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► **To cite this version:**

Thiebaud Richeton. Stress partitioning and effective behavior of N-phase laminates in anisotropic elasticity from a fast explicit method. 2021. hal-03347969v1

HAL Id: hal-03347969

<https://hal.science/hal-03347969v1>

Preprint submitted on 17 Sep 2021 (v1), last revised 31 Mar 2022 (v3)

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Stress partitioning and effective behavior of N-phase laminates in anisotropic elasticity from a fast explicit method

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In this work, a fast explicit method, easy to implement numerically, is proposed in order to compute the effective behavior and the distribution of stresses in a general N-phase laminate made of parallel, planar and perfectly bonded interfaces. The solutions are exact for a homogenous far-field loading and work for an arbitrary number of phases, a general linear anisotropic elasticity, as well as different uniform thermal and plastic strains in the phases. A simple direct analytical formula is also derived to compute the stress in a given phase once the effective behavior of the laminate is known. Moreover, the correctness of the proposed method is checked by comparisons with finite element simulation results on a same boundary value problem, showing excellent agreements. Finally, an application of the method is performed for a near- β titanium alloy with elongated grains, by comparing the level of internal stresses for different elastic loadings within a N-phase laminate made of 100,000 orientations and a 2-phase laminate of equal volume fraction with maximal elastic contrast. Interestingly, the maximum von Mises stress of the 2-phase laminate is always the lowest, which is explained by a volume fraction effect.

Keywords laminate, anisotropic elasticity, incompatibility stresses, effective behavior, composite structure

1 Introduction

At a material surface of discontinuity like a grain boundary or an interface between two dissimilar materials, incompatibility stresses may arise due to heterogeneous thermo-elastic properties and/or difference of plastic strains. These internal stresses can be computed numerically thanks to finite element methods (Roters et al. 2010) or spectral approaches (Lebensohn 2001). Analytical formulations also exist in the case of two-phase laminates (Stupkiewicz et al. 2002; Milton 2004; Franciosi et al. 2007; Glüge and Kalisch 2014) which can also be viewed as infinite bi-crystals or bi-materials subjected to a homogeneous far-field loading (Gemperlova and Paidar 1985; Gemperlova, Paidar, and Kroupa 1989; Richeton and Berbenni 2013; Richeton, Tiba, et al. 2015; Richeton 2017). Such analytical models provide instantaneous responses which are first-order estimations around interfaces subjected to more complex boundary conditions (Peralta et al. 1993; Tiba et al. 2015). Hence, it can be very useful to test, on a huge number of configurations, the influence of different parameters like material single crystal elastic constants, crystallographic misorientation, crystal volume fraction, interface inclination, amount of plastic deformation or loading type (Gemperlova, Paidar, and Kroupa 1989; Richeton and Berbenni 2013; Richeton, Tiba, et al. 2015). In the same way, explicit analytical models for N-phase laminates are very useful to investigate the influence of the previously mentioned parameters in problems involving several parallel interfaces, like in thin films (e.g., Welzel et al. 2003; Abadias et al. 2018), in electronics packaging (e.g., Wong et al. 2008), in nanolaminated materials (e.g., Mukhopadhyay et al. 2017; Wang et al. 2017) or in materials with welding joints which are classically modeled as a base metal, a heat affected zone and a weld metal (e.g., Du et al. 2020). Actually, laminated materials are widely used in the industry because of the relative ease to control their properties from the choice of the materials combination, volume fraction and orientation (Glüge and Kalisch 2014). For the design of laminates, fast estimates of their properties is thus required.

The objective of the present paper is to derive an explicit method, which can be easily implemented in a numerical code, in order to compute the effective behavior and the distribution of stresses in a general N-phase laminate made of parallel, planar and perfectly bonded interfaces and which is assumed to be subjected to a far-field homogeneous loading and/or homogeneous temperature variation, in addition to the possibility of having different uniform plastic strains in each phase. The method should be exact for a small number of phase ($N = 2, 3, 4, \dots$) as well as for an infinitely large number of phases. Such an objective may appear relatively standard considering previous works on laminates in the literature (e.g., Milton 2004; Franciosi et al. 2007; Omri et al. 2000; Glüge and Kalisch 2014; Glüge 2016). However, it is generally hard to find compact explicit formulas for direct use in engineering applications. Besides, at the difference of previous approaches where explicit formulas were restricted to a finite number of phases

(Milton 2004; Franciosi et al. 2007) or to isotropic elasticity (Omri et al. 2000; Milton 2004; Glüge 2016) or to purely elastic behavior (Milton 2004; Glüge and Kalisch 2014), this contribution provides explicit compact formulas for an arbitrary number of phases, a completely general anisotropic elasticity as well as different thermal and plastic strains in the phases. In particular, a simple direct analytical formula is derived in order to compute the stress in a given phase once the effective behavior of the laminate has been determined. Furthermore, the code to compute the effective behavior and the stress distribution in an arbitrary N-phase laminate is made freely available for download at <https://github.com/AniPlas/Laminate>.

The paper is organized as follows. Section 2 reminds the expressions of incompatibility stresses in a two-phase laminate from which the effective compliance, effective plastic strain and effective thermal expansion tensors can be deduced. Then, Section 3 presents an explicit method to find the effective behavior and the stress distribution for a general N-phase laminate based on the solutions of the two-phase laminate. Section 4 is dedicated to the validation of the proposed method by comparisons with finite element simulation results on a same boundary value problem. In Section 5, an application of the method is performed for a near- β titanium alloy with elongated grains, by comparing the level of internal stresses for different elastic loadings within a N-phase laminate made of 100,000 orientations and a 2-phase laminate with maximal elastic contrast. The results and the proposed method are then discussed, in particular with respect to self-consistent models. Finally, concluding remarks are given in Section 6.

In the following, the Einstein summation convention over repeated indices and the contracted Voigt notation (Voigt 1928) ($11 \rightarrow 1, 22 \rightarrow 2, 33 \rightarrow 3, 23 \rightarrow 4, 31 \rightarrow 5, 12 \rightarrow 6$) are used, i.e. indices range from 1 to 6. For consistency, an engineering convention is considered for strain components, i.e., $\varepsilon_4 = 2\varepsilon_{23}, \varepsilon_5 = 2\varepsilon_{31}, \varepsilon_6 = 2\varepsilon_{12}$, while the components of the elastic compliance tensor s_{ij} include the multiplying factors of 2 and 4 (Voigt 1928). Besides, the notation $\langle \rangle$ denotes a volume average and superscripts 1, 2 or m denote fields in materials 1, 2 or m , respectively.

2 Two-phase laminate

2.1 Averaging rules, constitutive behavior and continuity conditions

In this section, an infinite 2-phase laminate composed of two alternating materials or crystals perfectly bonded along planar interfaces is considered. The laminate is supposed to have been deformed under the action of a homogeneous temperature variation ΔT , as well as a macroscopic homogeneous and remotely applied stress Σ_i satisfying the averaging rule

$$\Sigma_i = \langle \sigma_i \rangle, \quad (1)$$

where σ_i is the local Cauchy stress. Linear thermo-elasticity is assumed, as well as the possibility of having different uniform plastic strains ε_i^p in each phase. Under these assumptions and through consideration of strains compatibility and balance of linear momentum without body force in a static small strain setting, it can be shown that σ_i and the local strains ε_i are uniform in each material (Gemperlova, Paidar, and Kroupa 1989; Omri et al. 2000; Milton 2004; Richeton and Berbenni 2013), so that

$$\Sigma_i = f^1 \sigma_i^1 + f^2 \sigma_i^2, \quad (2)$$

$$E_i = \langle \varepsilon_i \rangle = f^1 \varepsilon_i^1 + f^2 \varepsilon_i^2, \quad (3)$$

where f^1 and $f^2 = 1 - f^1$ are the material volume fractions. E_i is defined as the macroscopic strain. The strains in each material are expressed as

$$\varepsilon_i^m = s_{ij}^m \sigma_j^l + \varepsilon_i^{p^m} + \alpha_i^{T^m} \Delta T, \quad (4)$$

where $m = 1, 2$ while s_{ij} and α_i^T are the components of the elastic compliance tensor and of the symmetrical thermal expansion tensor, respectively. Both s_{ij} and α_i^T are supposed isotherm and uniform in each material. Besides, considering that the interface normals are parallel to the direction \mathbf{e}_2 of a Cartesian frame $(\mathbf{e}_1, \mathbf{e}_2, \mathbf{e}_3)$, Equations (2) and (3) along with tractions continuity and strains compatibility impose the following continuity conditions (Omri et al. 2000; Richeton and Berbenni 2013; Glüge and Kalisch 2014)

$$\begin{aligned} \sigma_2^1 &= \sigma_2^2 = \Sigma_2, \\ \sigma_4^1 &= \sigma_4^2 = \Sigma_4, \\ \sigma_6^1 &= \sigma_6^2 = \Sigma_6, \end{aligned} \quad (5)$$

$$\begin{aligned}
\varepsilon_1^1 &= \varepsilon_1^2 = E_1, \\
\varepsilon_3^1 &= \varepsilon_3^2 = E_3, \\
\varepsilon_5^1 &= \varepsilon_5^2 = E_5.
\end{aligned} \tag{6}$$

2.2 Stress partitioning

It was previously shown that the stresses in both materials can be written as (Richeton and Berbenni 2013; Richeton, Tiba, et al. 2015; Tiba et al. 2015; Richeton 2017)

$$\begin{aligned}
\sigma_i^1 &= \Sigma_i + f^2 G_{ik} \left[\left(s_{kj}^2 - s_{kj}^1 \right) \Sigma_j + \varepsilon_k^{p2} - \varepsilon_k^{p1} + \left(\alpha_k^{T2} - \alpha_k^{T1} \right) \Delta T \right], \\
\sigma_i^2 &= \Sigma_i - f^1 G_{ik} \left[\left(s_{kj}^2 - s_{kj}^1 \right) \Sigma_j + \varepsilon_k^{p2} - \varepsilon_k^{p1} + \left(\alpha_k^{T2} - \alpha_k^{T1} \right) \Delta T \right],
\end{aligned} \tag{7}$$

where the non-zero components of the symmetric tensor G_{ij} are given by

$$\begin{aligned}
G_{11} &= (\tilde{s}_{35}^2 - \tilde{s}_{33}\tilde{s}_{55}) / D, & G_{13} &= (\tilde{s}_{13}\tilde{s}_{55} - \tilde{s}_{15}\tilde{s}_{35}) / D, \\
G_{33} &= (\tilde{s}_{15}^2 - \tilde{s}_{11}\tilde{s}_{55}) / D, & G_{15} &= (\tilde{s}_{15}\tilde{s}_{33} - \tilde{s}_{13}\tilde{s}_{35}) / D, \\
G_{55} &= (\tilde{s}_{13}^2 - \tilde{s}_{11}\tilde{s}_{33}) / D, & G_{35} &= (\tilde{s}_{11}\tilde{s}_{35} - \tilde{s}_{13}\tilde{s}_{15}) / D,
\end{aligned} \tag{8}$$

$$\text{with } D = \tilde{s}_{11}\tilde{s}_{35}^2 + \tilde{s}_{33}\tilde{s}_{15}^2 + \tilde{s}_{55}\tilde{s}_{13}^2 - \tilde{s}_{11}\tilde{s}_{33}\tilde{s}_{55} - 2\tilde{s}_{13}\tilde{s}_{15}\tilde{s}_{35} \text{ and } \tilde{s}_{ij} = f^2 s_{ij}^1 + f^1 s_{ij}^2.$$

It is noteworthy that these expressions can also be viewed as the solutions in an infinite bi-material (Gemperlova, Paidar, and Kroupa 1989; Richeton and Berbenni 2013; Richeton, Tiba, et al. 2015) and that the effect grain boundary sliding could be considered as well (Richeton 2017). The differences $\sigma_i^1 - \Sigma_i$ and $\sigma_i^2 - \Sigma_i$ are called incompatibility stresses in materials 1 and 2, respectively. Besides, it must be noted that

the tensor G_{ij} is a function of s_{ij}^1 , s_{ij}^2 and f^1 (or f^2) only so that we will use the notation $G_{ij}(\overbrace{s^1, s^2, f^1})$ in Section 3 where multiple tensors G_{ij} are considered.

2.3 Effective behavior

By definition of the effective behavior, the macroscopic strain satisfies the following relation

$$E_i = S_{ij} \Sigma_j + E_i^p + A_i^T \Delta T, \tag{9}$$

where S_{ij} is the effective compliance tensor, E_i^p the effective plastic strain tensor and A_i^T the effective thermal expansion tensor. From Equations (3), (4) and (7), we have also

$$\begin{aligned}
E_i &= \langle s_{ij} \sigma_j \rangle + \langle \varepsilon_i^p \rangle + \langle \alpha_i^T \rangle \Delta T \\
&= \langle s_{ij} \rangle \Sigma_j - f^1 f^2 \left(s_{ij}^2 - s_{ij}^1 \right) G_{jk} \left(\left(s_{kl}^2 - s_{kl}^1 \right) \Sigma_l + \varepsilon_k^{p2} - \varepsilon_k^{p1} + \left(\alpha_k^{T2} - \alpha_k^{T1} \right) \Delta T \right) + \langle \varepsilon_i^p \rangle + \langle \alpha_i^T \rangle \Delta T.
\end{aligned} \tag{10}$$

Then, by identification with Equation (9), the three effective tensors can be expressed as

$$S_{ij} = \langle s_{ij} \rangle - f^1 f^2 \left(s_{ik}^2 - s_{ik}^1 \right) G_{kl} \left(s_{lj}^2 - s_{lj}^1 \right), \tag{11}$$

$$E_i^p = \langle \varepsilon_i^p \rangle - f^1 f^2 \left(s_{ik}^2 - s_{ik}^1 \right) G_{kl} \left(\varepsilon_l^{p2} - \varepsilon_l^{p1} \right), \tag{12}$$

$$A_i^T = \langle \alpha_i^T \rangle - f^1 f^2 \left(s_{ik}^2 - s_{ik}^1 \right) G_{kl} \left(\alpha_l^{T2} - \alpha_l^{T1} \right). \tag{13}$$

3 N-phase laminate

In the following, an infinite N-phase laminate composed of N alternating materials perfectly bonded along parallel planar interfaces is considered. All the other assumptions made for the 2-phase laminate in Section 2, including Equation (1) and Equation (4), are maintained so that stresses and strains are uniform within each phase (Glüge and Kalisch 2014).

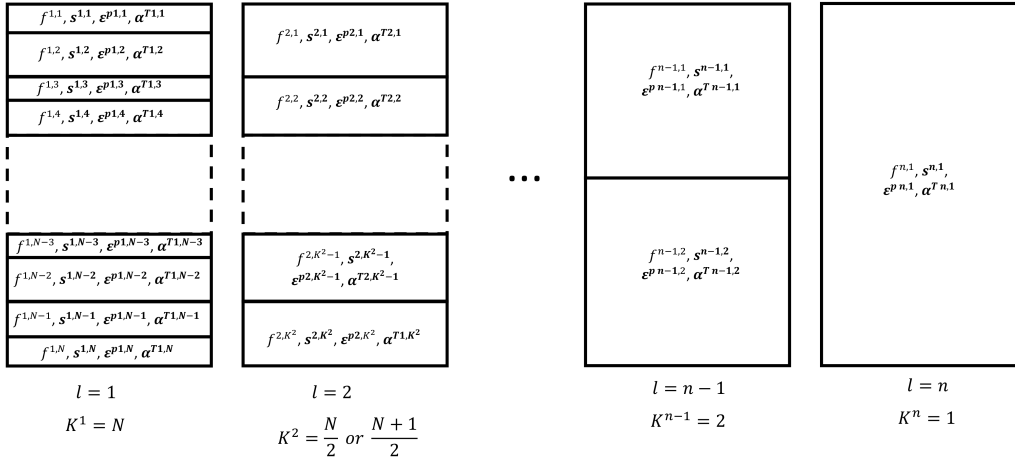


Figure 1: Description of the multiple steps procedure for determining the effective behavior of a N-phase laminate and the stresses in each phase. l denotes the step number and K^l the number of phases at step l .

3.1 Effective behavior

The solutions of the 2-phase laminate are first used to find the effective behavior of the N-phase laminate and then the stresses in each phase. The general methodology is described in [Figure 1](#). It consists in a multiple steps procedure, where at each step l , the phases are grouped two by two and replaced by a new phase having the effective properties of a 2-phase laminate made of the corresponding two phases. The procedure is followed until only one phase is remaining, corresponding to $l = n = \lceil \frac{\log N}{\log 2} + 1 \rceil$. The number of phases or materials of the laminate at a step l is denoted K^l . Hence, we have $K^1 = N$, $K^n = 1$ and

$$K^{l+1} = \begin{cases} \frac{1}{2}K^l & \text{if } K^l \text{ is even,} \\ \frac{1}{2}(K^l + 1) & \text{if } K^l \text{ is odd.} \end{cases} \quad (14)$$

At step $l+1$, the properties of a new phase m , volume fractions $f^{l+1,m}$, elastic compliances $s_{ij}^{l+1,m}$, plastic strains $\epsilon_i^{p^{l+1,m}}$ and thermal expansions $\alpha_i^{T^{l+1,m}}$, are computed based on [Equations \(11\)](#) to [\(13\)](#)

$$\begin{aligned} f^{l+1,m} &= f^{l,2m-1} + f^{l,2m}, \\ s_{ij}^{l+1,m} &= f^* s_{ij}^{l,2m-1} + (1-f^*) s_{ij}^{l,2m} - f^* (1-f^*) \left(s_{ik}^{l,2m} - s_{ik}^{l,2m-1} \right) G_{kl} \overbrace{(s^{2m-1}, s^{2m}, f^*)} \left(s_{ij}^{l,2m} - s_{ij}^{l,2m-1} \right), \\ \epsilon_i^{p^{l+1,m}} &= f^* \epsilon_i^{p^{l,2m-1}} + (1-f^*) \epsilon_i^{p^{l,2m}} - f^* (1-f^*) \left(s_{ik}^{l,2m} - s_{ik}^{l,2m-1} \right) G_{kl} \overbrace{(s^{2m-1}, s^{2m}, f^*)} \left(\epsilon_i^{p^{l,2m}} - \epsilon_i^{p^{l,2m-1}} \right), \\ \alpha_i^{T^{l+1,m}} &= f^* \alpha_i^{T^{l,2m-1}} + (1-f^*) \alpha_i^{T^{l,2m}} - f^* (1-f^*) \left(s_{ik}^{l,2m} - s_{ik}^{l,2m-1} \right) G_{kl} \overbrace{(s^{2m-1}, s^{2m}, f^*)} \left(\alpha_i^{T^{l,2m}} - \alpha_i^{T^{l,2m-1}} \right), \end{aligned}$$

with $f^* = \frac{f^{l,2m-1}}{f^{l,2m-1} + f^{l,2m}}$.

Imposing the following consistency condition between stresses at step l and at step $l+1$

$$f^* \sigma_i^{l,2m-1} + (1-f^*) \sigma_i^{l,2m} = \sigma_i^{l+1,m}, \quad (16)$$

the stresses at step l can then be related to the stresses at step $l+1$ by application of [Equation \(7\)](#)

$$\begin{aligned} \sigma_i^{l,2m-1} &= \sigma_i^{l+1,m} + (1-f^*) G_{ik} \overbrace{(s^{2m-1}, s^{2m}, f^*)} \left[\left(s_{kj}^{l,2m} - s_{kj}^{l,2m-1} \right) \sigma_j^{l+1,m} + \epsilon_k^{p^{l,2m}} - \epsilon_k^{p^{l,2m-1}} + \left(\alpha_k^{T^{l,2m}} - \alpha_k^{T^{l,2m-1}} \right) \Delta T \right], \\ \sigma_i^{l,2m} &= \sigma_i^{l+1,m} - f^* G_{ik} \overbrace{(s^{2m-1}, s^{2m}, f^*)} \left[\left(s_{kj}^{l,2m} - s_{kj}^{l,2m-1} \right) \sigma_j^{l+1,m} + \epsilon_k^{p^{l,2m}} - \epsilon_k^{p^{l,2m-1}} + \left(\alpha_k^{T^{l,2m}} - \alpha_k^{T^{l,2m-1}} \right) \Delta T \right]. \end{aligned}$$

(17)

In case where K^l is odd, the last phase of step $l + 1$ is simply set to be the same as the last phase of step l , i.e. $f^{l+1, K^{l+1}} = f^{l, K^l}$, $s_{ij}^{l+1, K^{l+1}} = s_{ij}^{l, K^l}$, $\varepsilon_i^{p^{l+1, K^{l+1}}} = \varepsilon_i^{p^{l, K^l}}$, $\alpha_i^{T^{l+1, K^{l+1}}} = \alpha_i^{T^{l, K^l}}$ and $\sigma_i^{l+1, K^{l+1}} = \sigma_i^{l, K^l}$.

From the procedure just described, it is noticeable that averaging rules and continuity conditions are automatically satisfied at each step, i.e.

$$\forall l, \sum_m f^{l, m} = 1 \quad (18)$$

$$\forall l, \langle \sigma_i^l \rangle = \Sigma_i \quad (19)$$

$$\forall l, \langle \varepsilon_i^l \rangle = E_i \quad (20)$$

$$\begin{aligned} \forall l, \forall m, \sigma_2^{l, m} &= \Sigma_2, \\ \forall l, \forall m, \sigma_4^{l, m} &= \Sigma_4, \\ \forall l, \forall m, \sigma_6^{l, m} &= \Sigma_6, \end{aligned} \quad (21)$$

$$\begin{aligned} \forall l, \forall m, \varepsilon_1^{l, m} &= E_1, \\ \forall l, \forall m, \varepsilon_3^{l, m} &= E_3, \\ \forall l, \forall m, \varepsilon_5^{l, m} &= E_5. \end{aligned} \quad (22)$$

As a consequence, the remaining phase at step n satisfies the relation

$$E_i = s_{ij}^{n, 1} \Sigma_j + \varepsilon_i^{p^{n, 1}} + \alpha_i^{T^{n, 1}} \Delta T, \quad (23)$$

which means from Equation (9) that the effective tensors of the N-phase laminate are given by

$$\begin{aligned} S_{ij} &= s_{ij}^{n, 1}, \\ E_i^p &= \varepsilon_i^{p^{n, 1}}, \\ A_i^T &= \alpha_i^{T^{n, 1}}. \end{aligned} \quad (24)$$

3.2 Stress partitioning

The stresses in each phase of the initial N-phase laminate can be determined from a top-down algorithm, starting from $\sigma_i^{n, 1} = \Sigma_i$ at step n and then going down to step 1 by application of Equation (17) step by step. However, a direct and much faster computation is possible by first noticing that for a 2-phase laminate

$$\begin{aligned} \Sigma_i + (1 - f^1) G_{ik} \overbrace{(s^1, s^2, f^1)} &\left[(s_{kj}^2 - s_{kj}^1) \Sigma_j + \varepsilon_k^{p^2} - \varepsilon_k^{p^1} + (\alpha_k^{T^2} - \alpha_k^{T^1}) \Delta T \right] \\ &= \Sigma_i + G_{ik} \overbrace{(s^1, S, 0)} \left[(S_{kj} - s_{kj}^1) \Sigma_j + E_k^p - \varepsilon_k^{p^1} + (A_k^T - \alpha_k^{T^1}) \Delta T \right], \end{aligned} \quad (25)$$

where S_{ij} , E_i^p , A_i^T are, in this equation, the effective properties of the 2-phase laminate. Indeed, let us consider a fictitious cut of phase 1 parallel to the planar interface, such that one of two remaining parts, denoted hereafter 1^* , has a negligible volume fraction ($f^{1^*} \rightarrow 0$). Stresses in phase 1^* are equal to those in the first phase due to uniformity of stresses within a phase (see Section 2). This corresponds to the first member of Equation (25) (cf. Equation (7)). Then, as a consequence of the preceding procedure, it is known that these stresses can also be computed from a 2-phase laminate composed of phase 1^* and another phase of volume fraction equals to 1 and thus having necessarily the effective properties of the initial 2-phase laminate. This corresponds to the second member of Equation (25). The principle is illustrated in Figure 2.

Such a thought experiment can of course also be made for the N-phase laminate, i.e. performing a fictitious cut in a given phase m such that one of the remaining part has a negligible volume fraction and

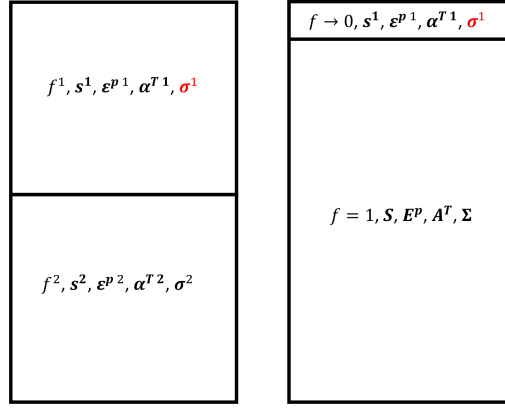


Figure 2: Description of the two configurations used to establish Equation (25).

then considering a 2-phase laminate made of the phase m^* with negligible volume fraction and another phase having the effective properties of the N-phase laminate. Accordingly, once the effective properties (S_{ij}, E_i^p, A_i^T) of the the N-phase laminate are known, the stresses in each phase can be directly computed as

$$\sigma_i^m = \Sigma_i + G_{ik} \overbrace{(s^m, S, 0)} \left[(S_{kj} - s_{kj}^m) \Sigma_j + E_k^p - \epsilon_k^{p^m} + (A_k^T - \alpha_k^{T^m}) \Delta T \right]. \quad (26)$$

From the above procedure, it is obvious that the relative positions of the different phases has no influence on the effective behavior and the stresses in each phase, as it was already noted by Glüge and Kalisch (2014). Besides, the numerical implementation of the proposed method is very straightforward and is provided as a Matlab code at <https://github.com/AniPlas/Laminate>. It contains two functions, one that computes the tensor G_{ij} (cf. Equation (8)) from two elastic compliance tensors and a volume fraction value and one that computes the effective properties of a 2-phase laminate according to Equation (15). The main code is just composed of a single iterative loop that calls the two aforementioned functions in order to compute the properties of the new phases step by step as described in Figure 1. At step n , the effective behavior of the N-phase laminate is known (cf. Equation (24)) and the stresses in any phase can then be directly computed from Equation (26).

Finally, it may be worth to indicate also the simple expressions of the stresses in the particular case of isotropic homogenous thermo-elasticity. With μ the shear modulus and ν the Poisson's ratio, the plane components are indeed given by (Rey et al. 1980; Richeton and Berbenni 2013)

$$\begin{aligned} \sigma_1^m &= \Sigma_1 + \frac{2\mu}{1-\nu} \left[\langle \epsilon_1^p \rangle - \epsilon_1^{p^m} + \nu \left(\langle \epsilon_3^p \rangle - \epsilon_3^{p^m} \right) \right], \\ \sigma_3^m &= \Sigma_3 + \frac{2\mu}{1-\nu} \left[\langle \epsilon_3^p \rangle - \epsilon_3^{p^m} + \nu \left(\langle \epsilon_1^p \rangle - \epsilon_1^{p^m} \right) \right], \\ \sigma_5^m &= \Sigma_5 + \mu \left[\langle \epsilon_5^p \rangle - \epsilon_5^{p^m} \right], \end{aligned} \quad (27)$$

while the anti-plane components are given by Equation (21).

4 Validation by finite element simulations

Several Finite Element (FE) simulations were performed in order to check the correctness of the methodology described in Section 3. The present Section 4 describes the particular case of an infinite 5-phase laminate with interface normals parallel to the direction \mathbf{e}_2 and volume fractions $f^1 = 0.25$, $f^2 = 0.1$, $f^3 = 0.3$, $f^4 = 0.2$ and $f^5 = 0.15$. Cubic elastic constants of Cu at room temperature were considered with $c_{11} = 170$ GPa, $c_{12} = 124$ GPa and $c_{44} = 75$ GPa for the 5 phases. Crystallographic orientations of the phases were randomly selected. For each phase, a slip of magnitude 0.02 was assigned to the most stressed system within a pure elastic loading, i.e. among the 12 FCC slip systems $\{111\}\langle 110 \rangle$, the system with the highest absolute value of its resolved shear stress $|\tau|$ was selected and affected $\gamma = \pm 0.02$ so that $\tau\gamma > 0$. No thermal strain was considered.

The FE simulation was performed thanks to the use of the open source software suite *freefem++* (Hecht 2012). A structured mesh made of 270,000 triangular quadratic Lagrangian elements was used for the meshing of a bar of length 100 (see Figure 3), which corresponds to 4 nodes along \mathbf{e}_1 and \mathbf{e}_3 and 5001 nodes along \mathbf{e}_2 . Periodic boundary conditions were imposed along the lateral sides. Arbitrary and fixed velocities

were imposed on the bottom and upper faces of the bar ($u_1 = -0.05, u_2 = 0, u_3 = -0.1$ on the bottom face, $u_1 = 0.05, u_2 = 0.1, u_3 = 0.1$ on the top face). Phase 1 was affected to the region $75 < x_2 < 100$, phase 2 to $65 < x_2 < 75$, phase 3 to $35 < x_2 < 65$, phase 4 to $15 < x_2 < 35$ and phase 5 to $0 < x_2 < 15$. A first simulation was performed in pure elasticity in order to determine the most stressed systems and then a second simulation was performed considering static plastic strains in each phase.

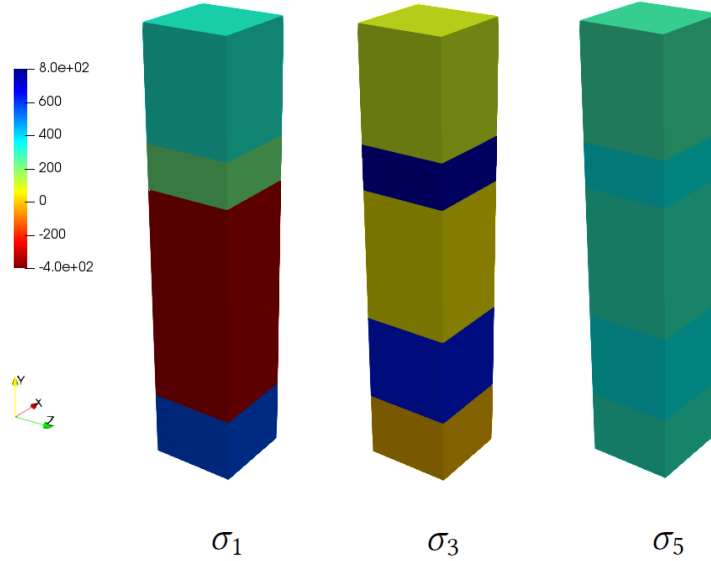


Figure 3: Distribution of σ_1 , σ_3 and σ_5 in MPa obtained by the FE simulation (see text).

The distribution of stresses obtained by the FE simulation is shown in **Figure 3**. For comparison, stresses were then computed from **Equations (15), (24) and (26)** considering the same elastic constants, the same crystallographic orientations of the phases, the same plastic strains and taking Σ_i as the average stress vector given by the FE simulation. In **Figure 4**, it can be seen that the matching between the stresses obtained numerically by a FE simulation and the ones computed analytically from **Equations (15), (24) and (26)** is excellent. The tiny differences are due to the fact that the FE method is an approximate numerical method. In particular, it was checked that the stress differences decrease with the number of nodes along e_2 . These results, along with the several other FE simulations performed, prove the correctness of the analytical formulas established in **Section 3**.

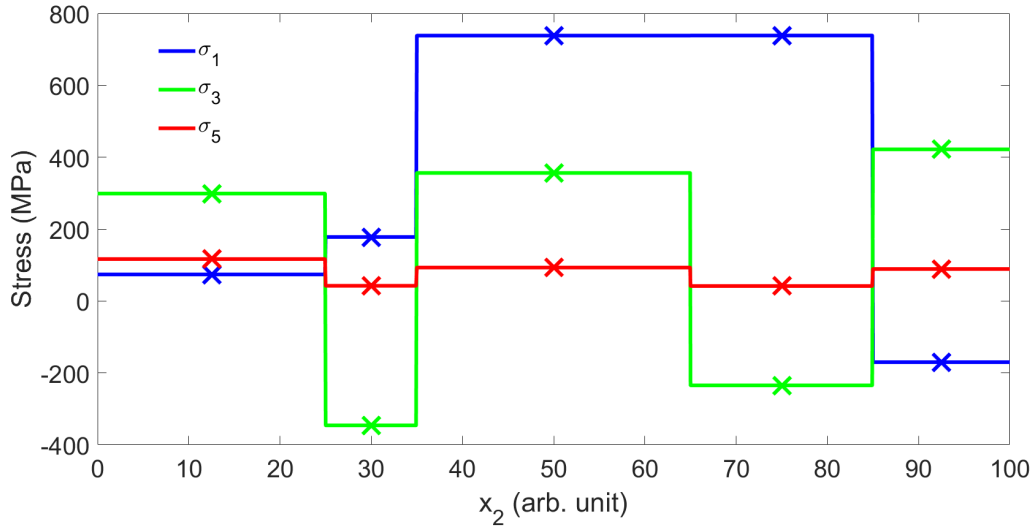


Figure 4: Comparisons of stresses obtained by the FE simulation (full lines) and the ones computed from **Equations (15), (24) and (26)** (crosses).

5 Application to polycrystals with elongated grains and discussion

As an illustration of potential applications, the N-phase laminate model can be used as a quick way to estimate the level of internal stresses in metals and alloys having very elongated grains due to rolling or

forging processes. For instance, near- β titanium alloys like Ti-1023 used in forged pieces of aircraft for landing gears or rotor systems can achieve high specific strength thanks to the building-up of complex α/β microstructures. However, these Ti-1023 alloys still contain very large domains of close β orientations which corresponds to the prior- β grains that were deformed during the forging steps above the β transus (Lhadi, Berbenni, et al. 2018; Lhadi, purohit, et al. 2020). These prior- β grains are millimeter size and are highly elongated along a same direction corresponding to the axial axis of the billet. Due to the significant elastic anisotropy of the cubic β phase (Purushottam-raj-purohit et al. 2021), strong mechanical contrasts exist between these millimeter size regions which might be at the origin of the early crack initiation sometimes observed in those materials (Lhadi, Berbenni, et al. 2018; Lhadi, purohit, et al. 2020). In order to get a very first insight into such a mechanical issue, it can thus be interesting to simplify the microstructure as an infinite laminate composed of different β orientations. By doing so, the focus is only on the mechanical interactions between prior- β grains and on a same kind of interfaces, i.e. the predominant long ones which are parallel to the axial axis of the billet.

Hence, a laminate composed of 100,000 different β orientations of equal volume fraction was considered as an application of the methodology described in Section 3. The orientations were generated from the open-source software package *Neper* (Quey et al. 2018) so that their distribution was nearly uniform in presence of cubic symmetry. It is noteworthy that the effect of a crystallographic texture could have considered as well without difficulty. Elastic constants of Ti-1023, $c_{11} = 92.6$ GPa, $c_{12} = 82.5$ GPa and $c_{44} = 43.5$ GPa, were taken from Purushottam-raj-purohit et al. (2021) who provided an estimation based on a method coupling Bayesian inference analysis, high energy X-ray diffraction and micromechanical modeling. These constant values correspond to a strong elastic anisotropy ratio of $A = 8.6$. The laminate interface normals were set parallel to the direction \mathbf{e}_2 of the Cartesian frame ($\mathbf{e}_1, \mathbf{e}_2, \mathbf{e}_3$). A uniaxial macroscopic stress of magnitude 300 MPa was applied without consideration of any plastic or thermal strains. The direction of the uniaxial stress was varied by rotation around \mathbf{e}_3 . This rotation was described by an angle θ , $\theta = 0^\circ$ corresponding to an uniaxial stress along \mathbf{e}_1 and $\theta = 90^\circ$ along \mathbf{e}_2 . The stresses in each grain were computed from Equations (15), (24) and (26) and then the maximum von Mises stress among the 100,000 grains was recorded and plotted with respect to θ , see Figure 5. For comparison, a 2-phase laminate made of grains of equal volume fraction was considered as well. The crystallographic orientations of the 2 grains were set so that the directions $[111]$ and $[\bar{1}\bar{1}0]$ in one grain and $[100]$ and $[010]$ in the other grain were parallel to \mathbf{e}_1 and \mathbf{e}_2 , respectively. $\langle 111 \rangle$ and $\langle 100 \rangle$ are actually the directions of maximum and minimum directional Young's modulus, respectively. Hence, the mechanical contrast should very strong for this 2-grains configuration.

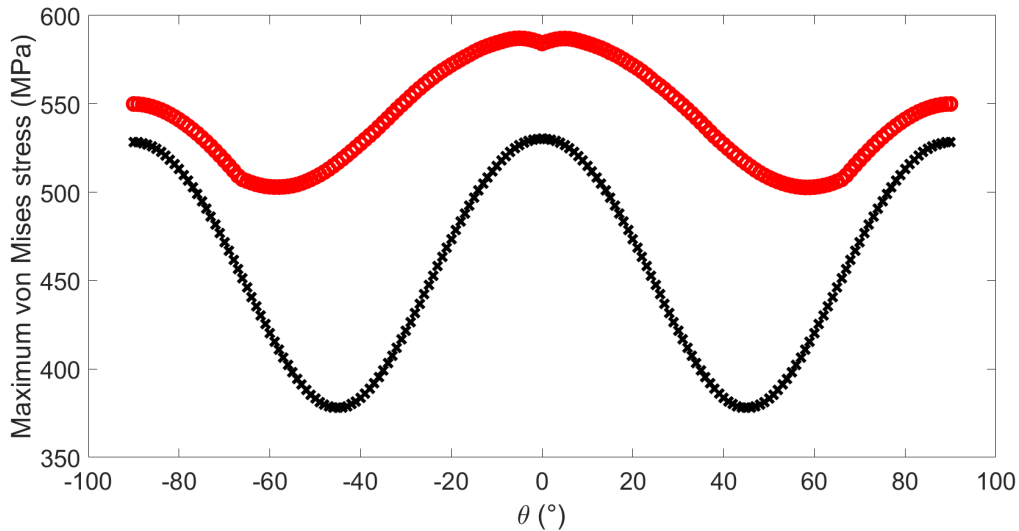


Figure 5: Comparisons of the maximum von Mises stress obtained between a laminate made of 100,000 uniform orientations (red circles) and a 2-phase laminate made of $[111] \parallel \mathbf{e}_1$ and $[100] \parallel \mathbf{e}_1$ -oriented grains (black crosses). θ represents a rotation around \mathbf{e}_3 of the uniaxial stress direction (see details in text). The elastic constants of Ti-1023 were used (Purushottam-raj-purohit et al. 2021).

In Figure 5, it is interesting to observe that the maximum von Mises stress of the 2-phase laminate is always below the one with 100,000 orientations despite the strong elastic contrast considered. However, it was shown in Section 3 that the stresses in a given phase of a N-phase laminate can also be deduced from a 2-phase laminate with the given phase in interaction with the whole laminate (see Figure 2 and Equation (26)). Accordingly, the elastic contrast between the phase where the maximum von Mises stress is located and the effective laminate of 100,000 grains should normally not exceed the elastic

contrast that exists between $[111] \parallel \mathbf{e}_1$ and $[100] \parallel \mathbf{e}_1$ -oriented grains. The difference of maximum von Mises stress is actually rather due to a volume fraction effect, incompatibility stresses in a phase of volume fraction f scaling indeed as $1 - f$, see Equation (7). Yet, the grain volume fraction is $f = 0.5$ in the 2-phase laminate whereas it is negligible ($f = 10^{-5}$) in the laminate with 100,000 orientations. Similar results could also be obtained by considering strong mechanical contrasts resulting from difference of plastic or thermal strains. As a consequence, it can be inferred that considering a 2-phase laminate of equal volume fraction with extremal mechanical contrast leads generally to underestimate the maximal level of internal stresses in polycrystals. Furthermore, the difference of positions of the local extrema between the 2-phase laminate and the one with 100,000 orientations can also be noticed in Figure 5.

Besides, it is noteworthy that such an internal stress evaluation could also have been made from an elastic self-consistent model (Hershey 1954; Kröner 1958; Hill 1965) with grains more realistic modeled as ellipsoidal inclusions. However, it is reminded that in that in self-consistent models, implicit equations need to be solved (Walpole 1969) which can cause convergence issues for strong mechanical contrasts or very big number of phases whereas the methodology of Section 3 is fully explicit and thus much more robust and much faster. In addition, self-consistent models are based on the use of Eshelby tensors (Eshelby 1957) whose determination in anisotropic elasticity involves numerical integration methods, which are not so simple to code. On the contrary, the expression of the tensor G_{ij} (Equation (8)) is fully analytical and very simple to code. Actually, it can be shown that the tensor G_{ij} can be seen as fundamental solutions from which the Eshelby stress solutions can be obtained from appropriate integrations (Richeton and Berbenni 2014). Another difference is that the methodology of Section 3 is as accurate for a very small or a very large number of phases whereas self-consistent models only work well for a very big number of phases. For instance, a self-consistent model cannot consider the direct interaction between two grains as done in Figure 5 as it would actually consider the mechanical interaction between one grain and the homogeneous equivalent medium made of the two grains.

6 Conclusion

This work provides a fast explicit method to compute the effective behavior and the distribution of stresses in a general N-phase laminate made of parallel, planar and perfectly bonded interfaces. The formulas are based on the solutions of the two-phase laminate. They are very compact which is convenient for numerical implementation and direct use in engineering applications. In contrast with previous approaches, this contribution considers, at the same time, an arbitrary number of phases, a completely general anisotropic elasticity as well as different thermal and plastic strains in the phases. In addition, a simple direct analytical formula is derived in order to compute the stress in a given phase once the effective behavior of the laminate has been determined. Finally, the code to compute the effective behavior and the stress distribution in an arbitrary N-phase laminate is made freely available for download at <https://github.com/AniPlas/Laminate>.

Besides, the correctness of the proposed method is checked by comparisons with finite element simulation results on a same boundary value problem, showing excellent agreements. An application of the method is also performed for a near- β titanium alloy with elongated grains, by comparing for different elastic loadings the level of internal stresses within a N-phase laminate made of 100,000 orientations and a 2-phase laminate with maximal elastic contrast. Interestingly, the maximum von Mises stress of the 2-phase laminate is always the lowest despite the strong elastic contrast considered, which is explained by a volume fraction effect. Similar results could be obtained by considering strong mechanical contrasts resulting from difference of plastic or thermal strains. Accordingly, it can be inferred that considering a 2-phase laminate of equal volume fraction with extremal mechanical contrasts leads generally to underestimate the maximal level of internal stresses in polycrystals.

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Funding This work was supported by the French State through the program "Investment in the future" operated by the National Research Agency (ANR) and referenced by ANR-11-LABX-0008-01 (LabEx DAMAS).

Competing interests The author declares that he has no competing interests.

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