Both characteristics affect the strain distribution inside the wire, and thus the I_c response to the loads. In this category, we find the influence of the wire geometry (for example, round or rolled) on the test results. Additionally, the wire's size merits a special distinction, since it determines the ratio between the sample and the groove dimensions. In this matter, apart from defining the strain distribution inside the wire, the size affects the force redistribution between the sample and the impregnated medium.
- The wire technology (RRP and PIT) and its filament layout: The filament disposition and the production route affects as well the superconductor response. In a very similar way to that of the previous bullet, the distribution of mechanical strain in the superconductor depends on the geometrical arrangement of the Nb₃Sn sub-elements. Furthermore, different irreversible limits were found for similar wire sizes produced by the RRP and PIT method.
- The stiffness of the impregnated medium: The rigidity of the resin employed for impregnation, or the addition of insulation layers, has also an impact in the electro-mechanical behavior of the wires. A stiffer medium surrounding the wire, for example by including a fiberglass sleeve before impregnation, increases the load value required to obtain a certain reduction of I_c . This result can be straightforwardly understood as follows: for the same strain reaching the superconducting phase, a larger load is required when the stiffness of the surrounding

medium is higher.

Some of these results will be used as a complement of our analysis during the interpretation of the results.

5.6 Results

The outcome from the experimental campaign performed in the framework of this thesis, including the axial and transverse tests, is reviewed in the following.

5.6.1 I_c vs. axial strain

Figure 5-5 shows the measured critical current of the round (top) and 15 % rolled (bottom) wires as a function of the applied axial strain at 4.2 K and 19 T. In both plots, the origin of the x-axis (0 % strain) has been shifted to match the value of $I_{c0 \varnothing strain}$, thus showing the real applied strain to the sample. As mentioned in Section 5.5.2, a separate measurement was needed for the determination of $I_{c0 \varnothing strain}$, resulting in 108 A and 115 A for the round and rolled samples respectively.

In the graphs, the solid markers represent the value of the critical current at a certain strain level, whereas the empty ones are used to distinguish the so called "Backstep" points. These last points correspond to a new I_c measurement done after the unload of the sample from a higher applied strain, and serve to identify the onset of irreversible effects in the wires. Specifically, the samples have been unloaded by 0.03 %, at each point after the maximum of the curve (in the tensile strain regime).

The measurement of the two wires starts at a moderate compressive strain value ($\varepsilon_{applied} > -0.25\%$), chosen to ensure the absence of an irreversible decrease in I_c before the application of tensile loads. The difference on the first point location between the round and rolled specimens (the one at the highest compressive strain) is motivated by the chronological order of the tests. Since the measurement on the rolled sample was performed later in time, the compressive region of the curve was further explored (still well inside the reversible behavior) to provide a better input

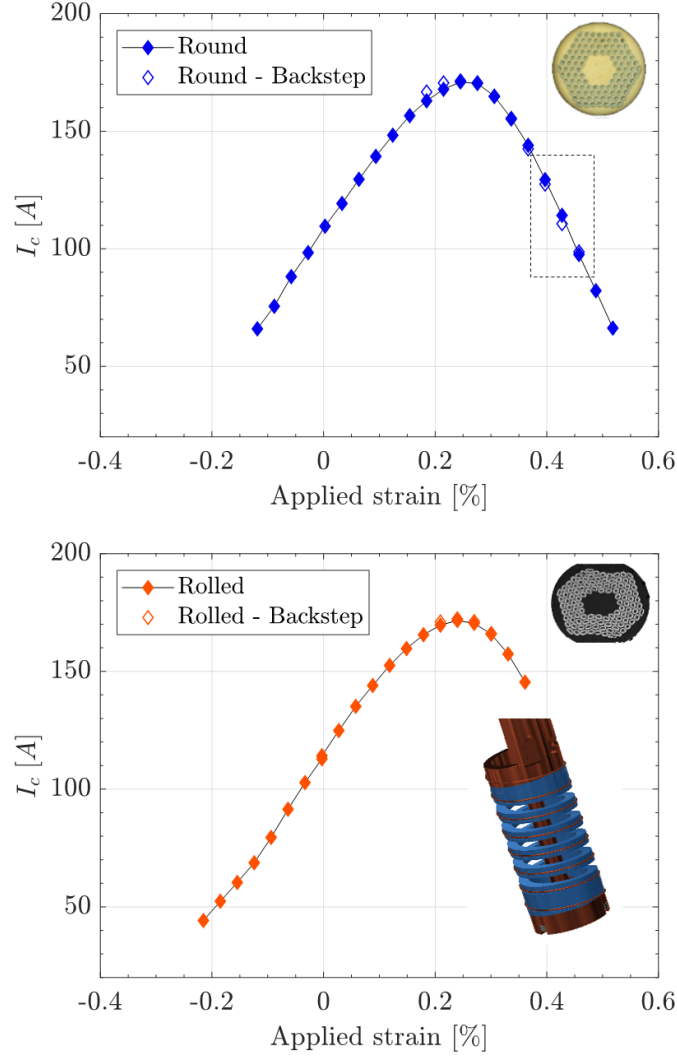


Figure 5-5: Critical current evolution as function of the applied axial strain for the reference wires measured in the WASP device. The top graph corresponds to the results of the round wire, while the bottom plot shows the case of the rolled sample. Only the measurements at 19 T are shown for simplicity.

to the interpretation of the MQXF FEM results. The finite element model predicts an axial strain level in the homogenized MQXF superconducting coils ranging from -0.2% during a quench, up to 0.1% at nominal operation. Therefore the investigation of this region of the curve became critical for us. We will come back to this point during the interpretation of the test results with respect to the magnet simulations.

In the tensile regime, the application of axial loads results into the increase of the

wire's I_c up to a maximum current value. The applied strain at which the peak is attained is denoted as ε_m , and has been found to be 0.24 % for the two wires. The well-known shape of this curve for Nb₃Sn superconductors suggests that at ε_m , the axial load compensates the initial pre-strain to which the Nb₃Sn phase is subjected due to the differential thermal contraction of the various materials in the composite wire. The fact that both round and rolled wires (identical samples, where just the geometry is changed) exhibit the same maximum current is hence a confirmation of the measurement outcome.

For the round wire, the backsteps show an irreversible reduction of 3 % in the critical current after the unload from 0.46 % applied strain ($\varepsilon_{irr} = 0.46$ %, highlighted by the dashed square in the picture). The value is largely above the strain values predicted by the FEM during operation. In view of this finding, the measurement of the rolled wire was stopped before irreversible effects were identified ($\varepsilon_{irr} > 0.35$ %). Instead, the test of this second sample was focused in the determination of the B_{c2} values close to the zero-strain condition.

The upper critical field at different strain levels was evaluated by using the well-known Kramer extrapolation [222] for I_c measurements done at various magnetic fields. With this objective, field sweeps from 15 T to 19 T were performed for both wires (with special emphasis on the rolled one, as already mentioned) at selected applied strain values. The obtained values are summarized in Table 5.3 and Table 5.4.

Table 5.3: Round wire: B_{c2} values extracted from the WASP experiment

Applied axial strain [%]	B_{c2} [T]
-0.15	22.95
0.03	24.7
0.12	25.7
0.22	26.2
0.31	26.1

Table 5.4: Rolled wire: B_{c2} values extracted from the WASP experiment

Applied axial strain [%]	B_{c2} [T]
-0.185	22.6
-0.09	23.55
0.0	24.56
0.03	25.02
0.12	25.76
0.24	26.35

5.6.2 I_c vs. transverse compressive stress

The results from the I_c measurements as a function of the applied transverse stress are shown in Figure 5-6. The top graph reports the critical current values for the round wire geometry, while the bottom plot depicts the corresponding points for the 15 % rolled samples.

In both cases, the I_c evolution is investigated at 4.2 K and 19 T and represented using two types of markers: full markers correspond to the value of critical current when the wire is loaded, and empty markers correspond to the value when the load has been removed. In a similar way to the axial test, the measurement's sequence accounts for the removal of the load each two stress steps, with the objective of analyzing in detail the onset of permanent damage in the wires.

The compressive stress value is computed using a purely geometrical relationship,

$$\sigma = \frac{Force \text{ (measured)}}{Width_{anvil} \times Length_{anvil}} \quad (5.15)$$

where the applied force is gradually increased up to a maximum of 35 kN, and the area of contact is calculated using $Width_{anvil}=1.15$ mm and $Length_{anvil}=126$ mm.

A total of three samples for each wire geometry have been measured. Even though all the specimens within each group (round and rolled) are identical and they were prepared following the same process, the figure shows that they present a different initial critical current (I_{c0} - defined as the critical current at 0 MPa) and a different electro-mechanical response to transverse stress.

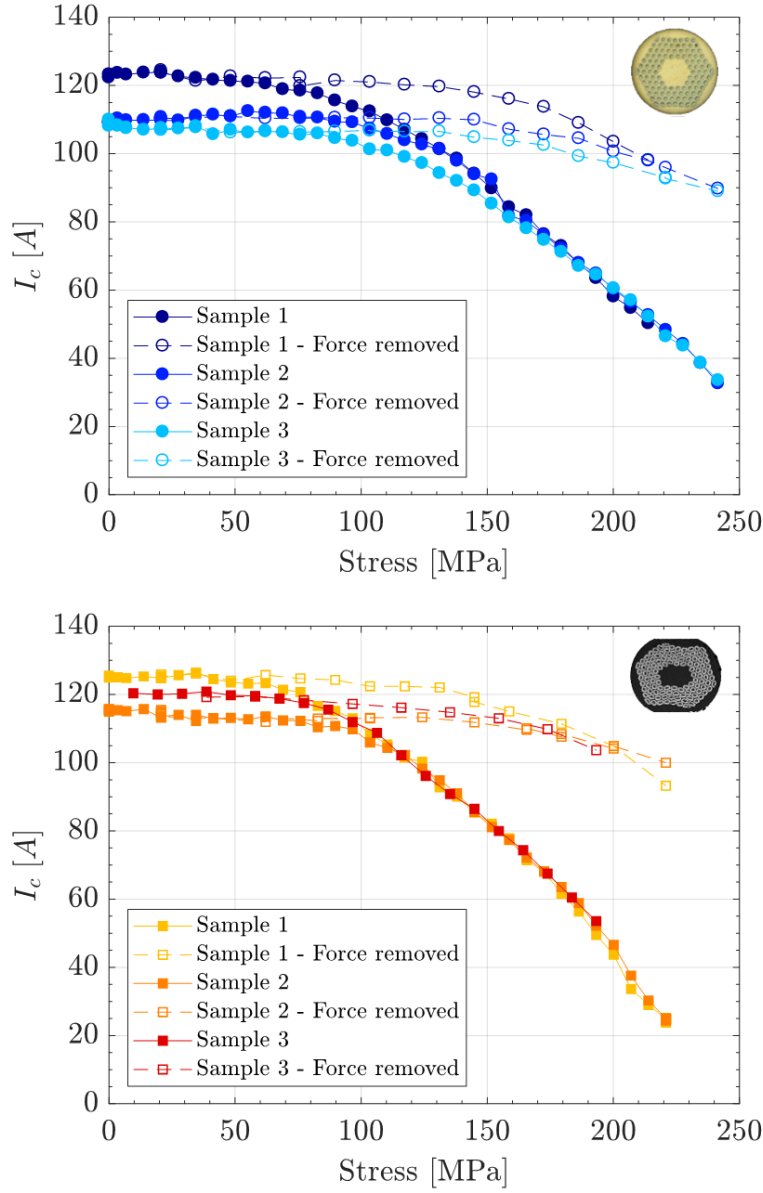


Figure 5-6: Critical current evolution as function of the applied transverse stress (computed using Equation 5.15) at 19 T. Full markers depict the I_c value when the load is being applied, empty markers show the critical current value upon force removal.

In the case of the round wires, the earliest measured sample (Sample 1) shows an $I_{c0} = 124$ A, the second sample (Sample 2) presents an $I_{c0} = 110$ A, and Sample 3 shows the lowest value among the three, with $I_{c0} = 107$ A. For the rolled geometry, I_{c0} equals 125 A, 115 A and 120 A for samples 1, 2 and 3 respectively.

With increasing stress levels, the wires converge to a common behavior under the presence of the transverse load. This phenomenon is clearly identified in the figure by the overlap of the three curves represented with solid markers. The implications of such an experimental evidence are twofold: first) the measurements indicate that there exists an initial difference between the samples, and second) they show that the original variation is overcome due to the presence of large applied transverse loads. Interestingly, the same effect is not perceived once the load is removed: upon force unload, the samples recover part of their initial differences.

The understanding of this behavior, not seen in previous experimental campaigns [59], constitutes a novelty of our work and sheds new light on the mechanisms behind critical current degradation. Thereby, before investigating the performance limits of the wires, the phenomenon is carefully reviewed in the next paragraphs.

Effects of the initial axial strain state on the response to transverse stress

To explain the different response of the six wires, let us limit ourselves firstly to the beginning of each of the tests, when the wires are not subjected to any transverse mechanical load ($\sigma = 0$ MPa).

As it has been already introduced, for a wide range of multifilamentary Nb₃Sn superconductors, the scaling law for the field and strain dependence of I_c follows the simplified expression provided in Equation 5.13. Assuming that $p \approx 0.5$ and $q \approx 2$ [77], one gets:

$$I_c(B, \varepsilon) = C^* \left(\frac{B}{B_{c2}(\varepsilon)} \right)^{-0.5} \left(1 - \frac{B}{B_{c2}(\varepsilon)} \right)^2 \quad (5.16)$$

In the absence of filament cracks (when there is no reduction of the available superconductor area), and assuming that the lack of applied transverse loads is a sufficient condition to ensure the invariability of C^* , the scaling law suggests that a change in I_c for identical wires, measured at the same magnetic field and temperature, would imperatively come from a change in their B_{c2} , due to a dissimilar mechanical strain state.

In the present case, since no transverse loads are applied, the measured discrepancy between the wires shall be explained by a different initial axial strain condition. The latter is likely to have its origin in an elastic component introduced during sample mounting.

The complete prove for this statement is found in the evaluation of the upper critical field of the different samples, using again the Kramer extrapolation. The results, shown in Table 5.5 for the round samples and in Table 5.6 for the rolled ones, support straightforwardly the development presented above. The superscript load and unload denote the presence or the absence of the transverse force during each measurement.

The samples with larger I_{c0} show an initial¹⁵ B_{c2} that is superior to the rest of wires. In the round case, the upper critical field of Sample 1 equals 25.7 T, while in Sample 2 and 3 a value of 24.89 T and 24.87 T is respectively obtained, in good agreement with their lower measured I_{c0} . The same behavior applies to the rolled wires, where B_{c2} for Sample 1 equals 25.43 T, whereas for Sample 2 the value is 24.78 T. Note that, the variability in the B_{c2} determination for different I_c measurements has been confirmed to be lower than 1%.

Table 5.5: Round wires: B_{c2} values extracted from the C-WASP experiment

	Transverse	B_{c2}^{load} [T]	B_{c2}^{unload} [T]
	stress [MPa]		
Sample 1	21		25.7
	145	24.7	25.3
	214	23.3	24.85
Sample 2	35		24.89
	220	22.84	24.5
Sample 3	35		24.87
	186	23.55	24.5
	242	22	24.3

¹⁵The difference in the lowest stress level among samples is purely due to a change in the experimental procedure. No permanent effects are expected for any of these load values.

Table 5.6: Rolled wires: B_{c2} values extracted from the C-WASP experiment

	Transverse stress [MPa]	B_{c2}^{load} [T]	B_{c2}^{unload} [T]
Sample 1	35		25.43
	145	24.1	25.02
	179	23.34	24.98
	220	21.6	24.39
Sample 2	21		24.78
	200	22.65	24.7
	220	21.58	24.66

In addition, it is possible to combine these results with the information obtained from the I_c vs. axial strain tests (Section 5.6.1). The review of both tests together leads us to the following conclusions:

- For the round wires, the I_{c0} of Samples 2 and 3 is very close to the value of $I_{c0 \varnothing strain}$ determined in the axial experiment. In the case of rolled wires, the I_{c0} of Sample 2 equals the corresponding $I_{c0 \varnothing strain}$. These samples have been tested in an initial condition where no axial strain was applied.
- On the opposite side, the I_{c0} for the other wires should lie along the curves of Figure 5-5, in the vicinity of the zero applied strain state.

Indeed, 0.05% applied axial strain in the round wires leads to an increase of I_c of the order of 16 A. This increment is perfectly in line with the measured difference between Sample 1 and the other two samples, and provides a quantitative explanation for the I_{c0} variability. In the case of rolled wires, 0.03% axial strain leads to an increment of 10 A, which is coincident with the difference between samples.

To give a visual representation of these results, the analysis of the curves at 19 T is summarized in Figure 5-7. The plots show a zoom of the WASP measurement, adding all the values of I_{c0} from the transverse tests as function of their interpolated axial strain level.

- The same exercise can be done for the B_{c2} data extracted from both experiments. A very similar upper critical field to that of the samples with an I_{c0} close to $I_{c0 \varnothing strain}$ is found at 0% applied axial strain in the WASP tests. The values can be compared by looking at Tables 5.3 - 5.6.

All together, these results thus confirm the presence of an initial axial tensile component in the strain state of some samples, which cause a different response to transverse loads. Consistently with our explanation, the merging of the curves for the rolled wires (showing a smaller difference in axial strain) happens before than in their round counterparts.

The origins for such a variation in the initial axial strain state of the wires (C-WASP experiment) are identified in the delicate sample mounting activities, as mentioned before. The wires may be pulled differently during the preparation process, introducing an elastic axial strain component that is kept by the soldering of the wire extremities. Figure 5-8 shows the result from two stress-strain measurements performed for the round wires, supporting the explanation. As it can be seen from the test outcome, an axial force as small as 20 N - 30 N is enough to reach a level of strain of 0.05% – 0.09%.

Finally, we close our discussion on the observed behavior by providing some hints about the merging of the curves. For all the wires, the elastic axial pre-strain component seems to be released at large applied transverse stress levels when the load is present. This phenomenon could be partially explained by a strain release mechanism resulting from the Poisson effect of the applied transverse load, together with the geometry and particularities of the test setup. Finite Element simulations performed in [223] for wires enclosed in a cavity and subjected to transverse forces, confirm the relevant effects seen in axial direction.

A permanent contribution to the release of the axial strain may come as well from the plastic deformation of the stabilizing matrix and from filament cracks. On this topic, several works are available in the community dealing with the plastic release of residual strain for Nb₃Sn wires [224], [225] Although in our case, the lack of convergence after force removal points out that permanent effects are not the main

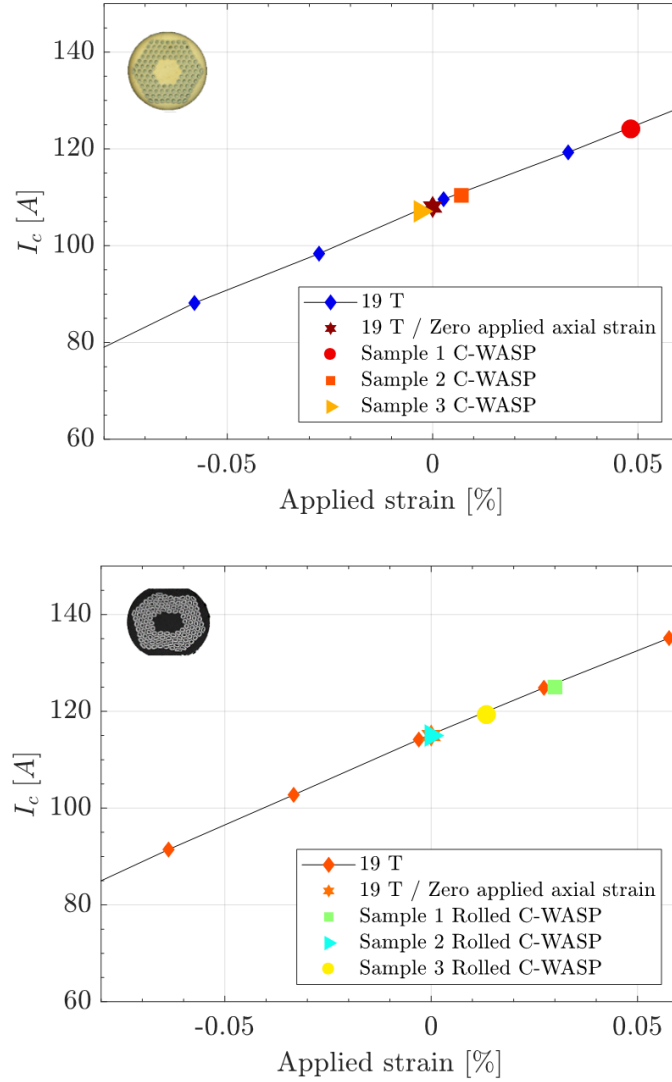


Figure 5-7: Detailed zoom of the WASP measurement. Markers display the interpolated values of strain for the C-WASP samples.

factor present.

The identification of the exact strain release mechanism acting in our test setup is left open for further research and will be tackled in future works.

The effect of transverse loads on conductor performance

Leaving aside the differences in the initial strain state, the results from the measurements are also employed to analyze the operational limits of both investigated wire geometries.

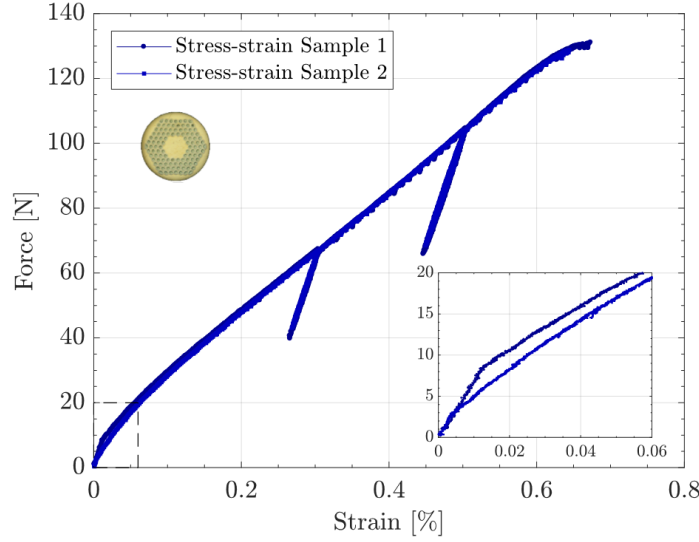


Figure 5-8: Mechanical stress-strain measurements performed for two straight samples of the investigated round wire. The bottom-right box shows a detailed zoom in the area indicated by the dashed rectangle. The force required to induce a strain in the range of 0.05% – 0.1% equals 20 N - 30 N.

At high level of stress, $\sigma_{applied} > 110$ MPa, the first signs of I_c decrease after force removal are identified for some of the wires, in particular, for the round Sample 1 and for the rolled Samples 1 and 3 (see Figure 5-9). A permanent reduction in critical current upon force unloading indicates the onset of critical current degradation in the specimen.

As it has been previously established, the determination of the experimental value of stress at which this occurs (σ_{irr}) is an exercise of the highest impact for magnet design. Such a value can be conventionally obtained by defining a critical threshold for the fraction of I_c recovered after force unloading (I_c^{unload}/I_{c0}). Following our standard convention, we propose a threshold value¹⁶ of 5% at 19 T.

By proceeding in this way, a similar irreversible stress range has been found for the two wire geometries. Taking a look to the round samples (see Figure 5-9): $145 \text{ MPa} \leq \sigma_{irr} \leq 175 \text{ MPa}$. For the rolled case, instead: $145 \text{ MPa} \leq \sigma_{irr} \leq 180 \text{ MPa}$.

¹⁶The variation in I_{c0} for the wires makes the analysis delicate, since a different σ_{irr} appears for identical samples that were supposed to provide an unique consistent number. Nonetheless, the practical range obtained also provides crucial information.

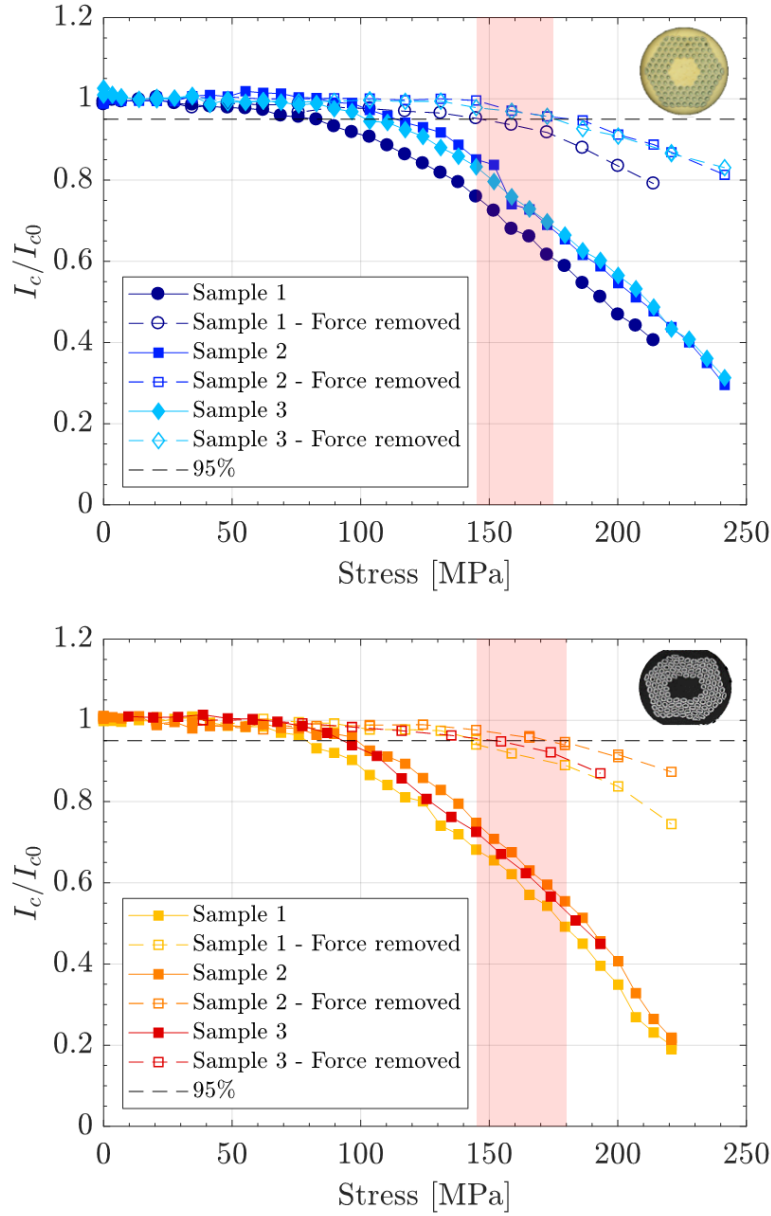


Figure 5-9: Critical current evolution as function of the applied transverse stress using normalized values. In the top plot, the results for the round wires are shown. The bottom plot, instead, depicts the results for the 15 % rolled samples. In both cases, the horizontal dashed line shows the 95%, while the red box contains the range at which this line intersects the unload curves of the three wires.

These results suggest that a 5 % permanent reduction in I_c happens at 145 MPa for round and rolled wires pre-tensioned axially. On the contrary, 175 MPa to 180 MPa are needed to cause the same reduction in non preloaded samples. In consequence,

Table 5.7: Summary on wires' results

Sample	I_{c0} (19 T)	$I_{c0 \varnothing strain}$ (19 T)	Initial B_{c2}	σ_{irr}
S1 - round	124 A	108 A	25.7 T	145 MPa
S2 - round	110.4 A	108 A	24.89 T	175 MPa
S3 - round	107.1 A	108 A	24.87 T	175 MPa

S1 - rolled	125 A	115 A	25.43 T	145 MPa
S2 - rolled	115 A	115 A	24.78 T	180 MPa
S3 - rolled	120 A	115 A	n.a.	155 MPa

the joint investigation of the loads applied in the main spatial directions becomes essential to determine the limits of the superconductor.

The last important conclusions from our work can be extracted by studying the wires' behavior under load. Here, we have found that a transverse stress level of 150 MPa¹⁷ induces an I_c reduction at 19 T of about 16% – 28% with respect to each wire's initial critical current (I_c^{load}/I_{c0}) for the round samples. In the rolled case, the range was found to be 29% – 34%. Though the reduction at lower fields would be smaller, the plot highlights the essential role of mechanics in the performance of these wires, and motivates its impact in magnet design.

A complete summary of the results is shown in Table 5.7.

5.6.3 An insight into the degradation mechanisms of RRP Nb₃Sn wires

To conclude our analysis, we investigate next the complete evolution of the upper critical field along the transverse loading history. Following the guidelines described in Section 5.3.2, the study of the permanent effects in B_{c2} (after load removal) adds very valuable information on the mechanisms causing the critical current degradation.

In this regard, the irreversible decrease of the critical current under transverse loads was attributed to the combination of two mechanisms: the plastic deformation of the Cu matrix, and the breakage of the Nb₃Sn filaments. The effects of these two

¹⁷In line with the stress values expected during operation of new high-field magnet designs.

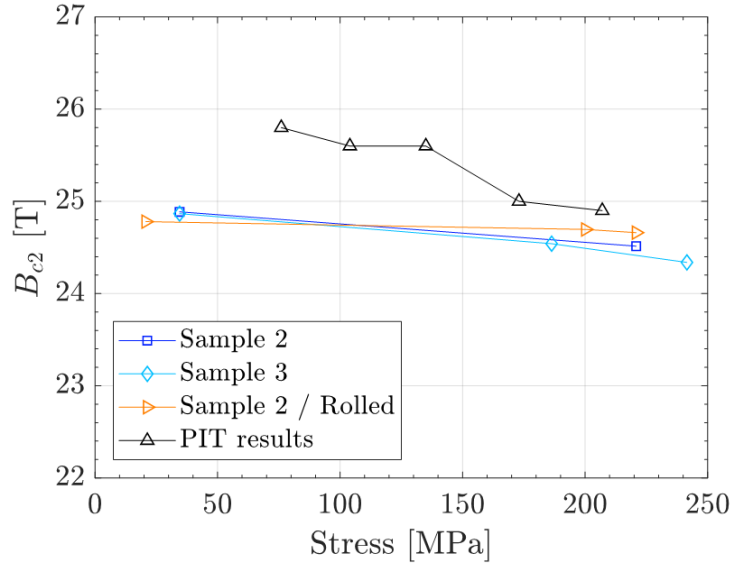


Figure 5-10: B_{c2} evolution after force removal for the investigated wires and a PIT sample measured in [59]. The fact that the decrease in B_{c2} for the RRP wires with zero initial applied strain is approximately 50% of the one reported for PIT, indicates a different degradation mechanism.

phenomena on the critical current were also claimed to be different. On one side, the effect of residual strain decreases the wire's B_{c2} due to a deformation of the Nb_3Sn crystal structure. On the other side, filament cracks reduce the available area of superconductor (leaving B_{c2} almost unaltered).

Figure 5-10 summarizes the results obtained for the B_{c2} evolution of the investigated samples, and adds for comparison the example of a PIT wire published in [59]. For the sake of a fair comparison, only the measurements of the wires with an initial zero applied axial strain state, which include the PIT sample, are considered. Under the same range of pressure, the decrease in B_{c2} is considerably smaller for the RRP samples than for their PIT counterpart, approximately 50%.

Although the reduction in B_{c2} upon force unloading for the RRP samples is only of 0.4 T, the permanent decrease in critical current has been found to be consistent with plastic strain effects¹⁸ [226]. For the measured wires, the dominant mechanism

¹⁸The use of Equation 5.13 in combination with the B_{c2} values extracted from the test, provide an accurate estimation of the I_c values after force unload. In other words, the small reduction in B_{c2} is enough to explain the permanent decrease in critical current.

driving the permanent reduction in critical current is the presence of a residual plastic strain affecting the superconductor. We plan to investigate further this result, which could have important implications for magnet design.

5.7 Application of the test results to the MQXF magnet

Gathering all the presented results, the most important conclusions of the test campaign, which are applicable to the understanding of the MQXF magnet performance, can be grouped as follows:

- Under axial loads, no irreversible reduction of the wires' critical current is observed for compressive strain levels in the range $-0.25\% < \varepsilon_{applied} < 0\%$. In the tensile regime, a 3% degradation in I_c was visible after the unload from 0.46% applied strain ($\varepsilon_{irr} = 0.46\%$). All results were obtained at 19 T.
- For transverse applied stresses, the permanent reduction of I_c is strongly influenced by the initial strain state of the sample. For round and rolled samples, a similar irreversibility range of $145\text{ MPa} \leq \sigma_{irr} \leq 175\text{ MPa}$ and $145\text{ MPa} \leq \sigma_{irr} \leq 180\text{ MPa}$ was respectively obtained. As for the previous case, all results were obtained at 19 T.

An extremely important aspect needs to be considered here. It has been just mentioned that the dominant mechanism driving the permanent reduction in critical current is thought to be the presence of residual plastic strain affecting the superconductor. Under this consideration, and following Equation 5.13, the conventional σ_{irr} value depends on the magnetic field at which the measurement is performed. The two ranges shown in this bullet were obtained at 19 T, where the I_c strain sensitivity is larger (compared, for instance, to the nominal operation regime of the MQXF conductor). At lower magnetic fields, such as the peak field level in MQXF (11.414 T), a shift towards larger σ_{irr} will happen.

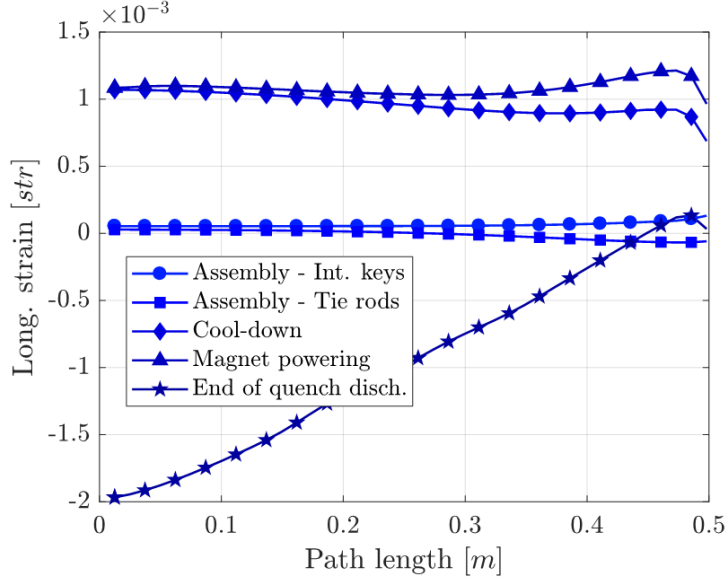


Figure 5-11: Longitudinal strain values along a path that travels through the straight section of the inner layer pole block.

Therefore, the proposed irreversibility thresholds cannot be directly applied to the magnet case, but they still offer crucial information for magnet designers.

On the other hand, considering the 3D FEM predictions for the magnet peak stresses during a quench:

- A peak stress of 135 MPa was identified in transverse (azimuthal) direction as a result of the reference quench discharge. This value was slightly higher in the 2D model (155 MPa), but attributed to a corner region of the block.
- In terms of axial strain, the model predicts a maximum compressive value during a quench of -0.2% and a peak tensile one at nominal current of 0.1% . These results are shown in Figure 5-11, which plots the axial strain along a path that travels through the straight section's pole block (in the inner layer, where the hot spot is located).

The same figure provides also important information on the combined axial and transverse strain state in the MQXF coils. As it can be seen in the graph, at nominal current the conductor is subjected to a certain axial tension. With the increase

of the coil temperature during the quench, and the reduction of the e.m. forces, the situation results inverted at the end of the transient. In this last case, the coil experiences compressive loads in all spatial directions due to the thermal expansion. Thus, although the process is complex, at the point of maximum transverse pressure also an axial compression is acting in the conductor. Such a strain state contrasts with the case of the C-WASP tests, where the lowest σ_{irr} corresponded to axially tensioned wires.

One additional consideration needs to be taken into account. As previously introduced, it has been proven for PIT conductors in [59] that the addition of a fiberglass sleeve in the C-WASP tests (reproducing the insulation of Nb₃Sn cables) increases the irreversible stress value¹⁹.

Henceforth, to the best of our knowledge, all the results suggest that the peak stress levels generated during the reference quench in the MQXF magnet should not translate into a visible permanent reduction of its performance. The last statement, has been proven as well by the High MIITs tests done for the short model magnet MQXFS4. The simulation of this last exercise showed a peak of 165 MPa in the corner node of the block (in the geometrical location of the pole insulation) and an uniform maximum stress of 135 MPa. The latter value is again below the expected degradation limits and confirms the experiment's outcome.

5.8 Conclusions

To close the thesis, we have presented the details of our test campaign aimed at characterizing the electro-mechanical response of state-of-the-art RRP Nb₃Sn wires to mechanical loads. With the objective of obtaining relevant information on magnet performance from a single wire experiment, we investigated the effect of loads applied independently in transverse and axial directions.

The obtained results were summarized in the previous sections and provided crucial information for the understanding of the operational limits in the MQXF

¹⁹In the available PIT case, an increment of approximately 50 MPa was measured.

quadrupole. Moreover, the main findings are also expected to have a large impact in the design of future magnets.

In this regard, a remarkable effect of the initial axial strain state in the wire's response to transverse loads has been identified. For the correct characterization of Nb₃Sn superconductors, the joint investigation of the loads acting in the main spatial directions becomes consequently a strong requirement.

Lastly, by studying the wires' response to mechanical loads at different magnetic fields, we found a very limited permanent decrease in the upper critical field (B_{c2}) resulting from transverse stresses up to 250 MPa. Still, the dominating mechanism behind critical current degradation, for the investigated wires, is thought to be the presence of residual plastic strain affecting the superconductor.

Chapter 6

Conclusions and outlook

6.1 Conclusions

This PhD Thesis has focused on the analysis of the mechanical behavior of a Nb₃Sn accelerator magnet during a quench. The new high field magnets based on the niobium-tin technology represent a novel paradigm in view of particle colliders with increased luminosity and energy. The content of the work can be divided in two main areas of research, both belonging to the field of Applied Superconductivity: Large Scale and Materials.

The first part of the thesis deals with Large Scale applications. We have presented there the development of a methodology aimed at studying the three-dimensional mechanical behavior of a superconducting accelerator magnet during a quench. Such an analysis becomes essential for the next generation of Nb₃Sn magnets, in order to guarantee the correct performance of the system and the absence of conductor degradation. As a proof-of-principle, we specifically investigated the case of the MQXF quadrupole magnet protected with quench heaters.

The work related to Materials is focused on the electro-mechanical characterization of Nb₃Sn superconductors. In this subject, a detailed experimental campaign has been carried out to investigate the critical current of impregnated Nb₃Sn strands under transverse and axial applied forces. One of the main objectives of the study was to identify the limits associated to the correct conductor performance. The axial

and transverse configurations were chosen to simulate the working conditions of the superconducting wires present in accelerator magnets. Ultimately, the link between both parts of the thesis allows to define safe limits for the magnet operation and protection, and it is thought to have a profound impact in the design of future high field units.

In the first chapter of the thesis, an introduction to the fundamentals of superconductivity and practical superconductors has been given, with special emphasis in the review of the Nb₃Sn conductor technology. The definition of a quench, together with the most relevant protection strategies, was also provided in order to equip the reader with all the required tools for the correct understanding of our research. The main challenges related to the use of Nb₃Sn and to the quench protection of new high field magnets are linked to the remarkable strain sensitivity of the superconductor, and to the large magnetic stored energies and current densities of the magnet designs. A safe magnet response in case of a quench, namely in terms of coil temperature, peak voltages and induced quench stresses, must be guaranteed in order to ensure the lack of conductor damage. The two first characteristics (the control of the temperature increase happening in the superconducting coils and the peak electric voltages generated during the discharge) have historically been considered as the core of the quench protection. For these, the community has established solid limits that preserve the magnet integrity during a quench. Unfortunately, the same type of limits have never been proposed for the potentially dangerous mechanical loads that may appear during a quench. The necessary link between the most relevant quench physics and the magnet mechanics remains yet to be established and it is tackled in our work.

The second chapter of the thesis introduces the characteristics of the Nb₃Sn MQXF quadrupole. The latter has been chosen as the reference magnet for our studies due to its relevant characteristics. As previously anticipated, its large stored energy and demanding protection turns the case of MQXF into one of the most interesting among the new Nb₃Sn accelerator magnet designs. In addition, the MQXF *low - β* quadrupole will be the first Nb₃Sn accelerator magnet ever installed in a particle accelerator, thus becoming an extremely important example of application for the

technology. The MQXF magnets have been designed as a part of the High Luminosity upgrade of the LHC at CERN, which seeks to increase the integrated machine luminosity by a factor of ten with respect to the current LHC nominal parameters. In terms of magnetic design, the MQXF magnet will produce a gradient of 132.6 T m^{-1} in an enhanced 150 mm single aperture, and will operate at a conductor peak field of 11.4 T in nominal conditions. On the other side, the magnet mechanical design has its foundation in the bladder and key technology. The four superconducting coils are assembled inside a shell-based support structure, which is conceived for managing the large electromagnetic forces whilst minimizing any potential displacement in the magnet components. One of the key characteristics of this concept relies on the application of an initial mechanical pre-load to the superconducting coils. The pre-load, also referred extensively as pre-stress, seeks to counteract the effect of the Lorentz forces that will later develop during magnet operation. It can be controlled, both at ambient and cryogenic temperature, by adapting the size of the interference keys used during magnet assembly.

In Chapter 3 the development of a three-dimensional multi-physics quench simulation code has been presented. Our proposed modelling strategy relies on the combination of a 3D thermal-electric finite element model with a decoupled magnetic computation and a simplified transitory losses model. The FE code has been developed in ANSYS APDL, ensuring a direct compatibility with the usual mechanical models used in the accelerator magnet community. To prove the validity of our approach, the simulation results have been validated using two types of experimental data: i) signals obtained from controlled laboratory-scale quench tests, which were done on single superconducting cables and ii) data from real full-scale magnet experiments, performed as a part of the short model campaign for the MQXF project. In what regards the validation using the single cable tests, the computation has been shown to reproduce within 13% the measured quench propagation velocities and turn to turn delays. In addition, it provides a very informative 3D description of the different magnitudes of interest along the cables (temperature, resistive voltages, etc.). On the other side, looking at the comparison with different magnet experi-

ments (using a pure quench heater protection scheme), the code is able to predict the so-called quench integral values for discharges at various current levels within 8%. Furthermore, considering the induced quench delays by the quench heater system, a maximum discrepancy of 1 ms at nominal current has been found. As for the previous case, a detailed three-dimensional description of the the quench discharge and magnet temperature is obtained from the model.

In Chapter 4, the 2D and 3D MQXF finite element mechanical models were thoroughly explained. With the objective of allowing the analysis of the magnet mechanical behavior during a quench, we presented the required coupling solution between the 3D thermal-electric simulation and the magnet mechanical one. A so-called one-way coupling mechanism has been employed, where the temperature distribution and the magnet current values (used for the scaling of the magnetic forces) are transferred to the structural simulation at selected time steps. In this way, the study of the quench induced stresses is performed following a quasi-static approach. The outcome of the analysis can be condensed into the next main results:

1. The simulation confirmed that new peak stresses appear in the superconducting coils of the magnet during a quench. Under the modelling assumptions, they are the result of the coil thermal expansion inside the magnet structure (which remains fundamentally cold) and the fast transient in electro-magnetic forces.
2. The largest peak stresses are found at the end of the quench discharge, when the magnet stored energy has been completely released. There exists a correlation between the 3D coil stress and the temperature distribution resulting from the quench propagation.
3. One can define the "quench delta" at a given position as the difference between the stress values after the magnet cool-down and at the end of the quench. For the most relevant transverse coil stresses (azimuthal direction), this stress delta has been found to be mainly dependent on the average coil block temperature. The latter is determined by the magnet stored energy, protection scheme and the additional energy input coming from the initial quench propagation.

4. For a quench happening in the MQXF magnet center (at nominal current, protected with only quench heaters and with a 15 ms propagation time), an increase in azimuthal stress of 20 MPa - 40 MPa in the hot-spot area is predicted by the model. Following the quench propagation, the largest stress gradient (in z-direction) is however seen for longitudinal stresses. A gradual variation of the order of 60 MPa is identified for the inner layer conductor pole block. In the same spatial direction, a compressive stress difference (again between the values at cryogenic temperature and after the quench, $\Delta\sigma_z$) as large as 100 MPa is predicted by the model in specific points of the hot-spot region under the assumption of isotropic properties for the coil material.
5. As for the case of the thermal-electric model, the results from the mechanical simulation could be validated using the strain signals coming from the magnet instrumentation.

Finally, in Chapter 5, an experimental campaign aimed at characterizing the electro-mechanical response of state-of-the-art RRP Nb₃Sn wires to mechanical loads has been discussed. With the objective of obtaining relevant information on magnet performance from a single wire experiment, we investigated the effect of loads applied independently in transverse and axial directions. The superconducting wire used for the production of the MQXF magnets was chosen for the campaign. The main outcome from the work is enumerated next:

1. We identified a remarkable effect of the initial axial strain state in the wire's response to transverse loads. The axial component determines the initial critical current and its evolution at low transverse stress levels.
2. Critical current tests under axial loads served to provide the needed complimentary information for the study. The results shown in our thesis suggest that it is always essential to consider the complete strain tensor in the analysis of the electro-mechanical limits of Nb₃Sn superconductors.
3. The transverse stress value leading to a conventional permanent reduction in I_c

of 5%, has been found to be in the range of 145 MPa - 180 MPa for round and rolled wires at 19 T. The different initial axial strain state is behind the range obtained for the various samples of the same wire.

4. Under axial loads, no irreversible reduction of the wires' critical current is observed for compressive strain levels in the range $-0.25\% < \varepsilon_{applied} < 0\%$. In the tensile regime, a 3% degradation in I_c was visible after the unload from 0.46% applied strain ($\varepsilon_{irr} = 0.46\%$). Results were as well obtained at 19 T.
5. Lastly, by studying the wires' response at different magnetic fields, we found a very limited permanent decrease in the upper critical field (B_{c2}) resulting from transverse stresses up to 250 MPa. Despite the small variation, the dominating mechanism behind critical current degradation for the investigated wires is thought to be the presence of residual plastic strain affecting the superconductor.
6. In such a case, and due to the dependence of the strain sensitivity on the magnetic field at which the measurements are done, no direct comparison between the obtained wire results and the MQXF magnet case is possible. However, we can conclude, to the best of our knowledge, that the conservative magnet quench at nominal current should not translate into a visible permanent reduction of the superconductor performance.

6.2 Outlook

The analysis of the magnet mechanical behavior during a quench performed for the Nb₃Sn MQXF quadrupole has revealed a set of important conclusions. These conclusions need to be taken into account in the design of future accelerator magnets, in order to ensure their safe performance. Nevertheless, some interesting points would need to be further addressed; they are succinctly mentioned in next paragraphs.

First of all, to complete our understanding on the topic, it would be of paramount importance to perform the same study for other magnet designs. The new analyses

should tackle the impact of different coil geometries and magnet stored energies. Ultimately, our methodology could provide the means to define a range for the induced quench stresses in the most recurrent magnet configurations. The stress increase should be categorized as a function of the magnetic stored energy and protection characteristics.

Second, the three-dimensional nature of the analysis could be exploited to simulate the case of a quench happening in the coil ends. Note that in our work, we covered the case of a quench in the peak field conductor block (seeing as well the largest transverse stress), which has been found to be the most recurrent quench location in the MQXF series magnets. Nonetheless, a quench in the coil ends can be easily simulated. For such an exercise, a better assessment of the coil material properties would be beneficial. We will cover this point next.

In addition, the modelling capabilities presented in this thesis should be extended to account for the simulation of new protection strategies, such as CLIQ¹. Although the magnet protection with only quench heaters is seen as a conservative case, it would be very enriching to obtain the same detailed results accounting for different protection methods. Nowadays, the 2D CLIQ analysis would be already possible by coupling the ANSYS mechanical models with dedicated codes built for the CLIQ simulations. However, the three-dimensional analysis of a CLIQ discharge remains yet to be explored.

The detailed review of the material properties, both for the multi-physics quench and mechanical simulations, is as well an area of research that deserves further attention. Specially, the establishment of the mechanical properties used for the Nb₃Sn superconducting coil blocks becomes a crucial aspect, which has largely raised the attention of the superconducting magnet community. Linked to our thesis, the definition of more accurate material models or the microscopic modelling of the conductor constitute two of the most relevant ideas for future work.

Finally, concerning the work on the electro-mechanical characterization of Nb₃Sn

¹Moreover, it would be very interesting to include the effects of the mechanical loads on the superconductor's critical current through a fully coupled simulation scheme.

conductors, a new step forward in the understanding of the test results would be acquired by performing both two-dimensional and three-dimensional multi-physics simulations of the experiment.

Appendix A

A quick note on the fundamentals of accelerator physics used in this thesis

Some of the MQXF *low* $-\beta$ magnet characteristics, including its name, can be easily understood looking at the basic physics of high energy synchrotron accelerators; in particular, by reviewing the concepts related to the transverse¹ motion of a particle beam.

In few words, the transverse beam stability consists in a bounded oscillatory motion about the design trajectory [88]. This motion is termed as betatron oscillation for historical reasons. It is described, in one plane, by the following equation:

$$\frac{d^2x}{ds^2} + K(s)x = 0 \quad (\text{A.1})$$

As it can be seen, Equation A.1 resembles to that of an harmonic oscillator where x is the coordinate of the corresponding transverse plane, s is the path coordinate (following the beam trajectory) and $K(s)$ is the experienced restoring force. The solution to Equation A.1 is the one of an oscillator whose amplitude and frequency are modulated. In appropriate terms:

$$x(s) = \sqrt{\frac{\epsilon_N \beta_x(s)}{\beta_r \gamma}} \cos(\psi(s)) \quad (\text{A.2})$$

¹The term transverse refers to the planes that are orthogonal to the circulating beam direction.

where ϵ_N is the so-called normalized emittance, which is an invariant along the ring (a property of the injectors), β_x is the so-called beta function in the x-direction, which defines the envelope of the beam along the ring, γ is the Lorentz factor (defined as $1/\sqrt{1-(v/c)^2}$), β_r is the relativistic beta (defined as v/c), v is the beam velocity, c is the speed of light and ψ is the phase advance.

The beam size in x is therefore:

$$\sigma_x = |x(s)| = \sqrt{\frac{\epsilon_N \beta_x(s)}{\beta_r \gamma}} \quad (\text{A.3})$$

It follows from Equation A.3 that a decrease in the beam size can be obtained by the machine optics via a reduction of the beta function. Nevertheless, an extremely important remark needs to be considered when reviewing the behavior of β in the free space or drift regions, as those surrounding an interaction point (IP). There, β in any of the transverse directions for symmetrical optics can be expressed as [88]:

$$\beta(s) = \beta^* + \frac{s^2}{\beta^*} \approx \frac{s^2}{\beta^*} \quad (\text{A.4})$$

where s is the path coordinate and β^* is the beta function value at the interaction point ($s=0$).

Special lattice structures, named as *low - β* insertions, are designed around the IPs where the particle detectors are installed. As an example, to realize a beam crossing for which $\beta_x = \beta_y$, these structures are constructed with two symmetrically placed quadrupole triplets. Straightforwardly, the quadratic dependence on the path distance results in a large beta function at the position of these magnets². Furthermore, the inverse dependence on β^* translates into the requirement of larger aperture *low - β* quadrupoles to decrease the beam size at the interaction down to the desired values and to increase the luminosity of the machine.

To conclude this short introduction, the definition of the machine luminosity is also provided:

²The drift that takes place between the center of the experiment and the first magnets corresponds to the space required for the detectors and auxiliary machine components.

$$L = \frac{N_b^2 n_b f_{rev} \gamma}{4\pi \epsilon_N \beta^*} F \quad (\text{A.5})$$

where N_b is the number of particles per bunch, n_b the number of bunches per beam, f_{rev} is the revolution frequency, γ the relativistic gamma factor, ϵ_N the normalized transverse beam emittance, and β^* the beta function at the collision point. The factor F accounts for the reduction of luminosity due to the crossing angle at the interaction point.

Appendix B

Application of the Finite Element Method

In this appendix, we demonstrate the application of the Finite Element Method to the numerical approximation of a 1D boundary value problem. Due to its simplicity and the existence of analytical solution, the presented case offers a great opportunity to understand the fundamentals of the method.

Following the description presented in [178], the main steps involved in the FEM can be briefly explained as follows:

1. Formulation of the physical problem on its "variational" or "weak integral form".
2. Discretization of the domain into a finite number of smaller sub-domains. The solution can be then approximated with so-called unknown degrees of freedom and shape functions.
3. Expression of the equilibrium condition of all sub-domains in matrix forms.
4. Solution of the unknown values.

To illustrate each of these steps, our development will closely reproduce the problem presented in [82]. It reads:

$$\frac{d^2u(x)}{dx^2} = f(x), \quad x \in \Omega \quad (\text{B.1})$$

$$\Omega = [0, 1]$$

where boundary conditions can be imposed using the so-called Dirichlet¹ type,

$$u(x)|_{x=0} = u_0, \quad u(x)|_{x=1} = u_1 \quad (\text{B.2})$$

or the Neumann one,

$$\frac{du}{dx}|_{x=0} = q_0, \quad \frac{du}{dx}|_{x=1} = q_1 \quad (\text{B.3})$$

The example can be identified as the 1D representation of a heat conduction problem featuring unit thermal conductivity. Consequently, the potential $u(x)$ would represent the temperature field and $f(x)$ would correspond to the heat source term. Let us consider for our analysis f to be constant, i.e. $f(x) = c$, and the following Dirichlet boundary conditions,

$$u(x)|_{x=0} = 0, \quad u(x)|_{x=1} = 0 \quad (\text{B.4})$$

Under these assumptions, the analytical solution takes the form:

$$u^*(x) = \frac{c}{2}(x^2 - x) \quad (\text{B.5})$$

On the other hand, to obtain the numerical approximation to Equation B.5 by means of the Finite Element Method, we need first to re-formulate the problem on its weak integral form. In this equivalent formulation, the equation is forced to hold in a weighted, projective sense over the domain Ω . Appropriately chosen weighting/test functions, $w(x)$, are hence introduced:

¹A restriction on the field variable is called Dirichlet or essential boundary condition. Instead, a restriction on the first derivative is called Neumann condition or natural boundary condition.

$$\int_{\Omega} w(x) \frac{d^2 u(x)}{dx^2} dx - \int_{\Omega} w(x) f(x) dx = 0 \quad (\text{B.6})$$

Integrating by parts to reduce the order of the differential term, and after some manipulation, we obtain the weak form of the boundary value problem:

$$-\int_{\Omega} \frac{dw(x)}{dx} \frac{du(x)}{dx} dx + \left[w(x) \frac{du(x)}{dx} \right]_0^1 - \int_{\Omega} w(x) f(x) dx = 0 \quad (\text{B.7})$$

As it can be seen, reduced differentiability requirements² are achieved for $u(x)$. Equation B.7 allows for the approximation of the solution by linear basis functions, and the accounting for Neumann type boundary conditions (BC). Assuming zero heat flux on both ends, i.e. homogeneous Neumann BC, the equation reduces to:

$$-\int_{\Omega} \frac{dw(x)}{dx} \frac{du(x)}{dx} dx - \int_{\Omega} w(x) f(x) dx = 0 \quad (\text{B.8})$$

The domain (Ω) can be then divided into a desired number (J) of sub-domains³, denoted as Ω_j . Each of these sub-domains is defined by two boundary nodes,

$$\Omega = \bigcup_{j=1}^J \Omega_j \quad (\text{B.9})$$

One can assume that the potential $u(x)$ varies linearly between the nodes of Ω_j ,

$$u(x) = \alpha_{j2} x + \alpha_{j1}, \quad x \in \Omega_j, \quad (\text{B.10})$$

Thus, if $[x_{n-1}, x_n]$ are the coordinates of the two boundary nodes; $u(x)$ for a point within Ω_j can be expressed as function of the element nodal values (u_{n-1}, u_n),

$$u(x) = \alpha_{j2} x + \alpha_{j1} = \frac{x_n - x}{x_n - x_{n-1}} u_{n-1} + \frac{-x_{n-1} + x}{x_n - x_{n-1}} u_n \quad (\text{B.11})$$

The quotients in Equation B.11 are referred as the local element shape functions

²Hence, the term "weak form".

³Strictly speaking, the sub-domains Ω_j are known as the support of the finite elements. They cannot be directly called the finite elements, since the latter are characterized not only by the spatial discretization but also by the number of degrees of freedom, the definition of its nodal variables, the polynomial basis of the approximation, and the interelement continuity.

(N_{jk} , where $k = 1, 2$ denotes the local node number in Ω_j). Instead, the nodal values of the potential receive the name of degrees of freedom (DOF),

$$u(x) = N_{j1}(x)u_{n-1} + N_{j2}(x)u_n \quad (\text{B.12})$$

At this point, the weak form can be rewritten for each sub-domain as:

$$-\int_{\Omega_j} \frac{dw_l(x)}{dx} \sum_{k=1,2} \frac{dN_{jk}(x)}{dx} u_{jk} dx - \int_{\Omega_j} w_l(x) f(x) dx = 0, \quad l = 1, 2 \quad (\text{B.13})$$

where $u_{j1} = u_{n-1}$ and $u_{j2} = u_n$ are the node potentials in the local numbering scheme of Ω_j .

The appropriate weighting functions (w_l) need now to be defined. The choice on these functions, ultimately defines the numerical method. The well-known Galerkin's method uses the element shape functions as weighting functions:

$$w_l(x) = N_{jl}(x) \quad (\text{B.14})$$

Substituting and re-arranging, it yields,

$$\int_{\Omega_j} \frac{dN_{jl}(x)}{dx} \sum_{k=1,2} \frac{dN_{jk}(x)}{dx} u_{jk} dx = - \int_{\Omega_j} N_{jl}(x) f(x) dx = \quad l = 1, 2 \quad (\text{B.15})$$

Or equivalently⁴,

$$\begin{aligned} & \int_{x_{n-1}}^{x_n} \left(\frac{dN_{j1}}{dx} \frac{dN_{j1}}{dx} u_{n-1} \right) + \left(\frac{dN_{j1}}{dx} \frac{dN_{j2}}{dx} u_n \right) dx = - \int_{x_{n-1}}^{x_n} N_{j1}(x) f(x) dx \\ & \int_{x_{n-1}}^{x_n} \left(\frac{dN_{j2}}{dx} \frac{dN_{j1}}{dx} u_{n-1} \right) + \left(\frac{dN_{j2}}{dx} \frac{dN_{j2}}{dx} u_n \right) dx = - \int_{x_{n-1}}^{x_n} N_{j2}(x) f(x) dx \end{aligned} \quad (\text{B.16})$$

⁴We omit, from now on, the argument of N_{jk} , i.e. $N_{jk}(x) = N_{jk}$

This linear equation system (B.16) can be finally expressed in a convenient matrix form as:

$$[k_j]\{u_j\} = \{f_j\} \quad (\text{B.17})$$

where $[k_j]$ is called the symmetric element stiffness matrix, $\{u_j\}$ is the vector of the node potentials and $\{f_j\}$ is the element force vector. Note that inhomogenous Neumann BC could be introduced by means of an additional boundary vector $\{t_j\}$,

$$[k_j]\{u_j\} + q_0\{t_j\} = \{f_j\} \quad (\text{B.18})$$

To obtain the solution of the problem, the assembly of the complete equilibrium condition for all sub-domains needs to be performed. This is done by matrix manipulation, where the contribution of each element is sorted into an overall stiffness matrix according to the global numbering scheme of the DOFs. Dirichlet BC are accommodated by writing the corresponding values into the $\{u_j\}$ vector of the elements. Indeed, their rows and columns are eliminated to avoid an over-determined equation system. Finally, the solution can be obtained.

Example:

The domain Ω is discretized using 5 elements with equal length ($l = 0.2$). A constant heat source, $f(x) = 3$, is considered.

$$\begin{aligned} [k_j] &= \int_{x_{n-1}}^{x_n} \begin{pmatrix} \frac{dN_{j1}}{dx} \frac{dN_{j1}}{dx} & \frac{dN_{j1}}{dx} \frac{dN_{j2}}{dx} \\ \frac{dN_{j2}}{dx} \frac{dN_{j1}}{dx} & \frac{dN_{j2}}{dx} \frac{dN_{j2}}{dx} \end{pmatrix} dx \\ &= \int_{x_{n-1}}^{x_n} \begin{pmatrix} \frac{1}{(x_n - x_{n-1})^2} & \frac{-1}{(x_n - x_{n-1})^2} \\ \frac{-1}{(x_n - x_{n-1})^2} & \frac{1}{(x_n - x_{n-1})^2} \end{pmatrix} dx = \begin{pmatrix} \frac{1}{l} & \frac{-1}{l} \\ \frac{-1}{l} & \frac{1}{l} \end{pmatrix} \end{aligned}$$

$$\begin{aligned}
\{f_j\} &= - \int_{x_{n-1}}^{x_n} \begin{pmatrix} N_{j1} \\ N_{j2} \end{pmatrix} f(x) dx \\
&= -c \int_{x_{n-1}}^{x_n} \begin{pmatrix} \frac{x_n-x}{x_n-x_{n-1}} \\ \frac{-x_{n-1}+x}{x_n-x_{n-1}} \end{pmatrix} dx = -\frac{c}{2l} \begin{pmatrix} 2x_n x - x^2 \\ -2x_{n-1}x + x^2 \end{pmatrix} \Big|_{x_{n-1}}^{x_n} \\
&= -\frac{c}{2l} \begin{pmatrix} (x_n - x_{n-1})^2 \\ (x_{n-1} - x_n)^2 \end{pmatrix} = -\frac{1}{2} \begin{pmatrix} cl \\ cl \end{pmatrix}
\end{aligned}$$

Assembling the system for all elements, it yields:

$$\begin{pmatrix} \frac{1}{l} & \frac{-1}{l} & 0 & 0 & 0 & 0 \\ \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} & 0 & 0 & 0 \\ 0 & \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} & 0 & 0 \\ 0 & 0 & \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} & 0 \\ 0 & 0 & 0 & \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} \\ 0 & 0 & 0 & 0 & \frac{-1}{l} & \frac{1}{l} \end{pmatrix} \begin{pmatrix} u_1 \\ u_2 \\ u_3 \\ u_4 \\ u_5 \\ u_6 \end{pmatrix} = -\frac{1}{2} \begin{pmatrix} cl \\ 2cl \\ 2cl \\ 2cl \\ 2cl \\ cl \end{pmatrix}$$

Introducing the Dirichlet boundary conditions and eliminating the corresponding rows and columns,

$$\begin{pmatrix} \frac{2}{l} & \frac{-1}{l} & 0 & 0 \\ \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} & 0 \\ 0 & \frac{-1}{l} & \frac{2}{l} & \frac{-1}{l} \\ 0 & 0 & \frac{-1}{l} & \frac{2}{l} \end{pmatrix} \begin{pmatrix} u_2 \\ u_3 \\ u_4 \\ u_5 \end{pmatrix} = - \begin{pmatrix} cl \\ cl \\ cl \\ cl \end{pmatrix}$$

Finally,

$$\begin{pmatrix} u_2 \\ u_3 \\ u_4 \\ u_5 \end{pmatrix} = -\frac{l}{5} \begin{pmatrix} 4 & 3 & 2 & 1 \\ 3 & 6 & 4 & 2 \\ 2 & 4 & 6 & 3 \\ 1 & 2 & 3 & 4 \end{pmatrix} \begin{pmatrix} cl \\ cl \\ cl \\ cl \end{pmatrix} = - \begin{pmatrix} 2cl^2 \\ 3cl^2 \\ 3cl^2 \\ 2cl^2 \end{pmatrix}$$

The solution (see Figure 3-2) becomes:

$$\begin{pmatrix} u_2 \\ u_3 \\ u_4 \\ u_5 \end{pmatrix} = \begin{pmatrix} -0.24 \\ -0.36 \\ -0.24 \\ -0.36 \end{pmatrix}$$

Appendix C

Coupling methods available in ANSYS APDL

ANSYS APDL software offers the following coupling capabilities between physics:

1. **Direct coupled-field analysis:** The direct method involves a single analysis using coupled-field elements. The latter contain all necessary degrees of freedom for the problem solution. In this case, the coupling mechanism is handled by calculating element matrices and/or element load vectors that contain all the required terms.
2. **Load-transfer methods:** The load-transfer method relies instead on performing separate simulations (two or more analysis), each of them belonging to a different field. The results from one of the physics are applied as loads in the corresponding coupled model.

List of Figures

1-1	Historic plot of the resistance $[\Omega]$ (vertical axis) versus the temperature $[\text{K}]$ (horizontal axis) for a Mercury sample. The superconducting transition is clearly visible at 4.2 K. Figure extracted from [1] and dated on the 26 th of October 1911.	2
1-2	Illustration of the critical surface for Nb-Ti (left) and Nb ₃ Sn (right) [10]. The larger critical values of Nb ₃ Sn materials allow the preservation of the superconducting state in a larger parameter space compared to Nb-Ti.	6
1-3	Nb ₃ Sn superconducting wire and Rutherford cable examples. a) Cross sectional view of the unreacted Nb ₃ Sn RRP superconducting wire used for the new MQXF quadrupole, produced following the 108/127 sub-element layout. b) Rendering of a Rutherford cable composed of different wires transposed together. Courtesy of F. Wolf [30].	10
1-4	Two-dimensional magnetic field vectors and conductor cross section for three multipole accelerator magnet examples ($\cos \theta$ - type): a) Dipole, b) Quadrupole and c) Sextupole.	13
1-5	Mechanical support structure concept for a short high-field Nb ₃ Sn dipole magnet. The structure provides both radial and axial support to the superconducting coils, which are subjected to large electromagnetic forces during operation.	15

1-6	Implementation of external quench heater circuits in the new MQXF Nb ₃ Sn quadrupole magnet. The top plot shows the positioning of the different circuits in the high and low field region of each coil. The bottom plot depicts a schematic cross section assuming a circuit based on stainless steel stations [98].	22
2-1	Schematic view of the LHC tunnel. The four main experiments are indicated in magenta. Figure modified from [104].	31
2-2	LHC and HL-LHC baseline plan for the next decade and beyond. Luminosity and centre-of-mass proton collision energy values are shown in red. Updated in 2020 [106].	33
2-3	MQXF magnet cross section and side view. The position of the mechanical instrumentation is indicated by the round markers. Vertical lines define the longitudinal position of the measuring stations for the long magnets.	35
2-4	a) Non-Cu critical current density as function of the total magnetic field for the reference strand specifications. It includes the self-field correction and assumes a 5% critical current density degradation due to cabling. b) MQXF non-linear magnet load line. The operation regime is represented by the empty dot. The short sample limits are instead highlighted with a triangular and squared marker.	38
2-5	Magnetic flux density [T] for the iron parts in one quarter of the magnet cross section.	39
2-6	2D cable and strand disposition for a magnet octant. The magnetic flux density in T is shown for all the strands. The coil peak field is identified for the inner layer conductor located in contact with the pole.	40
2-7	MQXF short model coil design. Views from the outer layer (top) and inner layer (bottom).	41

2-8	Computed temperature margin for each superconducting strand in one magnet octant. The peak field strand shows the lowest temperature margin, defining the corresponding value for the magnet.	44
2-9	MQXF temperature margin for the peak field conductor obtained at nominal current ($B_p = 11.4$ T and $I = 16\,470$ A). The operation regime is represented by the empty dot. The current sharing temperature is highlighted with a squared marker.	44
2-10	Rendered view of the MQXFS short model magnet. Half of the stainless steel shell for He containment has been removed for visualization purposes. The longitudinal support system based on tie rods and end plates can be seen in the left image.	46
2-11	Pre-loading sequence using the bladder and key concept: a) the pressurized bladders are inflated, opening the required gap for key insertion, b) the keys are inserted and the bladder pressure is released, c) cool down process and d) magnet powering. Red arrows provide an schematic representation of the forces in the structure.	47
2-12	View of the longitudinal support system composed of four tie rods and two thick end-plates.	48
2-13	Voltage taps installed in the MQXF short model magnets. Inner layer taps are named using the I- prefix, while outer layer ones use the O- prefix.	50
2-14	Γ function computed for the MQXF insulated conductor at a constant peak field and at the reference RRR value. The conservative limit of 300 K is shown by the round marker and corresponds to 34 MIITs. . .	51
2-15	Computed MQXF analytical current decay for reference parameters, assuming that all the conductor length is quenched instantaneously at $t = 0$ s. The concept of time margin is highlighted by the red diagonal pattern.	52
2-16	Geometry of the outer layer quench heater strips. The heating stations are shown in grey, while the copper cladding is drawn in red. . . .	53

3-1	Quench simulation diagram. Arrows depict the coupling mechanisms, while the boxes group the main entities in hierarchical order. For the sake of a better understanding, we just explicitly refer to the shared variables between models (For example, important quantities as the peak voltages can be considered as internal results of the corresponding model).	64
3-2	Analytical and finite element solution for the 1D boundary value problem solved in the Annex. The element size (l) is indicated by the black arrow. 5 elements have been considered.	68
3-3	Quench simulation using a thermal-electric model. In this case, the magnetic part is completely decoupled. The necessary information is instead accounted as an external input to the calculation (B , P_{loss} and L_{magnet}).	71
3-4	Element types used for the 3D FEM thermal-electric model.	75
3-5	Thermal conductivity as a function of the temperature for the materials used in the 3D FEM model. When applicable, the material properties are evaluated at a magnetic field of 11.071 T (corresponding to the average field value found in the MQXF pole turn conductor at nominal magnet current).	78
3-6	Heat capacity as a function of the temperature for the different materials used in the quench simulation. A consistent magnetic field value of 11.071 T has been used for the calculation of the equivalent conductor properties.	79
3-7	Description of the stop and restart process implemented in ANSYS APDL for the simulation of quench events.	80
3-8	Experimental cable assembly setup. Schematic extracted from [192]. .	86
3-9	Detailed position of the voltage taps and the corresponding conductor segments. The homogeneous field region is indicated by the black arrow. The figure has been extracted from [192].	87

3-10	Comparison of experimental results, extracted from [192], with FEM simulations. The top plot shows only the signals from the experiment, while the bottom one superposes the results from the 3D model. The naming of the different segments corresponds to Figure 3-9.	88
3-11	Components of the modelled cable assembly.	89
3-12	Spatial and temporal discretization study for the test at 4.35 K, 17.45 kA and 10.08 T. Converged solution is practically obtained with a time step size of 10 ms and a longitudinal element size of 0.5 mm	90
3-13	Model and experimental results comparison for the test at 13 kA, 4.35 K and 9.96 T.	91
3-14	Comparison for the signals obtained from the third experiment at 17.45 kA, 1.9 K and 8.49 T.	92
3-15	Quench propagation velocity as a function of the magnetic field. As for the previous plots, the experimental data has been extracted from [192].	93
3-16	Comparison of test results with the FE model and analytical formulas. The average magnetic field takes into account the cable self-field component.	94
3-17	MQXF Thermal-Electric FE models for the magnet straight section. The position of both quench heater stations is indicated, corresponding to the high field and low field outer layer conductor blocks.	96
3-18	MQXF Thermal-Electric FE octant model for the full coil. Volumes are shown. The insulation has been hidden for visualization purposes. The division between the conductor straight section and the curve ends is visible in both layers.	97
3-19	Measured and computed current decay in two different quench integral tests performed for MQXFS5 and MQXFS4 magnet. The effect of the IFCL in the current discharge is clearly visible at high current.	99

3-20	Comparison of the model predictions with experimental signals for different controlled quench discharges in two MQXFS magnets. The top plot shows the case of MQXFS5, while the bottom plot shows the one of MQXFS4.	100
3-21	Measured and calculated delays when firing the OL-HF and OL-LF units respectively. Markers show the experimental values obtained during the test of MQXFS4 magnet. The dashed line represents the results obtained with the Finite Element Model.	101
3-22	Delays to quench measured from the moment the heaters are powered. Markers show the experimental results, and the dashed lines depict the values obtained from the simulation. The bottom plot corresponds to tests performed in the low-field block, the top to the high-field one. .	102
3-23	Average coil temperature as a function of the magnet stored energy density or conductor enthalpy. The solid line corresponds to the analytical adiabatic estimate and the markers show the experimental extracted values from quenches protected with only QH. The good agreement between both confirms that the released stored energy is, in practical terms, completely absorbed by the coil enthalpy.	104
3-24	Coil and magnet structure temperature after a quench integral test at nominal current. As it can be seen, due to the fast nature of the transient and the coil insulation, the magnet structure remains cold through the complete discharge.	105
3-25	Top: Resistive voltage growth for pole turn (I7I8) and layer jump segments (I8O1). Dotted lines with markers show the measured data, solid lines represent the Finite Element Simulation. Bottom: Voltage derivative for both segments.	107

3-26	Temperature results from the simulation of a reference quench happening at nominal current. The bottom plot shows the 3D coil geometry, where the insulation and the winding pole are hidden for visualization purposes. The top plot shows the same simulation performed using the magnet straight section.	109
4-1	2D octant mechanical model.	118
4-2	Boundary conditions for the 2D octant mechanical model.	118
4-3	Full two-dimensional magnet model. The vertical displacement is constraint in P1, the horizontal in P2 and the rotation in P3.	119
4-4	3D octant mechanical model.	120
4-5	Thermal strain as a function of the temperature for the model materials.	122
4-6	Pole azimuthal stress as a function of the shell stress for a dummy magnet assembly. The red lines show the results of the 3D model, while the green boxes depict the measured variation from the different strain sensors. Figure courtesy of G. Vallone.	125
4-7	Mechanical transfer function plot for MQXFS1 model magnet. The red lines show the results of the 3D model and the black one corresponds to the 2D simulation. As for the previous plot, the green boxes indicate the measured variation from the different strain sensors. Figure courtesy of G. Vallone.	126
4-8	Delta in azimuthal pole stress during the MQXFS5 magnet ramp. Two red curves show the 2D model results: 1) the solid line with markers corresponds to the simulation where the coil can detach from the coil and 2) the dashed line represents instead the case where the interface is bonded. The black vertical lines indicate the nominal and ultimate current level.	127
4-9	On the left, coil radial stresses during the three main simulation steps. On the right, corresponding azimuthal stresses. A peak stress of 125 MPa is found during cool-down in azimuthal direction.	128

4-10	Coil Von Mises equivalent stress computed with the 2D octant model. The peak stress during operation equals 110 MPa and is located at the coil mid-plane.	129
4-11	2D force redistribution in the MQXF magnet structure for the reference case.	130
4-12	In the left, coil azimuthal stress computed in the three-dimensional model. In the right, Von Mises equivalent stress distribution.	132
4-13	Coil radial stress (left) and coil longitudinal stress (right) distributions for the main load steps.	133
4-14	Schematic showing the azimuthal prestress (S_0) and the azimuthal electromagnetic force (F_θ) acting on a $\cos(\theta)$ coil.	135
4-15	Comparison between the three-dimensional simulation of the current ramp in MQXFS5 magnet (orange) and the measured values. The delta in azimuthal stress seen in the winding pole is shown.	136
4-16	Top) Resulting temperature distribution from the 3D Thermal-electric FEM model. The division of the various conductors defines the coil domain. Bottom) Corresponding temperature field transferred to the mechanical model using mesh interpolation. In this case, the conductor is modelled as an homogeneous block. The temperature distribution is well preserved during load transfer, as well as the minimum and maximum values.	137
4-17	Raw strain readings from the active gauge and the compensator for a standard quench event (azimuthal strain for the active gauge). The left column provides an overview on the full event, while the right one plots a zoom in the region of interest.	141
4-18	Current cycle for the quench whose measurements are depicted in Figure 4-17.	142

4-19	Two-dimensional computed temperature in the winding pole for the reference quench case at nominal current. The image shows that the hot-spot conductor is located next to the inner pole piece, thus resulting in the highest temperature at the position of the mechanical instrumentation. Note that adiabatic boundary conditions are assumed.	143
4-20	Azimuthal and longitudinal stress seen in the winding pole for a standard training quench in MQXF. The solid blue line depicts the strain gauge readings, and the dashed red line the corresponding FBG response. The "quench delta" is highlighted by the black arrow.	144
4-21	Comparison between the quench delta values measured by strain gauges and optic fibers for several quenches. The fit information is shown in the right box, where RMSE stands for "Root Mean Square Error". . .	145
4-22	Comparison between experimental and simulated values for the quench delta in azimuthal pole stress. In the top plot the results from the two-dimensional simulation are shown. The bottom plot depicts instead the case of the three-dimensional model. For the experimental values, the range between the four coils is indicated. Dashed lines represent the quadratic fit of the data.	147
4-23	2D simulation of the az. quench delta in MQXFS5 magnet (for quench integral discharges at various currents) using a variable friction coefficient between the coil blocks and the pole.	148
4-24	Coil azimuthal stress contours at the end of the 16.47 kA QI test (see Figure 4-22). The plots have been produced with the 2D octant geometry for: top) glued coil/pole interface and bottom) sep.no-sliding assumption. The only difference between both results is identified in the vicinity of the inner radius corner node, where the stress is intensified by 10 MPa in the separation case.	149

4-25	Parametric study performed with the 2D octant model. The left column shows the effects on the quench delta of the thermal expansion coefficient of the coil, while the right column shows the impact of the magnet preload. The first row depicts the results of the glued model and the second one, those from the sep./no-sliding approach.	151
4-26	Measured and simulated magnet current. Magenta color is used to highlight the FEM initial condition, the red color indicates the value at nominal current and black shows the end of the quench discharge.	152
4-27	Delta in pole azimuthal and long. stress for the controlled quench discharge at nominal current. Blue lines show the experimental results for the four coils, while the markers depict the results from the simulation. The color code is consistent with Figure 4-26. Arrows show the FEM and measured delta. The effect of the quench is well captured in both directions. The glued contact assumption results in a larger stress at nominal current in az. direction and in a smaller quench delta. In longitudinal direction the relationship is inverted.	153
4-28	Delta in the shell azimuthal and long. stress for the same case presented in previous figure. In azimuthal direction, the model overestimates the delta due to powering. However it still predicts a very small stress increase during the first ms after the quench (first green marker above the red one), in line with the experimental evidences. On the contrary, the posterior decrease is not observed in the test results. In longitudinal direction, measured and computed signals agree.	154
4-29	Delta in longitudinal stress measured at the tie rods. Although the model predicts a lower stress variation due to powering, the delta during a quench keeps the consistency with the measurements (arrows). .	155

4-30	Coil peak stress evolution during three of the quench integral discharges simulated for MQXFS5. The dashed horizontal lines show the corresponding stress values after the cool-down to cryogenic temperature (red color is used for the VM equiv. stress and blue for the azimuthal stress). The azimuthal and VM equiv. stress values are extracted from the coil elements in contact with the winding pole.	156
4-31	Coil azimuthal stress distribution at the end of the 16.47 kA QI discharge (top, reproduced from Figure 4-24) and the corresponding temperature distribution predicted by the multi-physics quench model (top).	157
4-32	Azimuthal (left) and VM equiv. stress (right) contours using the full (top) and symmetric (bottom) magnet models. The plots show the results of the reference quench simulation at $t = 0.4$ s. The results prove the conservative nature of the symmetric approach.	159
4-33	2D stress contours at the end of the reference quench discharge. In the top row, the azimuthal (left) and VM equiv. stress (right) are shown. The bottom row depicts instead the radial (left) and shear stress (right) in the coil.	161
4-34	Peak stress evolution in the coil during the reference quench. $t = 0$ s corresponds to the moment the quench starts. As in the QI tests, the VM and az. stress values are extracted from the coil elements in contact with the pole. Red color is used for the VM equiv. stress, while blue is chosen for the azimuthal one. The same color code is kept for the dashed lines, which depict the corresponding constant value at cryogenic temperature (just after magnet cool-down).	162
4-35	Impact of different assumptions on the VM peak stress during quench. Markers highlight the average value and blue boxes shows the range of variation.	163
4-36	Peak stress quench position dependency.	165

4-37	In the top: coil azimuthal stress distribution after magnet cool-down (left) and at the end of the quench discharge for the reference event at nominal current (right). The effect of longitudinal quench propagation is clearly visible in the plot. The location of the peak stress corresponds to the position where the temperature is higher. In the bottom: coil equivalent stress distribution. A peak stress close to 140 MPa is located in the coil ends.	166
4-38	Azimuthal stress difference between the quench case and the cool-down values in the hot-spot region.	167
4-39	Top: Coil radial stress after magnet cool-down (left) and at the end of the quench (right). The frontal view highlights the peak value, found close to the hot-spot location. Bottom: Coil longitudinal stress contours.	168
4-40	Definition of the two paths along the coil straight section.	169
4-41	Top row) computed azimuthal stress along the two different paths. Bottom row) results for the longitudinal stress component along the same paths. The quench creates the largest stress gradient in long. direction. In addition, the delta with respect to the cool-down is maximum in the magnet centre (90 MPa), and differs slightly from the value reported in the text (100 MPa) due to the path position. The red marker adds the result from the 2D simulation.	170
4-42	Azimuthal stress (top) and Von Mises equivalent stress (bottom) along a path that follows the mid-radius of the coil inner layer, from 0° to 45°. The 2D results from the detailed and average temperature approaches are depicted. Note that the cooper wedges and the titanium poles are included in the path.	173
4-43	Quench delta measured for various controlled discharges in diverse MQXFS magnets. The average of the four coils is shown with markers, while the vertical lines define the measured variation between them. Dashed lines correspond to the quadratic fit of the avg. values.	174

4-44	Computed and measured current discharge for the High MIITs test in MQXFS4.	175
4-45	The first row shows the detailed temperature distribution at the end of the magnet discharge. In the middle, the azimuthal stress distribution is shown (also for the end of the discharge). The third row plots the VM. equiv. stress values.	176
5-1	Example of the B_{c2} evolution for a Nb ₃ Sn bronze wire subjected to axial strain. The diamond markers represent the data derived from critical current measurements and the curves show the fits calculated using only six points around $B_{c2\ Max}$ (those limited by the vertical dashed lines) for different scaling equations. The exponential form described in the text is shown in red. Figure reproduced from [76].	186
5-2	RRP 0.85 mm 108/127 investigated wire's cross section.	192
5-3	Principle of the Walters spring (WASP) design. The superconducting wire is soldered inside a rectangular-shaped groove. The axial strain is exerted by applying a torque to the spring's upper end.	193
5-4	3D model and cross-sectional cut of the C-WASP device conceived by Seeber et al. at University of Geneva. The sample holder is made of Stainless Steel 316L and Copper OFHC.	197
5-5	Critical current evolution as function of the applied axial strain for the reference wires measured in the WASP device. The top graph corresponds to the results of the round wire, while the bottom plot shows the case of the rolled sample. Only the measurements at 19 T are shown for simplicity.	201
5-6	Critical current evolution as function of the applied transverse stress (computed using Equation 5.15) at 19 T. Full markers depict the I_c value when the load is being applied, empty markers show the critical current value upon force removal.	204

5-7	Detailed zoom of the WASP measurement. Markers display the interpolated values of strain for the C-WASP samples.	209
5-8	Mechanical stress-strain measurements performed for two straight samples of the investigated round wire. The bottom-right box shows a detailed zoom in the area indicated by the dashed rectangle. The force required to induce a strain in the range of 0.05% – 0.1% equals 20 N - 30 N.	210
5-9	Critical current evolution as function of the applied transverse stress using normalized values. In the top plot, the results for the round wires are shown. The bottom plot, instead, depicts the results for the 15 % rolled samples. In both cases, the horizontal dashed line shows the 95 %, while the red box contains the range at which this line intersects the unload curves of the three wires.	211
5-10	B_{c2} evolution after force removal for the investigated wires and a PIT sample measured in [59]. The fact that the decrease in B_{c2} for the RRP wires with zero initial applied strain is approximately 50% of the one reported for PIT, indicates a different degradation mechanism.	213
5-11	Longitudinal strain values along a path that travels through the straight section of the inner layer pole block.	215

List of Tables

1.1	Properties of main industrial LTS Type II superconductors	6
2.1	Strand specifications.	36
2.2	Cable specifications.	37
2.3	Coil and magnet parameters*.	42
2.4	Magnet margin concepts.	43
2.5	Analytical mechanical estimates for MQXF (single sector coil approx.).	45
2.6	Outer layer quench heater circuit powering parameters.	54
2.7	CLIQ parameters.	55
4.1	Coupling strategy for the structural analysis	113
4.2	MQXF - Material properties used in the mech. model	122
4.3	Coil block mechanical properties [129]	123
4.4	MQXF - Material limits	134
5.1	RRP 0.85 mm 108/127 reaction heat treatment	192
5.2	Technical specifications for the C-WASP test	198
5.3	Round wire: B_{c2} values extracted from the WASP experiment	202
5.4	Rolled wire: B_{c2} values extracted from the WASP experiment	203
5.5	Round wires: B_{c2} values extracted from the C-WASP experiment . .	206
5.6	Rolled wires: B_{c2} values extracted from the C-WASP experiment . .	207
5.7	Summary on wires' results	212

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