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1 Superelastic cellular NiTi tube-based materials: fabrication, 2 experiments and modeling

3 G. Machado^{*,a,b}, H. Louche^a, T. Alonso^b, D. Favier^b

4 ^a*LMGC, CNRS-Université de Montpellier 2, Place E. Bataillon CC048, 34095 Montpellier, France*

5 ^b*UJF-Grenoble1, CNRS, TIMC-IMAG - UMR5525, Grenoble, France.*

6 **Abstract**

The aim of this paper is to present an experimental and modeling study as the first step towards designing and optimizing architected materials constituted of NiTi tubes. The idea is to combine the intrinsic and novel properties of nickel-titanium shape memory alloys with purposely engineered topologies. By joining thin-wall superelastic tubes via electrical resistance welding, we create regular cellular material demonstrators. The superelastic behavior of two simple architected materials based on identical tubes, but with two topologies, are experimentally characterized and modeled using finite element approaches. The predicted behaviors are compared by simulating complex loading, exploring the influence of the constitutive material behavior on the effective mechanical properties of cellular materials. The parameters of the constitutive equations are identified on tensile tests performed on small dog-bone shaped specimens, machined from the tubes by spark cutting. The modeling results are finally compared with compression tests performed on these simple architected NiTi materials. As a further validation of the proposed study, two large cell structures (square and hexagonal stacking) were modeled to gain greater insight into the role of different architectures.

7 *Key words:* NiTi Shape Memory Alloy; NiTi tubes; resistance welding; architected
8 material;

9 **1. Introduction**

10 Nickel-titanium (NiTi) shape memory alloys (SMA) have been used in a wide variety
11 of consumer products and industrial applications; such as in automotive [1], aerospace [2],
12 biomedical [3] and many other potential industrial commercial markets [4] due to their
13 remarkable superelastic properties, shape memory effects, and biocompatibility. These

*Corresponding author

Email address: guilherme.machado@imag.fr (G. Machado)

¹Phone: +33456520086 Fax: +33476768844

14 alloys can withstand more than 8 % strain during a superelastic tensile test with practically
15 full recovery of all this deformation during unloading. This leads to a highly non-linear
16 stress-strain curve that includes hysteresis.

17 Despite being widely used in a broad range of industries, the implementation of NiTi
18 in structural applications can be expensive and complex due to limitations in traditional
19 fabrication processes, e.g., machining [5] and casting [6]. More so than other materials,
20 NiTi SMA properties are significantly affected by the fabrication processes [7]. Given the
21 difficulties in obtaining and manufacturing NiTi parts associated with the fact that the
22 most widely manufactured commercial shapes are round cross-sectional wires and bars,
23 tubes and small rectangular cross-sectional strips, one of the solutions to address this
24 challenge is to develop an architected material.

25 Architected materials are combinations of two or more materials or of materials and
26 void, having a controlled architecture at different length scales, and configured in such a
27 way as to have attributes not offered by any bulk material alone [8]. Fig. 1 shows examples
28 of architected materials.

29 There have been several attempts to fabricate SMA architected materials using basic
30 commercial shapes. Grummon *et al.* [9] developed a novel brazing technique that allowed
31 creation of prototypes of superelastic cellular honeycomb topologies from conventional
32 NiTi precursor materials such as corrugated sheets (Fig. 1a). In a similar manner, after
33 shape-setting the honeycomb foil shape, Okabe *et al.* [10] designed a sandwich panel where
34 NiTi foils of honeycomb core were glued using a modified silicone adhesive. Hassan *et al.*
35 [11] used mechanical fasteners to construct a prototype of a smart SMA chiral honeycomb
36 using NiTi ribbons (Fig. 1b). Various combinations of SMAs and other materials have
37 improved the material performance such as triple-state changing effect presented in [12]
38 combining SMA wires and shape memory polymer matrix. Marcadon *et al.* [13] presented
39 another interesting study exploring the influence of the constitutive material behavior
40 on the effective mechanical properties of brazed cellular materials using tubes made of
41 Inconel[®] 600.

42 The present paper discusses the fabrication, testing, and finite element analysis of su-
43 perelastic cellular NiTi tube-based materials. These cellular structures made from welded
44 tubes are especially interesting for their potential to provide superelasticity and shape
45 memory in a light-weight material. Nevertheless, they can be designed to have high
46 stiffness-to-mass ratios and desirable energy absorption characteristics since their stress-
47 strain curves may exhibit large hysteresis loops. Multi-tube structures allow combination

48 of different diameter-wall thickness ratios and stacking topologies. The structure effect, by
49 exploiting wall bending, can substantially amplify the SMA intrinsic properties as com-
50 pared with the monolithic SMA volume. In this context, Section 2 starts with a brief
51 description of resistive welding, where the set-up and fabrication of two demonstrators of
52 cellular structures are outlined. In Section 3, all precautions concerning the experimental
53 set-up are emphasized and an analysis is conducted concerning the thermal transformation
54 behavior of the received material. In the same section, mechanical tests are divided into
55 three categories: (1) uniaxial tensile tests; (2) single tube under radial compression by
56 flat loading surfaces and (3) two cellular demonstrators under compression. In Section 4,
57 experimental data are compared to a finite element model carried out using a SMA me-
58 chanical model fitted only on uniaxial tensile tests. Based on previous results, two large
59 cell structures (square and hexagonal stacking) were modeled to gain greater insight into
60 the role of different architectures. Finally, Section 5 contains some concluding remarks
61 and outlines some future perspectives.

62 **2. Tube stacking specimen fabrication**

63 Electrical resistance welding was used as technique to join NiTi stacked tubes. Analysis
64 of the experimental evidence presented in [14] demonstrates that resistance welding is a
65 feasible technique for joining NiTi tubes for the design and creation of complex structures
66 with high reversible elasticity.

67 In this study, NiTi tubes were provided by Minitubes (Grenoble, France), with a
68 nominal composition of Ti-50.8 at.% Ni. Tubes were obtained by the cold drawing process.
69 The end tube dimensions are the result of a series of drawing passes through different
70 die/mandrel sizes in order to progressively reduce the inner and outer diameters [15, 16].
71 The tube has an outer diameter of $\phi_{ext} = 5.74$ mm and a wall thickness of $t = 0.31$ mm.
72 From as-received cold-worked tubes, tubular samples of $l = 5$ mm length were cut using a
73 diamond saw.

74 A DC Hughes Model VTA-60 resistance welder was used. The welding set-up is il-
75 lustrated in Fig. 2. Two cylindrical copper electrodes of 4 mm diameter and 20 mm long
76 were used to clamp the tubular samples together. A plexiglas template ensured the tube
77 position and alignment. Resistance welding was carried out in an argon atmosphere using
78 an electrical pulse energy of $E = 145$ J and a contact force of $F = 100$ N. Each weld was
79 done in a single step. The weld line was well distributed along the length of the tube and
80 approximately 0.5 mm wide, observed in a scanning electron microscope. Two cellular

81 structures, named here square (Fig. 7a) and triangular (Fig. 8a), were fabricated and aged
82 at $T = 350^\circ\text{C}$ for 60 min after welding. For further details on the effect of aging treatment
83 on the transformation behavior and on deformation behavior of Ni-rich alloys, see [17] and
84 [18].

85 **3. Experimental results and Analysis**

86 *3.1. Thermal transformation behavior*

87 The thermal transformation behavior of cold-worked material and samples aged at
88 350°C for 60 min were determined by differential scanning calorimetry, using a DSC Q200
89 V24.4 instrument. The heating and cooling rates were set at $10^\circ\text{C}/\text{min}$. All measurements
90 started with heating from $T = 40^\circ\text{C}$. Fig. 3 shows the DSC curves of the cold-worked
91 and the aged samples for two different zones. Fig. 3a was obtained with DSC specimens
92 taken far from the welded joint. Fig. 3b represents DSC specimens in the welded joint.
93 Thus, four different states are defined: *state 1*, the cold-worked material; *state 2*, the aged
94 material; *state 3*, the welded zone before aging; *state 4*, the welded zone after aging. For
95 *state 1*, the peaks have a low intensity. This is consistent with previous studies for cold-
96 worked NiTi [19, 20]. *State 2* exhibits flat but distinguishable A-to-R during cooling and
97 R-to-A during heating transformation peaks above room temperature. The R-to-M and
98 M-to-R transformations are undetectable due to their extremely low intensities or their
99 low temperatures. Concerning *states 3* and *4* nothing can be precisely concluded since the
100 DSC samples are not homogeneous over the welded zone. The austenite finish temperature
101 (A_f) was determined to be under 60°C in all states.

102 *3.2. Uniaxial tensile tests*

103 In order to determine the mechanical behavior of tube specimens, uniaxial tensile
104 tests were performed on small dog-bone shaped specimens machined from the tubes by
105 spark cutting. The samples had an initial gage length $l_0 = 6\text{ mm}$, width $w_0 = 0.8\text{ mm}$ and
106 thickness $t_0 = 0.31\text{ mm}$, as shown in Fig. 4. All tests on the following were performed using
107 a Gabo Explorer testing machine with a $\pm 500\text{ N}$ load cell and with specially designed grips.
108 The testing temperature was controlled using a furnace in air with fanned convection
109 with an accuracy of $\pm 1^\circ\text{C}$. In all cases, the testing temperature was approached by
110 cooling from a temperature higher than 60°C . Test images were recorded at 10 Hz with
111 a Jai TM-4200GE CCD 1024×1024 pixel camera and the spatial resolution achieved
112 for digital image correlation (DIC) was 0.05 mm . Strain was calculated by averaging
113 the strain field over the gage zone. The tests were performed at a global strain rate of

114 10^{-5} s^{-1} . Strain fields were calculated for the sequence of images corresponding to loading
 115 and unloading. Fig. 5a shows the nominal stress-strain first load-unload curves of the
 116 uniaxial sample tested at 60, 70 and 80 °C. The effect of the testing temperature on critical
 117 stresses for the forward and the reverse transformations was found to follow the Clausius-
 118 Clapeyron relationship with a slope close to 6.5 MPa/°C. The samples tested above 60 °C
 119 demonstrated pseudoelastic behavior, recovering any minor remaining deformation upon
 120 heating. Fig. 5b shows the transformation and plastic strain until failure at 60 °C for a
 121 sample aged at 350 °C for 60 min (MAT1), with the sample solution treated at 850 °C for
 122 60 min followed by a 350 °C for 60 min aging (MAT2). Note that results were expressed
 123 in terms of logarithm and Green-Lagrange strains measures and their respective stresses.
 124 These results will be used to model the plastic behavior of tubes. For simplicity, we assume
 125 negligible temperature effects in the plastic hardening curves. The uniaxial behavior
 126 of MAT2 will be used to approximate the mechanical behavior of welded zones (WZs).
 127 Indeed, as pointed out in [14], the microstructures of a heat treated (solution treated
 128 and aged) and weld fusion zone are very similar. They contain large grains with almost
 129 identical shape and size.

130 3.3. Radial compression test of a single tube

131 Research on the large deformation behavior of NiTi tubes under quasi-static radial
 132 compression is not new in the literature. This type of test was used, for example, by [21]
 133 in the design of passive vibration isolation. [22] performed radial compression of tubes
 134 with different diameter-wall thickness ratios (ϕ_{ext}/t). They highlighted the “giant supere-
 135 lasticity effect”, that combines the geometry effect, expressed by the ϕ_{ext}/t ratio, and the
 136 intrinsic properties of NiTi SMA. [23] conducted a systematic investigation on deformation
 137 behavior and influences of geometric dimensions for different boundary constraints.

138 In the present work, tubular samples of $l = 5 \text{ mm}$ length were radially loaded between
 139 steel platens without lubrication. The lower head was fixed and the upper one was the
 140 loading head moving at 0.1 mm/s. Force and displacement sensors recorded the force-
 141 displacement curves, presented in Fig. 6, at 60 and 70 °C.

142 3.4. Radial compression test of tube stackings

143 Compressive quasi-static loading was applied to the tube stacking with a constant
 144 crosshead velocity of 0.1 mm/s. Three loading-unloading cycles were successively per-
 145 formed at 4 %, 8 % and 12 % global strain ($\varepsilon_g = \Delta H/H_0$) for both cellular structures. No
 146 damage of welded zones (WZs) was observed at this deformation level. Stress concentra-
 147 tion and localized plasticity phenomena induced by the stacking geometry could result

148 in different and more complex material behavior for tube stackings compared to single
149 tubes. Figs. 7 and 8 present the undeformed configuration and deformed shapes recorded
150 by a visible CCD camera at $T = 23^\circ\text{C}$. The deformation mode for the square sample is
151 symmetric with respect to the vertical and horizontal axes, whereas the triangular sample
152 is symmetric only with respect to the vertical axis, and the deformation appears to be
153 concentrated on the upper tube.

154 Fig. 9 shows the load-deformation curves for the two samples before weld damage for
155 three temperatures. The maximum global compression strain imposed is 12% in both
156 cases. Stiffness is higher for the square sample than for the triangular one, whereas the
157 hysteretic loops are quite similar.

158 4. Finite element simulations

159 4.1. Modeling of the experimental results

160 Nonlinear finite element simulations were performed to analyze the deformation behav-
161 ior of NiTi tube structures. The Abaqus commercial finite element code was used with a
162 user material routine (UMAT) that follows the model proposed by [24]. This constitutive
163 model reproduces some basic features of shape-memory alloys at finite strains. The model
164 is based on an additive strain decomposition in which the total strain is taken as the sum of
165 the elastic strain, the transformation strain, and the plastic strain. Plastic strains develop
166 as soon as the material is loaded beyond full transformation. The work-hardening behavior
167 is assumed to exhibit tension-compression symmetry. A comprehensive treatment would
168 require knowledge of the complete yield envelope and the work-hardening characteristics
169 as a function of the stress state.

170 The material data required by the model are obtained only from observations of the
171 uniaxial tensile tests performed in Section 3.2. The data characterize the start and end
172 of the phase transformation during loading, unloading and reverse loading. The different
173 elastic constants for the austenite and martensite phases are taken into account. Temper-
174 ature effects are included as well. In this work, no volumetric transformation strains are
175 considered. Many authors [25, 26, 27] have found asymmetries of transformation stress
176 and transformation strain between tension and compression. The start of transformation
177 stress during loading in compression, is reported to be around 30% greater than start of
178 transformation stress during loading in tension. For comparison, the tension-compression
179 asymmetry is considered only in the single tube model. Table 1 presents two sets of pa-
180 rameters to model the material for the tube wall (MAT1) and the welded zone material

181 (MAT2), respectively. Table 2 presents the stress-strain points that define the yield curves.
182 All parameters are expressed in terms of Cauchy stress and logarithm strain measure.

183 Fig. 10a compares the finite element predicted curves to the experimental uniaxial
184 tensile tests. A good match with the experimental data is obtained when the temperature
185 effects are included. A continuum 8-node biquadratic plane strain fully integrated element
186 (CPE8) was used for single tube, square and triangle models. The mesh sensitivity was
187 first assessed by running multiple simulations with gradual mesh size refinement. The
188 mesh size was selected after which the results showed mesh independence. All elements
189 were chosen to be close to a square, insofar as possible, in undeformed configuration. For
190 all subsequent analysis 18 Gauss integration points over the tube thickness were used.
191 This ensured that the results were independent of the mesh geometry even though some
192 small differences can be noted at singular points. A flat analytical rigid surface was used
193 to represent the compression platens. The hard contact pressure-overclosure relationship
194 was used to define the finite-slide contact interaction between platens and tubes. Zero-
195 penetration condition was enforced by an augmented Lagrange iteration scheme that drives
196 down the penetration distance. For simplicity, a frictionless behavior is assumed. These
197 same contact settings were used to define the self-contact between neighboring tubes during
198 stacking compaction.

199 Simulation results for a single tube compression (Fig. 10b) and both architectures, i.e.,
200 square (Fig. 10c) and triangle (Fig. 10d), are also superimposed with the experimental
201 data. It is noted that the predicted results for a single tube compression (Fig. 10b) were
202 stiffer than the experimental curve. This difference further increased when a tension-
203 compression dissymmetry of 20% was considered. The difference between experimental
204 and model results could be explained by the fact that undeformed tubes did not have a
205 perfectly cylindrical shape (straightness imperfections). Then the contact surface of the
206 tube, especially in the first load increments (until 5%), was not uniform. The effects of this
207 type of small imperfection tended to decrease, i.e., to be averaged when more tubes were
208 added, as in the case of the Figs. 10c and 10d. Even with all precautions taken to align
209 tubes during welding, a small discrepancy resulting from tube misalignment can also occur.
210 On one hand, work on small samples increases the sensitivity to defects and makes the
211 comparison between the modeling and experimental results more difficult. On the other
212 hand, it is important to keep in mind the simplicity of the constitutive model. Fig. 10
213 shows relatively close agreement between the experimental data and FEA predictions for
214 all cases. However, for larger strain levels, the predicted hysteresis overestimated the

215 experimental hysteresis.

216 These first results provide information on the respective contributions of both constitu-
217 tive material properties and architecture of the cellular structure on its effective behavior.
218 The balance between these two contributions varies according to deformation level and
219 the type of architecture.

220 4.2. Role of the modeling in the design of superelastic cellular NiTi tube-based materials

221 In order to illustrate the possibility of using the previous results to gain a grater insight
222 into the role of the architecture and boundary conditions, two large cell structures shown
223 in Figs. 11 and 12 are modeled: a 6×6 square stacking and a $5(4) \times 6(3)$ hexagonal stacking,
224 containing approximately the same number of tubes. The influence of boundary conditions
225 was also evaluated using two different compression platen geometries. In the first load
226 case, flat platens were modeled by an analytical rigid surface, as shown in Fig. 11. In the
227 second load case, an analytical rigid surface composed of semicircles was used to model
228 grooved platens, as shown in Fig. 12. The compression mechanical responses of square and
229 hexagonal stackings, with the two different loading cases, are plotted in Fig. 13.

230 Due to symmetry, only one-quarter of the stackings were simulated, while applying
231 planar boundary conditions on symmetrical faces. The results were mirrored in the sym-
232 metry planes. Figs. 11a, 11b, 12a and 12b show the undeformed configurations (in solid
233 lines) and the deformed configurations (Mises equivalent stress field) under imposed dis-
234 placement, resulting in 5% of global strain in all cases. The computed deformed shapes
235 exhibited a stress concentration along the weld fillet. No adaptive remeshing procedure
236 was applied.

237 For the first load case (flat platens), no horizontal forces occurred in the square staking
238 (Fig. 11a) since no friction was considered. The maximum strain of $\varepsilon_g = 5\%$ was not
239 sufficient to activate phase transformation. Thus, no hysteresis was observed in the force
240 vs. global strain curve in Fig. 13. In the hexagonal structure (Fig. 11b), the upper and
241 lower layers moved horizontally, causing a strong deformation localization on these layers.

242 In the second load condition (grooved platens), Fig. 12a shows that square stacking
243 attained a barrel shape. This was due to horizontal forces which were not constant over
244 the entire cross section of the specimen. However, a more uniform force distribution on
245 the tube-wall network was obtained for hexagonal stacking using grooved platens.

246 The modeling results show that a more uniform force distribution was obtained us-
247 ing flat platens for the square stacking and grooved platens for the hexagonal stacking.
248 Considering the compression mechanical behavior in Fig. 13, both architectures are fully

249 recovered after load removal. This suggests that most of the integration points are in the
 250 pseudo-elastic regime. It is interesting to note that the hexagonal stacking is stiffer than
 251 the square one for both load cases. This is in agreement with previously reported data
 252 [13] for a different constitutive material. In addition, higher hysteresis is obtained with
 253 hexagonal stacking.

254 The analysis was then extended by isolating a single tube from each architecture con-
 255 sidering the second load case using grooved platens. Figs. 12c and 12d show, in detail,
 256 the equivalent strain in these tubes chosen near to the symmetry planes. This precau-
 257 tion is especially necessary to avoid border effects and ensure a periodic circumferential
 258 strain distribution. The results shown in Figs. 12c and 12d suggest that the numbers of
 259 martensitic transformation hinges (THs) are equal to the number of WZs and they are
 260 located symmetrically on the circumference of the tube. Comparing both architectures, it
 261 is possible to observe that when the number of WZs increases over the tube circumference
 262 the arc-length of cell wall decreases, causing the structure to become stiffer. At the same
 263 time, it also increases the level of cell wall bending.

264 The contour plot of the equivalent strain is shown in Figs. 12c and 12d at the moment
 265 of maximum global strain ($\varepsilon_g = 5\%$). In the square stacking, the maximum strain is less
 266 than 5%. Only a few parts of the tube near WZs experienced a phase transition and THs
 267 were not fully developed. For the same global strain level, the hexagonal stacking shows a
 268 maximum local strain of about 8.15% in WZ₂ and WZ₅. Six THs seem to be formed, but
 269 only four (TH₂, TH₃, TH₅ and TH₆) show local strains greater than the transformation
 270 strain $\varepsilon^l = 0.04$ defined in Table 1. In both architectures, no plasticity was induced since
 271 the local strains were less than those that define the beginning of the yield curve in Table 2.

272 Figs. 12c and 12d reveal that bending deformation can also activate stress-induced
 273 martensitic phase transformation in WZs. This effect is more evident in certain WZs:
 274 WZ₁ and WZ₃ for square stacking and WZ₂ and WZ₅ for hexagonal stacking. Otherwise,
 275 transformation can be attributed to stress concentration on the WZs neighborhood: WZ₂
 276 and WZ₄ for square stacking and WZ₁, WZ₃, WZ₄ and WZ₆ for hexagonal stacking.
 277 This suggests that the geometrical disposition between WZs and THs zones as well as the
 278 martensite volume fraction of each zone are important. Thus, some welded zones can also
 279 contribute with the overall structure hysteresis.

280 **5. Conclusions**

281 Modeling is very useful for designing and optimizing architected materials. In this
282 paper, the mechanical superelastic behavior of NiTi architected tube-based NiTi mate-
283 rials subjected to quasi-static compression was studied using two simple cellular samples.
284 This study demonstrated that resistance welding is a feasible technique to obtain architec-
285 tured materials consisting of NiTi with low density and high reversible pseudo-elasticity.
286 All the samples can practically recover their initial shape during unloading in the exper-
287 imental load range, and the load-displacement curves appear as hysteretic superelastic
288 loops, which are related to reversible austenite-martensite phase transformation. These
289 first results provide information on the respective contributions of the constitutive material
290 properties and architecture of the cellular structure on its effective behavior. The balance
291 between these two contributions varies according to the stress-strain level. Further work
292 to optimize the architecture, thermal treatment and process parameters is ongoing using
293 the experimental and modeling approaches described in the present paper.

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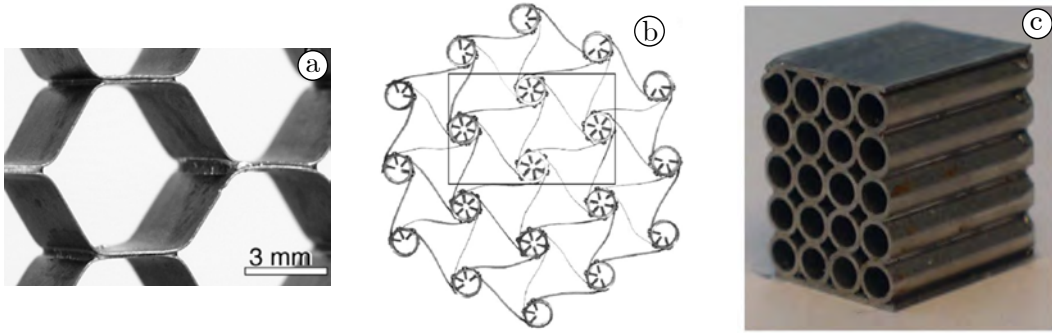


Figure 1: Some architected material examples: (a) superelastic Nitinol honeycomb structure [9]; (b) SMA chiral honeycomb using NiTi ribbons [11]; and (c) Inconel[®] 600 architected cellular material processed using a brazing heat treatment [13].

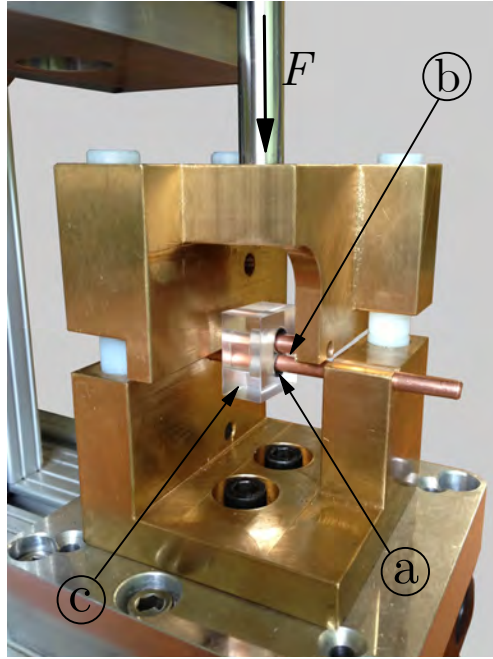


Figure 2: Tube welding set-up: (a) tubes; (b) cylindrical copper electrodes; and (c) Plexiglas template.

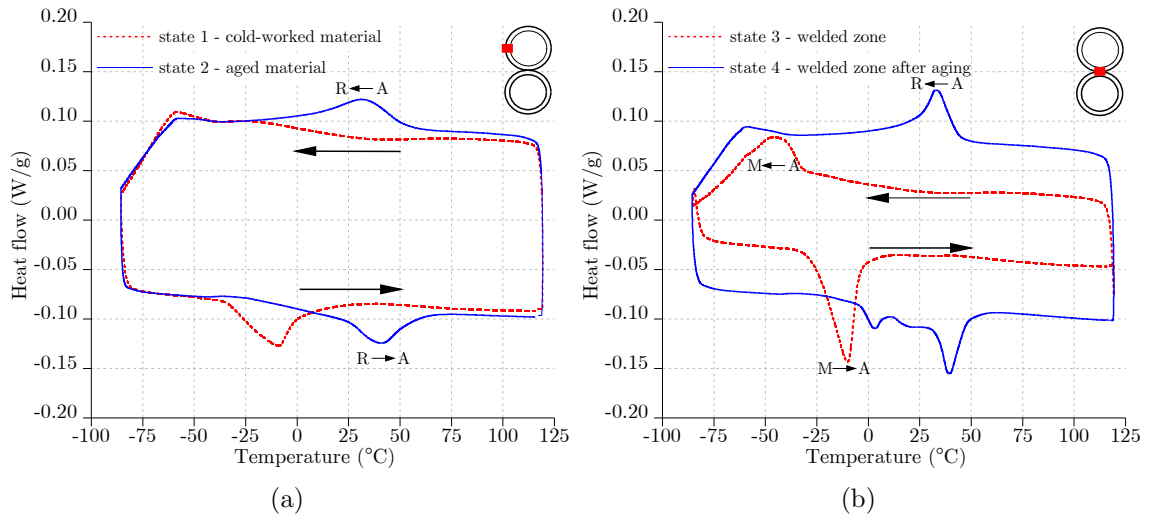


Figure 3: Thermal transformation behavior of Ti-50.8 at.% Ni for (a) zone far from the welded joint and (b) welded zone. In both cases, samples were examined before and after aging treatment at 350 °C for 60 min.

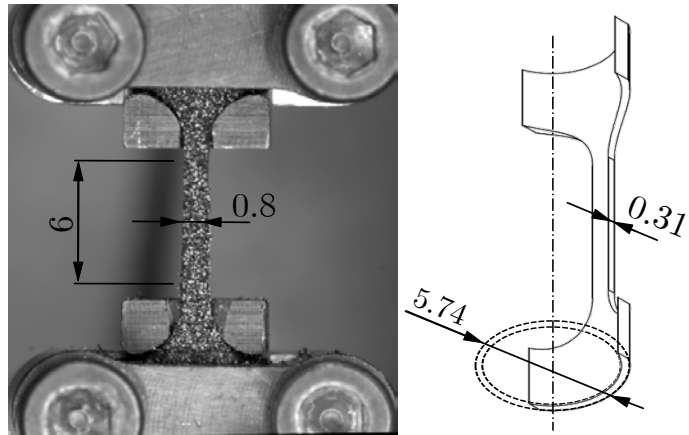


Figure 4: Uniaxial tensile specimen machined from tubes by spark cutting (dimensions in mm).

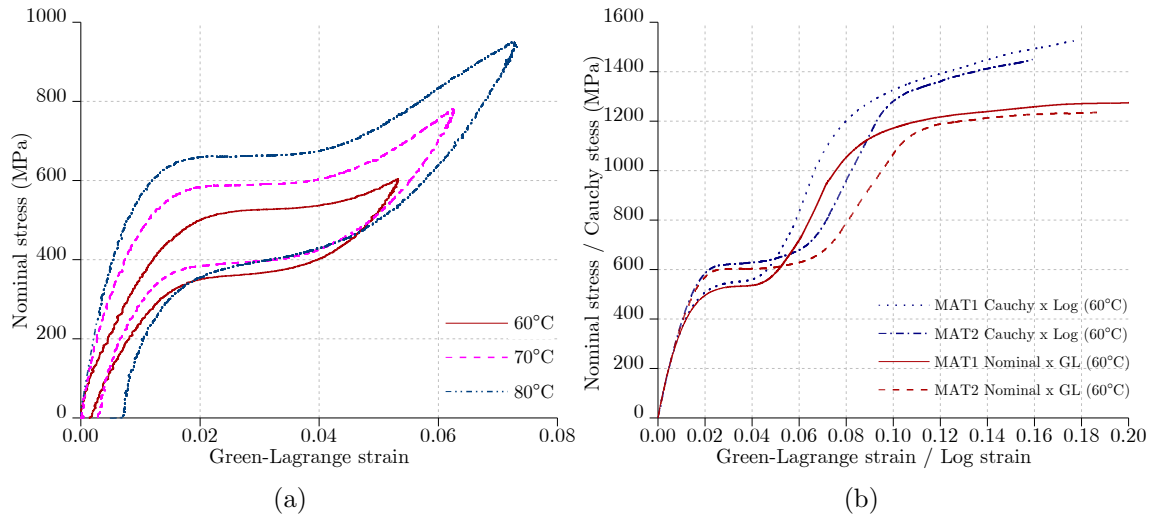


Figure 5: Nominal stress-strain curves resulting from tensile tests: (a) influence of the test temperature during loading and unloading at $T = 60, 70$ and 80°C ; (b) Ultimate tensile strength at 60°C for the specimen aged at 350°C for 60 min (MAT1) and specimen solution treated at 850°C for 60 min followed by 350°C for 60 min aging (MAT2).

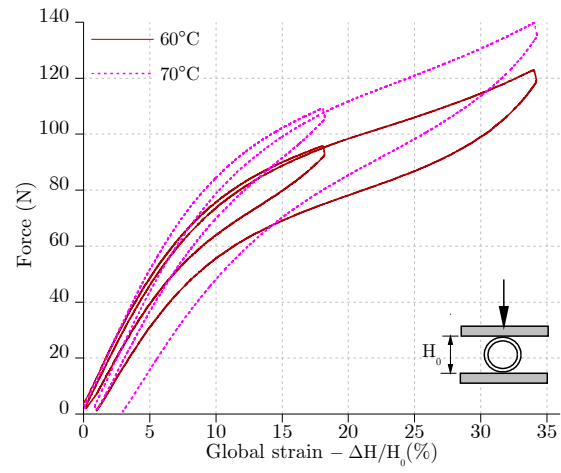


Figure 6: Force vs. global strain ($\varepsilon_g = \Delta H/H_0$) response at $T = 60^\circ\text{C}$ and 70°C of radially loaded superelastic NiTi tube $\phi_{ext} = 5.74\text{ mm}$ at a loading rate 0.1 mm/s .

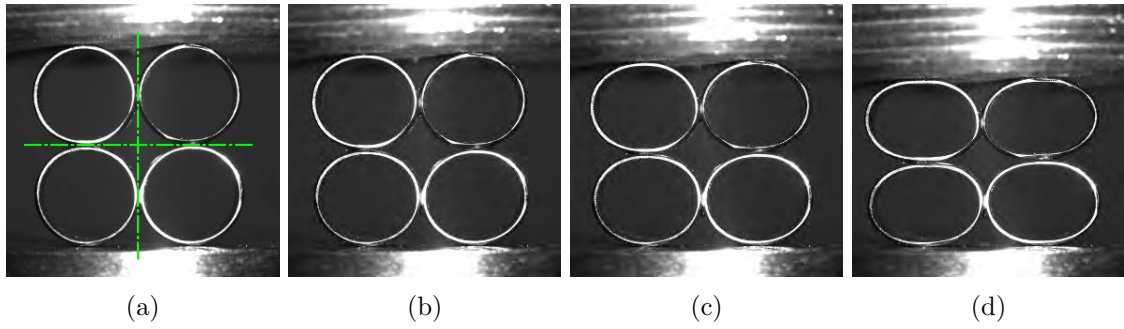


Figure 7: Visible CCD images of a square sample at $T = 23\text{ }^\circ\text{C}$ under quasi-static compression at a loading rate of 0.1 mm/s . (a) undeformed configuration with initial length $H_0 = 11.48\text{ mm}$, (b) $\varepsilon_g = 4\%$, (c) $\varepsilon_g = 8\%$ and (d) $\varepsilon_g = 12\%$.

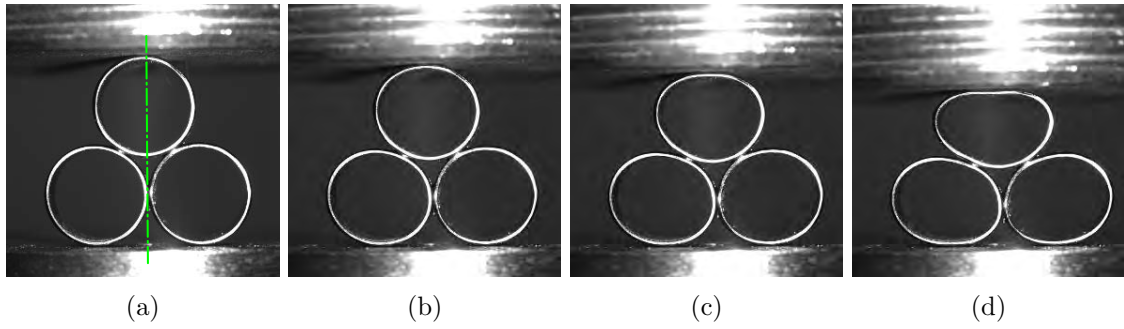


Figure 8: Visible CCD images of a triangular sample at $T = 23^\circ\text{C}$ under quasi-static compression at a loading rate of 0.1 mm/s . (a) undeformed configuration with initial length $H_0 = 10.71\text{ mm}$, (b) $\varepsilon_g = 4\%$, (c) $\varepsilon_g = 8\%$ and (d) $\varepsilon_g = 12\%$.

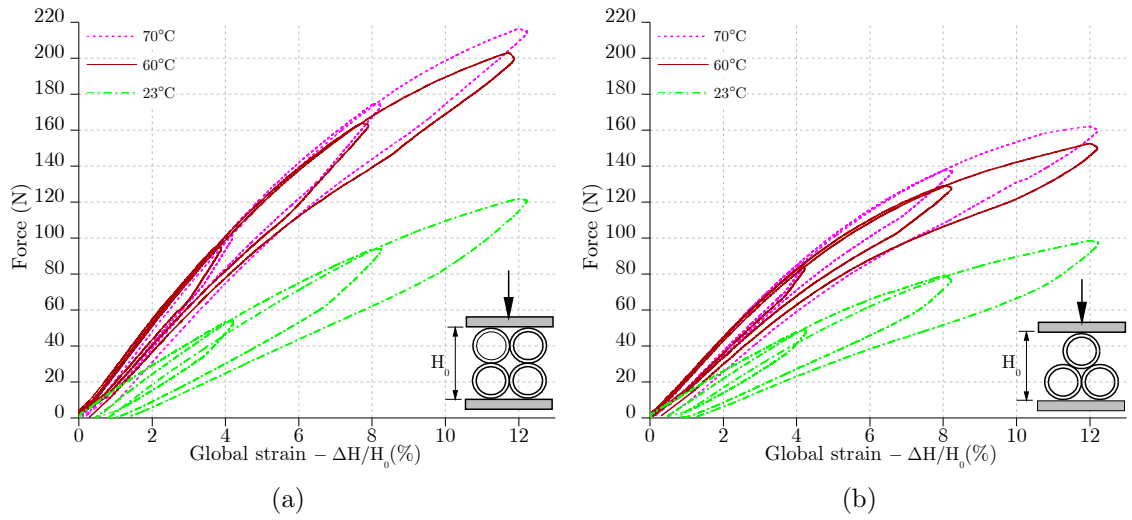


Figure 9: Load-deformation curves: (a) square sample under compression; (b) triangular sample under compression.

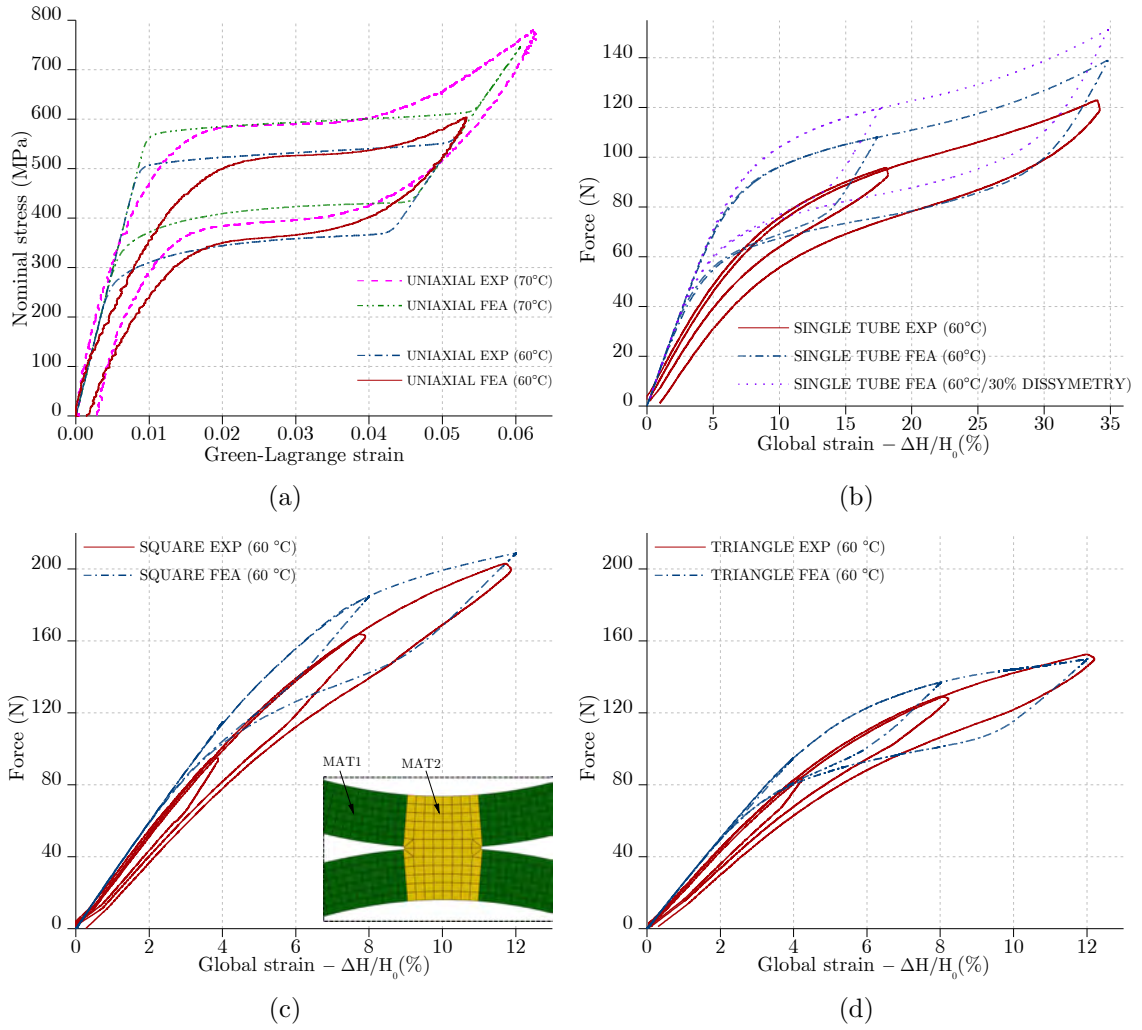


Figure 10: Comparison of experimental (EXP, red curves) and finite element analysis (FEA, blue curves) results at $T = 60^\circ\text{C}$. (a) uniaxial tensile test used for parameter identification; (b) compression behavior of a single tube; (c) compression behavior of square sample with a mesh detail of the welded zone; (d) compression behavior of a triangular sample.

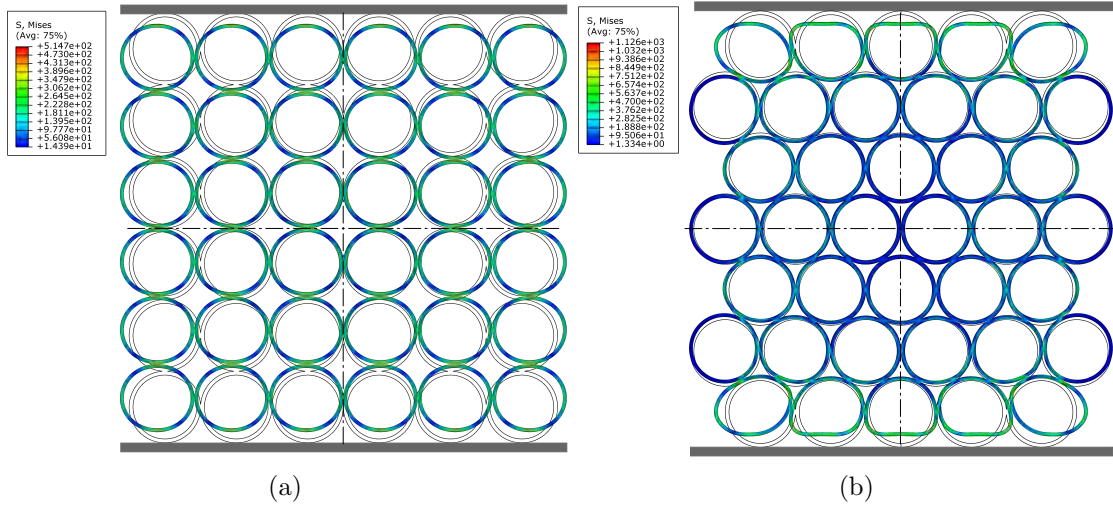


Figure 11: Equivalent Mises stress field of (a) 6×6 square stacking (36 tubes) and (b) $5(4) \times 6(3)$ hexagonal stacking (38 tubes), using flat platens. The undeformed configuration is superimposed by solid lines.

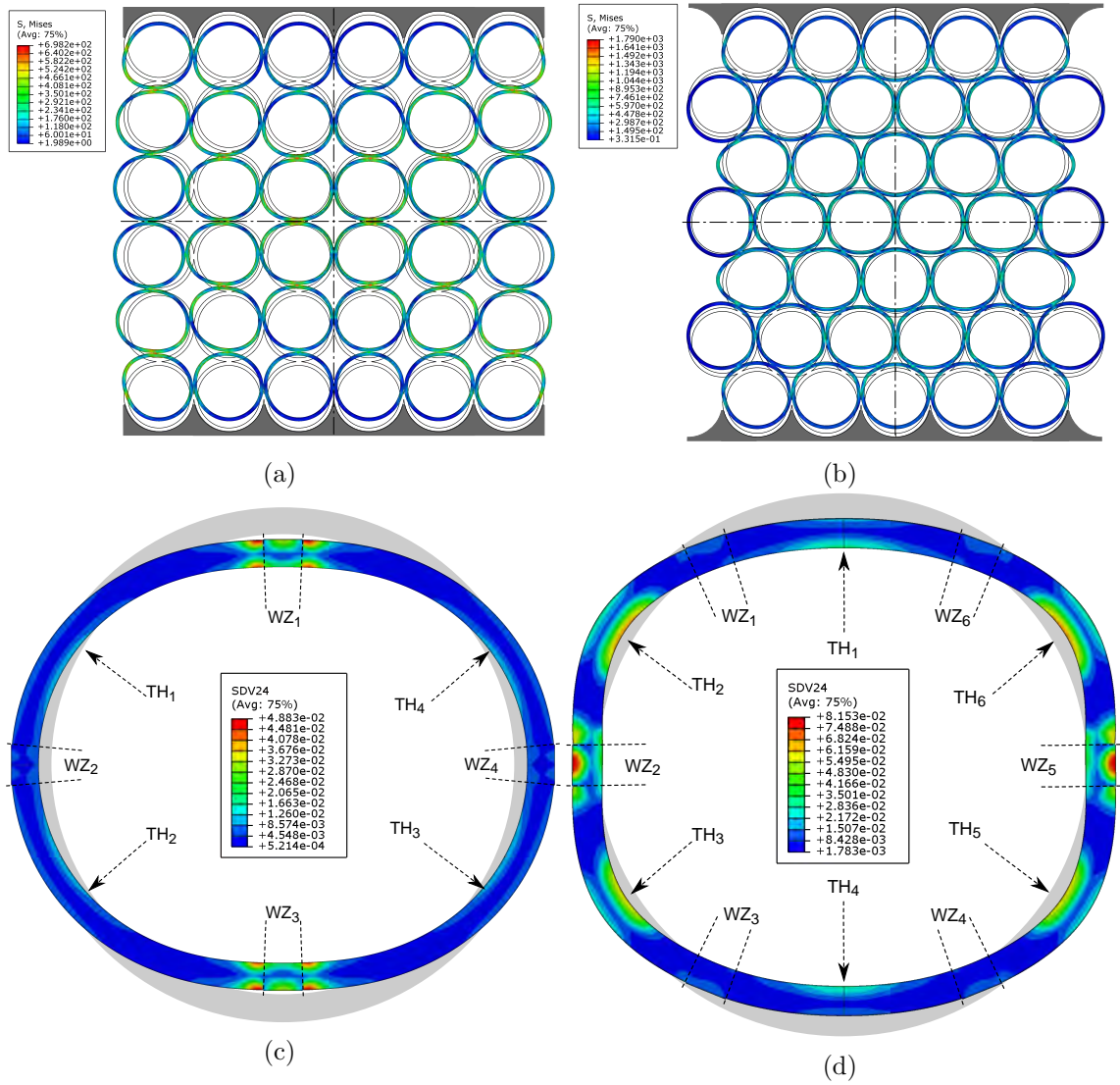


Figure 12: Finite element results of 6×6 square stacking (36 tubes) and a $5(4) \times 6(3)$ hexagonal stacking (38 tubes) using grooved platens. (a) and (b) equivalent Mises stress field. Undeformed configuration is superimposed by solid lines. (c) and (d) detail of unity tube showing the equivalent strain field for square and hexagonal stackings respectively. Welded zones (WZs) and transformation hinges (THs) are also indicated. The undeformed configuration is superimposed in gray.

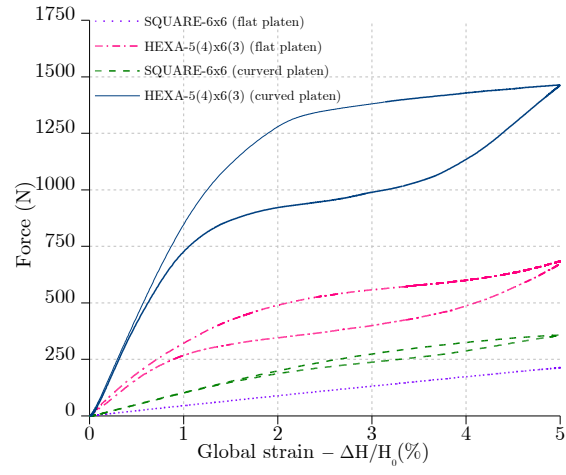


Figure 13: Compression mechanical responses of square 6×6 and hexagonal $5(4) \times 6(3)$ stackings.

Table 1: Values of the constitutive equation parameters (Auricchio's model in Abaqus UMAT)

Parameter	Unit	MAT1	MAT2
Austenite Young's modulus (E_a)	GPa	60	40
Austenite Poisson's ratio (ν_a)	-	0.33	0.33
Martensite Young's modulus (E_m)	GPa	25	20
Austenite Poisson's ratio (ν_a)	-	0.33	0.33
Transformation strain (ε^l)	-	0.04	0.05
Clausius-Clapeyron relationship ($\partial\sigma/\partial T$)	MPa/ $^{\circ}$ C	6.5	6.5
Start of transformation loading (σ_l^s)	MPa	500	600
End of transformation loading (σ_l^e)	MPa	580	720
Reference temperature (T)	$^{\circ}$ C	60	60
Start of transformation unloading (σ_u^s)	MPa	390	400
End of transformation unloading (σ_u^e)	MPa	240	350

Table 2: Stress-strain points in the yield curve (Auricchio's model in Abaqus UMAT)

MAT1		MAT2	
ε_n	σ_n^p (MPa)	ε_n	σ_n^p (MPa)
0.067	1018.0	0.093	1206.9
0.071	1078.8	0.097	1253.3
0.075	1152.1	0.101	1289.1
0.087	1261.9	0.104	1308.7
0.100	1324.4	0.110	1329.0
0.112	1368.4	0.117	1354.3
0.124	1402.4	0.126	1380.1
0.136	1436.3	0.134	1400.4
0.148	1469.4	0.143	1419.4
0.177	1524.3	0.159	1449.3