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INTRINSIC STRUCTURAL-BALLISTIC INTERACTIONS IN COMPOSITE ENERGETIC MATERIALS

PART II - MODELING

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ABSTRACT

The structural properties of a propellant and the mechanical loads acting on it during deflagration can significantly alter its combustion behavior on a microscopic scale, leading to what can be considered as an "intrinsic" coupling mechanism. This can affect the performance of a solid rocket motor, and has been the subject of a study performed at the SPLab (Space Propulsion Laboratory) of Politecnico di Milano on AP-HTPB based composite propellant formulations. Part II of this study builds up from the results and experiments presented in Part I. Modeling activities were performed to understand and explain the coupling effect observed in the experiments and extrapolate them to motor's conditions. A correlation between the average state of damage of the propellant and the burning rate is suggested.

Nomenclature

а	thermal diffusivity or crack length
a _T	Time-temperature superposition factor
c_p/c_v	Constant pressure/constant volume specific heat
E	Stiffness
E(t)	relaxation modulus in tension
G	Cross flow in the bore of a solid rocket motor
g	Strain softening function - Swanson & Christensen's NLVE model
g(ε)	Strain softening function – Swanson & Christensen's NLVE model
H/C	Hydrocarbon

k	thermal conductivity
LVE	Linear viscoelastic material model
IM	Insensitive Munition
NLVE	Non-linear viscoelastic material model
q	thermal energy flux
<i>r</i> _b	burning rate
S_p	Shapery's damage parameter
Т	Temperature
t	Time
T_f	Final flame temperature
Ts	surface temperature
T_{0}	soak temperature
\mathcal{E}'	strain rate
ε	Strain
λ	Thermal conductivity
ρ	Density
σ	Stress

Introduction

"Intrinsic" structural-ballistic interactions are those influencing the speed of deflagration of the propellant itself. Their effect is on a microscopic scale, without any occurrence of structural collapse of the grain by crack generation and propagation or excessive deformation. Their triggering cause is mechanical damage: the experiments illustrated in part I showed that the root cause for burning rate augmentation is the presence of porosity, kept open by a tensile stress/strain field and generated by mechanical damage on a microscopic scale, i.e. adhesive fracture between the solid particles and the binder or cohesive fracture in the binder itself. This mechanism, known as dewetting, alters therefore the apparent burning rate of the material, increasing it, as it was suggested by Summerfield and Parker in [1]. Part II of this study describes some simple modelling activities performed to help understanding the physical phenomena involved in burning rate augmentation. A correlation between the amount mechanical damage and burning rate augmentation is also suggested. This information is useful to set a fundamental material property such as the burning rate for damage portions of the grain within the scope of rocket motor simulations [2].

Preliminary comments on the burning rate experiments

Following results were obtained for damaged material samples of an AP-HTPB based propellant under load [3]:

Focusing on the burning rate measurements of material subjected to dewetting (1.47 mm/s vs. 1.15 mm/s), a few simple calculations give us more insight into the phenomena involved in the combustion of the propellant and explain the large burning rate dispersion measured as well as the maximum recorded values.



Fig. 1 *Left:* Evolution of one of the larger oxidizer particles in an undamaged propellant (Taken from [4]). *Right:* the evolution of the same particle in a propellant with dewetting.

Let us consider the evolution of one of the larger oxidizer particles when it is reached by the burning surface in an <u>undamaged</u> portion of propellant (Fig. 1, left). Assuming that the thermal energy feedback from the flame is constant and the rate of AP decomposition (r_b^{AP}) is also constant, then the mass flow generated by the particle is proportional to the instantaneous surface exposed to the flame (the top surface of a truncated sphere) and amounts to:

$$\dot{m} = \rho_{AP} r_b^{AP} A_b(t) = \rho_{AP} r_b^{AP} \pi \Big[r_{AP}^2 - (r_{AP} - h)^2 \Big]$$
(1)

h is the consumed segment, r_{AP} the particle radius, assometed by the particle until complete consumption is therefore:

$$\overline{\dot{m}} = \frac{2}{3} \rho_{AP} r_b^{AP} \pi r_{AP}^2$$
⁽²⁾

Let's now suppose that the particle belongs to a damaged portion of propellant, and is <u>completely</u> detached from the binder (full dewetting: Fig. 1, right). The initial burning surface is equal to the surface of the sphere, and the mass flow amounts to:

$$\dot{m} = \rho_{AP} r_b^{AP} A_b(t) = \rho_{AP} r_b^{AP} 4\pi r_{AP}^2 = \pi D^2 \rho_{AP} r_b^{AP}$$
(3)

The average mass flow generated by the dewetted particle until complete consumption is then:

$$\overline{\dot{m}} = \frac{4}{3} \rho_{AP} r_b^{AP} \pi r_{AP}^2$$
(4)

since the size of the two particles is the same, the average burning rate of the material having complete dewetting [3] is predicted to be about twice the rate of the undamaged material.

 $\mathbf{r}_{b, undamaged} = 1.15 \text{ mm/s}; \mathbf{r}_{b, full dewetting} = 2.3 \text{ mm/s}. (cf. Table 2, third line)$ (5) The estimate above would hold for pure AP, but: - In any propellant formulation, the HTPB-IPDI binder system and the metal fuel are much less than the amount of AP (generally at least 70% in mass), and the fuel sublimation is controlled by the thermal power feedback from the flame.

- The flame itself is controlled by the primary AP flame at low to intermediate pressures [5], and even at higher pressures, and the diffusion flame would be driven by the amount of available oxygen and other oxidising species released by AP decomposition.

As a matter of fact, the predicted value of $r_{b, full dewetting} = 2 r_{b, undamaged} = 2.3 mm/s$ fits rather well with the <u>maximum</u> observed values in propellant stretched to dewetting (2.32 mm/s). Higher burning rates were found only in association with damage pattern 2 and 3 [3], i.e. observable cracks.

The reason why the average burning rate of the damaged material is smaller than twice the rate of the undamaged material (5) might depend on the following:

– An implicit assumption contained in the estimate is that ignition occurs instantaneously around a completely detached particle. Even in propellant portions stretched to severe dewetting, not all particles are completely disconnected from the binder ([3] Fig. 2). Most will be partially bonded to the binder, or wetted by binder and plasticizer residuals. Therefore, not 100% of the AP particle will ignite immediately: latent times for cracking and sublimation of the H/C might be longer than the time needed to normally consume the AP particle [7]

– Another assumption made is that just one particle is detached from the binder. If several successive neighboring particles are detached in depth below the burning surface, then the burning rate increases as: $\mathbf{r}_{b} = \mathbf{k} \ \mathbf{2} \ \mathbf{n}_{dw}$ with \mathbf{n}_{dw} equal to the number of dewetted particles ignited at the same time as the first one and k<1 a proportionality constant taking into account the actual free, readily ignitable surface with respect to the total surface of the particle.

This two features generate a lot of scatter in the burning rate measurements, and the average burning rate is expected to be between the undamaged value and the one corresponding with full dewetting. In absence of convection, the average burning rate for a 2D sample under load in a layer with dewetting should be inversely proportional to pressure:

$r_{b, damaged} \sim 2(k/p)r_{b, undamaged}$

(6)

since the thermal diffusivity is inversely proportional to p: $a \sim 1/p$. This effect would change the overall pressure sensitivity of a propellant tested without convection by accelerating the burning rate at lower pressure. This holds in absence of mechanisms such as those described in [8]. On the other hand, <u>at motor conditions</u>, thermal power transmission will occur through convection and radiation, so that the dewetted particle will ignite rapidly at the free surface at any pressure.

Let's now consider a detached particle near another one which is completely or almost completely bonded to the binder (Fig. 1), as it is the case where a zone with diffused dewetting lays beside another one which is mostly undamaged. Looking at the mass flow generated by such a pair, we observe that the first particle generates a much higher flow (see Fig. 2). This mass flow difference generates vorticity. Vorticity generation produced at a microscopic scale is therefore considerably higher than for an undamaged propellant. In an undamaged propellant [4], the maximum difference in normalized mass flow generated by two adjacent particles is equal to π ; in a damaged propellant it is 4 times higher. In the case of larger cracks, the production of vorticity is visible in the test video recordings ([3] Fig. 7). Neighboring portions of materials with different damage levels burn at different rates and the burning surface looses planarity. Larger vortexes are generated at the surface, with an increase in combustion noise.



Fig. 2 Normalized mass flow generated by a detached (blue) and an undetached, large AP particle (black). Assuming a constant AP decomposition rate, the abscissa can be expressed in time without changing the shape of the curve: the burning time for an undetached particle is twice the time needed by a completely detached particle.

Modeling

Modeling activities on the 2D burning rate experiments involved structural analysis and transient thermal analysis.

Structural Analysis of unflawed and flawed samples

To investigate the stress, strain and damage distribution for the 2D burning rate samples under load with and without the presence of small cracks, a qualitative structural analysis was performed using Merlin, a FE program developed at the university of Colorado in Boulder for fracture mechanics problems [9].

The sample was modeled using a 2D geometry and plane stress quadratic elements with a Poisson's ratio of 0.495. The propellant was considered to be linearly elastic with a reference modulus chosen following Shapery's principle of correspondence [10,11]:

$$E_R = \frac{1}{\Delta t^*} \int_{0}^{\Delta t^*} E(t^* - \tau) d\tau$$
⁽⁷⁾

with Δt^* the time needed to reach the final strain ε_{max} , applied at constant rate for this particular case; t* is a reduced time which takes the temperature into account. Results for the unflawed sample show a non-uniform distribution of strain and stress (Fig. 3, left). This generates a non-uniform amount of damage in the material. What happens when dewetting evolves into a small local crack? Results for a model reproducing the 2D sample shown above (Figs. 3-4) indicate that a crack produces a high perturbation of the



Fig. 3 Left: FEA of the undamaged, loaded sample shown in Fig.7, ref. [3]: strain (ϵ_{xx}) distribution (magnified displacements). **Right:** FEA of the damaged, loaded sample shown in Fig. 7, ref. [3]: strain (ϵ_{xx}) distribution (magnified displacements). Notice the effect of the crack: it takes up the applied displacement and reduces the strain at the corresponding specimen ordinate (red zone), but magnifies the load at the tips (blue, butterfly shape)

strain, stress and displacement field. Locally, much of the stress is not transmitted, but at the stress concentration spots in the process zone region at the tips the material is more loaded than without a crack. Cracks do not necessarily appear at the spots with the highest strain level, but rather where the local load overcomes the local material capability: mixing and casting effects or the very nature of the materials used in a propellant cause concentration gradients of constituents, like a local accumulation of oxidizer or metal fuel particles in the form of agglomerates of complex shapes, or a local enrichment of one phase with respect to the others, such as near the bore or the thermal protection, where the mere presence of a boundary produces an enrichment in terms of binder, fine oxidizer and fine metal fuel particles [22]. This heterogeneity varies from formulation to formulation. It has a different influence when new ingredients, like metal nanopowders, partially replace older constituents, and when particles for which no really effective bonding agent exists are used (like metal fuel, some explosive crystals, etc.). The consequence of this are high failure properties gradients if the scale of observation is small enough.

Mechanical damage

Since mechanical damage and tensile load are both necessary to alter the burning rate, and both are not uniform and not perfectly reproducible, the burning rate on a sample, being the fingerprint of damage <u>and</u> load, will be distributed in different ways on different specimens. This does not fit with the idea of having a function correlating the local load (applied displacement/strain) and damage with the local burning rate, like in [1], where studies are quoted in which it is suggested that for a particular level of applied strain, the burning rate increases by a precise factor.

A practical way to proceed in order to obtain a function correlating burning rate enhancement with mechanical damage for the purpose of ballistic simulations could be calculating a surface average of the burning rate for the damaged sample and assign it to the material layers for which structural analysis predicts a similar <u>average</u> state of damage, i.e. obtain a function of this form:

 $\mathbf{r}_{b, damaged} = \mathbf{f} (\mathbf{r}_{b \text{ undamaged}}, \mathbf{damage parameter})$ at constant pressure (8) Tensile load is a pre-requisite for burning rate augmentation effects to occur. Bearing this idea in mind, we considered the non-linear, viscoelastic constitutive model of Swanson and Christensen [12] and chose the scalar strain softening function $\mathbf{g}(\mathbf{\epsilon})$ to be a good parameter to express the average state of damage for a continuum element of the material. After this model, the convolution integral of linear viscoelasticity [10,11,13] is corrected as follows for a one-dimensional state of stress:

$$\sigma(t) = g(\varepsilon) \int_{0}^{t} E_{rel}(t-\tau) \phi(\tau) \frac{\partial \varepsilon}{\partial \tau} d\tau$$
⁽⁹⁾

or, in general:

$$S_{ij}'(t) = g(\varepsilon) \int_{0}^{t} 2G_{rel}(t-\tau) \phi(\tau) \frac{\partial E_{ij}}{\partial \tau} d\tau$$
⁽¹⁰⁾

with $S'_{ii} = S_{ii} - (S_{kk} / 3)I$ the deviatoric part of the Piola-Kirchhoff stress.

 ϕ is a function correcting the basic LVE convolution integral for changing temperature-corrected strain rates; **g(e)** is in general a function of the strain invariants of the deviatoric part of the Green strain tensor, which is valid for large strains:

$$E_{ij} = E_{ij} - \left(\frac{E_{kk}}{3}I\right)$$
 and $E_{ij} = 1/2(F_{ji}F_{ij} - I)$ (11)

For a uniaxial state of stress (like in a uniaxial tensile test at constant strain rate), $g(\varepsilon)$ is a scalar function expressing the ratio of the Cauchy stress as measured during the test to the one that would be obtained applying the LVE constitutive model.

$$g(\varepsilon, \varepsilon' a_T) = \frac{\sigma_{test}(\varepsilon, \varepsilon' a_T)}{\sigma_{LVE}(\varepsilon, \varepsilon' a_T)}$$
(12)

Therefore, $g(\epsilon)$ takes the softening generated by dewetting into account: $g \le 1$ for $\epsilon \ge 0$. In a propellant like formulation 1, with an effective bonding agent, g decreases progressively only when the onset of damage is reached (Fig. 4). Otherwise, g decreases progressively as soon as a tensile

load is applied. Ref. [12] does not prescribe any particular form for the function g as long as experimental data is fitted well. In [14] following fit was taken to fit uniaxial stress-strain data for a formulation 1 propellant:

 $g = c_1 + c_2 x + c_3 y + c_4 xy + (c_5 + c_6 x + c_7 y + c_8 xy) \arctan(c_9 + c_{10} x + c_{11} y + c_{12} xy + c_{13} x^2 + c_{14} x^2 y)$ with x = maximum principal tensile strain, $y = \log_{10} (\varepsilon^3 a_T)$ the temperature reduced strain rate. c_i was determined via least-square fitting of uniaxial tensile data. In a thermoviscoelastic material both rate and temperature control the amount of available energy for microcrack generation and propagation. The use of the damage factor g allows a good correlation between uniaxial tensile tests and theory (9) with respect to what is obtained applying the mere LVE model (Fig. 4).For the 2D samples of this study (formulation 1), following average damage factor was computed:

 $g(\varepsilon) = 0.375$. The stress is therefore 37,5% of what would be generated in the undamaged material without load.



Fig. 4 *LVE* prediction, Swanson model prediction with a calibrated $g(\varepsilon)$ and uniaxial test data, taken from [14]

Thermal simulations

Focusing on (micro)cracks, since the burning rate measurements show faster decomposition rates and subsurface ignition in absence of significant convection, following propagation mechanism for the samples was suggested (Fig. 5) and confirmed by dedicated visualization experiments not reported here:

- 1. A conduction-induced decomposition of binder and small AP particles producing a high concentration of reactive gases in the crack, followed by:
- 2. Local ignition near an AP particle producing a jet of gases having $T \approx T_f$. The gas jet impinges the opposite crack surface and ignites it. Finally:
- **3.** Hot reaction gases fill the crack; thermal energy is delivered to the rest of the crack surface and the whole crack ignites.



into open micro-cracks without convection and radiation

Thermal simulations were performed to check whether this scenario is possible, and in which pressure range it is expected to accelerate the burning rate. If a microcrack is open under the combined action of a previous mechanical damage and tensile load, subsurface ignition is likely to occur in depth if thermal energy can propagate into the crack faster than in the solid phase. Thermal diffusivity, $a = k/\rho c_p$, will therefore play a fundamental role in controlling the energy diffusion through conduction for such a transient diffusion process. Following conditions must be met for the above burning rate augmentation mechanism to hold:

- Thermal energy must propagate faster in the crack than in the solid (the rate of thermal energy propagation in the solid is the burning rate at the pressure and soak temperature under consideration).

- The propagation of thermal energy in the gap is so much faster that the solid heats up to decomposition temperature and ignition occurs in the gas phase before the main burning surface has reached the bottom of the crack.

Considering the thermal diffusivity of the propellant, we have:

 $\lambda_{\text{propellant}} = 0.46 +/- 5.6\%$ W/mK; $a_{\text{propellant}}=2.7 \ 10^{-3} \text{ cm}^2/\text{s}$. (measurement with the hot disk technique on formulation 1).

For the gases in the crack, **a** depends on pressure (inverse proportionality) through its dependence on ρ [15]. Pressure will therefore play a fundamental role because it <u>decreases</u> the thermal diffusivity of

the gas in the crack and at the same time it <u>increases</u> the burning rate, so that the deflagration wave in the solid moves ahead faster.

For air, the thermal conductivity is one order of magnitude less then for the propellant (λ_{air} at 20°C is about 2,6 10⁻² W/mK) but at 1 atm the density is 3 orders of magnitude less, so that a_{air} at 1 atm is about: $a_{air} = 0.188 \text{ cm}^2/\text{s}$ vs. $a_{propellant}=2.7 \times 10^{-3} \text{ cm}^2/\text{s}$. At higher pressure, the situation changes, and at about 80 atm the diffusivities match (at a temperature of 20°C).

Considering a moving reference centered on the deflagration wave, we write the energy equation neglecting chemical reactions ahead of the burning surface or in the gas phase of the crack:

$$c_{p}\rho\frac{\partial T}{\partial t} - \nabla \cdot \left(\rho u c T - \lambda \nabla T\right) = 0$$
⁽¹³⁾

Chemical reactions would increase the local temperature, so the estimate is conservative.

An assumption made for the non-stationary simulation was to neglect the fact that the deflagration wave moves forward. This way, we have a fixed frame of reference and can neglect the convective energy term in equation 13, obtaining Fourier's equation. We can partially compensate it through the boundary conditions assuming stationary thermal profiles during the time required to decompose one of the larger AP particles at the sides of the crack. T at $(x,y,t) = (L_{,.,}0)$ can then be chosen to be the average of the thermal profile in the gas phase, between the surface and L_f. The final flame temperature is calculated at various pressures with Ref. [23] and T_s is taken from [24]. For the formulation investigated we have T_f= 3050 K at 1 bar and T_f= 3500 K at 60 bar. In our case the boundary condition would be approximately T=1990 K at 1 bar and 2250 K at 60 bar. Following the logic above, we take a characteristic time t*_{wave} to be the time needed by the deflagration wave to move ahead by the average size of one larger particle: t*_{wave} = D_{AP} / r_b in our case: t*_{wave} = 0.1739 s at p = 1 bar and t*_{wave} = 34 ms at 60 bar.

Propagation was computed for this time interval for a 2D geometry. At the interface, there is no continuity of physical properties, and the assumption leading to Fourier's equation:

 $\frac{\partial}{\partial x}\left(k\frac{\partial T}{\partial x}\right)$ to $k\left(\frac{\partial^2 T}{\partial x^2}\right)$ does not hold. The domains were therefore separated into gas phase and

condensed phase, where Fourier's equation holds, and calculations were iterated until the fluxes at the interface matched, i.e. applying following boundary condition:

 $k\nabla T \cdot \hat{\vec{n}} = k\nabla T \cdot \hat{\vec{n}}$ at the interface between gas phase and condensed phase

Results at 1 bar and 60 bar qualitatively confirm the previous estimates (Figs.7-8). At about 60 bar, the temperature at 200-300 μ m depth in the crack is just above 620 K, the value at which, according to [16], fast and complete decomposition of AP occurs. Which further reaction will then produce a localized ignition and ignite the rest of the crack walls by impingement/convection depends on pressure. For a premixed flame a temperature of about at least 830 K is necessary (formation of a liquid layer on an AP crystal) [7]: at pressures above 20 bar [6], ammonia and perchloric acid lead to an explosive reaction (the AP premixed monopropellant flame) and localized ignition would occur through this mechanism. At pressures below 20 bar, we know that the AP monopropellant flame generated by HClO₄ and NH₃ does not occur. In this case, HClO₄ would attack the hydrocarbon

molecules coming from some volatilized (and fragmented) binder or plasticizer and produce ignition, i.e. generating the same kind of diffusion flame existing during normal propellant combustion.



Fig. 7 Left: Temperature profile at the centre of the crack (cracks of different width) at 1 bar after $t=t^*$; **Right:** Temperature profile at the centre of the crack after $t=t^*$ for a crack of 4 mm width at 1 (dotted line) and 60 bar (continuous)



Fig. 8 Contour plot of the temperature distribution inside a crack after $t = t^*$ at 1 bar, showing thermal energy penetration in the crack. Right: 2D field for the simulation

This flame would stabilize the $HClO_4$ and NH_3 reaction at explosive rates. Notice that according to [16], $HClO_4$ enhances the volatilization of the binder by fragmenting the molecules at the surface. The source of H/C could be the binder but enough gas could be generated by the plasticizer, which is definitely more prone to evaporation.

At 60 bar, conduction in the gas phase can produce a self-sustained decomposition of AP by a depth equal to the size of a larger AP particle. At most at this pressure, no subsurface ignition in an observable crack will occur. The situation in a motor would be different, because thermal energy exchange by convection of the combustion products in the bore will quickly ignite any small crack.

Correlation of average damage factor and average burning rate

For our samples, we suggest that in the presence of damage caused by dewetting and the action of tensile load, the burning rate is:

$$r_{b,damaged} = \frac{k}{p} \left(\frac{1}{g(\varepsilon)} \right) r_{b,undamaged}$$
(14)

the average $g(\varepsilon)$ was determined to be 0.375. At 1 bar the rate of augmentation was 1.28; k is therefore 0,48. This formula should be validated with more experiments at different damage levels and pressures, since pressure dependence might be more complex. The constants are material-specific would need to be determined by least-square.

In a motor, the thermal energy exchange in microcracks is radically different; convection and hot particles impingement during the ignition transient and normal burning would ignite any microcrack exposed to the burning surface, with significant effects on motor performance [2, 17]. Even radiation alone would produce ignition [18]. An empirical property relationship for damaged material layers for a motor would therefore be:

$$r_{b, damaged} = \left(\frac{k}{g(\varepsilon)}\right) r_{b, undamaged}$$
(15)

Coupling effects with erosive burning mechanisms are possible: if the propellant zone near the surface of the bore or at other highly stressed areas is damaged and is kept under tension because of the compliance of a lightweight case [10,11,13], enhanced thermal energy transfer induced by a high cross-flow might be affected by the different nature of the propellant surface. Widely used semi-empirical models, like Lenoir and Robillard's, have constants which are fitted to match the pressure traces from development motors [19]; these constants would turn out to be different for a damaged lightweight motor if the surface porosity affects the thermal energy exchange in boundary layer.

Effects on motor level

Applying structural analysis [11, 13] to a flightweight motor we observe that under internal pressure the inner region of the grain at the bore is subjected to tensile strain because of the case compliance. A mechanically aged grain would satisfy the conditions investigated in this study if some microcrack-induced softening occurred before ignition: it would have layers of propellant with microcracks kept open by a tensile load during the first phase of burning. Depending on the motor's geometry and load history a quick estimate indicates that the inner portion of the grain could be affected by dewetting and microcracks if the motor has been subjected to thermal cycling and is designed to achieve a good performance (high web fraction). Notice that chemical aging affects the same zone of the grain making it more brittle through oxidative cross-linking [20]. An excellent structural simulation of a large motor showing the distribution of porosity in the grain at the bore can be found in reference [21]. An average 1.5-fold increase of the burning rate in the inner 10% of the web would produce a 5 % decrease in the total burning time, with consequences on the overall performance of the system.

Conclusions

The coupled effect of mechanical damage and load using AP:HTPB propellant samples has been modeled, with following results:

For the samples described in Ref. [3], thermal energy propagates quickly into cracks and microcracks at low pressures. Cracks will ignite starting from hot spots. On the other hand, material previously subjected to dewetting but without cracks burns faster (up to twice as fast).

Mechanical damage coupled with a tensile load changes the burning rate: a correlation between the burning rate increase and the average state of damage using the definitions provided by Swanson and Christensen's NLVE material model was obtained.

If a propellant has an optimal degree of saturation of the binder and an effective bonding agent, the onset of mechanical damage is delayed and nothing happens at strain levels below the threshold of damage. This confirms Summerfield's suggestions indicated in ref. [1]. On the other hand, if for some reason the mechanical properties of the material are poor (e.g. if effective bonding agents for new oxidizer crystals do not exist), then augmented burning rate is expected already at very low strains, since microcracks are generated continuously starting from very low strains.

While the thermal energy exchange into micro- or macro-cracks for the samples of this study is driven by conduction, on a system level, thermal energy exchange into a layer of damaged propellant at the bore would be heavily intensified by radiation and convection; a burning rate increase would occur at all pressures, with potential consequences on a system level.

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