Comparison of Different Passive Control Solutions for Reducing SRM Pressure Oscillations using Cold Flow Experiments

J. Anthoine* and M. R. Lema†
von Karman Institute for Fluid Dynamics, 1640 Rhode-St-Genèse, Belgium

Cold gas experiments are used to study the pressure oscillations occurring in solid rocket motors (SRM) and the performance of different ways of passive control of these oscillations. Previous studies stated that flow–acoustic coupling is mainly observed for nozzles including cavity. The nozzle geometry has an effect on the pressure oscillations through a coupling between the acoustic fluctuations induced by the cavity volume and the vortices traveling in front of the cavity entrance. The most important reduction of pressure oscillations is obtained by removing the cavity located around the nozzle head. It has been firstly proved that removing the cavity located around the nozzle head is a very good solution both for the axial and radial flow injection configurations, with a reduction factor up to 10. However, the nozzle integration cannot be avoided and this solution can then not be implemented on real flight. A permeable membrane (with holes to allow the combustion gas to pass through) placed in front of the cavity allows a reduction by a factor 1.5. The Helmholtz resonator shows small attenuation of the pressure oscillations; however, its design can be optimized in order to maximize the acoustic damping. The 3D-shaped inhibitors show a good attenuation of the pressure fluctuations, especially when the opening cross-section is increased. This increase results in a shift of the Mach number associated to excitation. For a similar cross-section, the asymmetric inhibitor (crenel-shaped) provides a net reduction of 48% compared to an axisymmetric inhibitor. So, the asymmetry of the inhibitor provides a promising way of reducing the pressure oscillations.

I. Introduction

The present research is an experimental investigation of the aeroacoustic instabilities occurring in a sub-scaled cold flow model of the Ariane 5 solid rocket motor. The phenomenon develops in the confined flow established in the motor and involves a coupling between hydrodynamic instabilities and longitudinal acoustic modes.

Aeroacoustic instabilities occur in a wide range of technical applications. The resulting oscillations are sometimes wanted in systems designed to produce the periodic motion efficiently as in musical instruments. Nevertheless, in most cases aeroacoustic instabilities perturb the operation, as for the Ariane 5 launcher. Then, the present research finds its interest knowing that these aeroacoustic instabilities lead to pressure and thrust oscillations which reduce the rocket motor performances and could damage the payload.

II. Previous studies

For technological reasons, large solid rocket motors are composed of a submerged nozzle and segmented propellant grains separated by inhibitors (figure 1). During propellant combustion, a cavity appears around the nozzle. Vortical flow structures may be formed from the downstream inhibitor (Obstacle Vortex Shedding - OVS) or from natural instabilities of the radial flow resulting from the propellant combustion (Surface

*Dr. Associate Professor, Environmental and Applied Fluid Dynamics Department, Chaussée de Waterloo 72; anthoine@vki.ac.be; AIAA Member.
†Research Engineer, Environmental and Applied Fluid Dynamics Department, Chaussée de Waterloo 72.
Vortex Shedding - SVS). Flandro and Majdalani\(^1\) use this concept of surface vortex shedding (SVS) to explain the instabilities in existing SRM’s which do not have inhibitors. Chedevergne et al.\(^2\) develop a stability analysis of the flow induced by wall injection. The hydrodynamic manifestations drive pressure oscillations in the internal flow established in the motor. When the vortex shedding frequency synchronizes acoustic modes of the motor chamber, resonance may occur and sound pressure can be amplified by vortex-nozzle interaction, leading to pressure and thrust oscillations.

![Figure 1. Internal geometry of the Ariane 5 solid rocket motor.](image)

The stability prevision of large solid propellant rocket motors has been an active subject, both in the USA and in Europe, in the past twenty-five years. Although these motors were predicted stable by classical stability assessment methods,\(^3,4\) such grain segmentation conducted to low amplitude, but sustained, pressure and thrust oscillations, on first longitudinal acoustic mode frequencies. These pressure oscillations have been reported for the Space Shuttle RSRM, the Titan-34D SRM, the Titan–IV SRMU and the Ariane 5 MPS.\(^4–8\) All these boosters have a length over diameter ratio (L/D) in the range 9 – 12 and demonstrated similar pressure oscillations, whatever the number of segments. Zero-peak relative amplitudes are typically less than 0.5% for pressure oscillations and less than 5% for thrust oscillations. Similar results were also observed on sub–scaled model rockets\(^9,10\) and from numerical simulations.\(^11–13\) The use of three-dimensional inhibitors to reduce the pressure oscillations was already tested and simulated by Telara \textit{et al.}\(^14\)

From cold flow experiments in a pipe with one or two inhibitors, Culick & Magiawala,\(^15\) Dunlap & Brown,\(^16\) Mettenleiter \textit{et al.}\(^17\) and Anthoine\(^18\) showed that the vortex shedding is produced at the inhibitors, and the confined space of the pipe acts as a resonator with its natural frequencies. Pressure oscillation reaches large amplitude when the vortex shedding frequency is close to the frequency of one resonant acoustic mode of the system. Culick & Magiawala\(^15\) showed that for their experiment it was impossible to sustain acoustic modes with only one inhibitor. The presence of an obstacle downstream of the shedding point of the vortices provides the necessary acoustical feedback when the vortices interact with it.\(^19,20\) In the case of segmented rocket motors, this second obstacle is the nozzle.

The development of original analytical models to point out the parameters controlling the flow–acoustic coupling (see section IV) and the effect of the nozzle design on sound production (non-linear model), was already presented, as well as their validation with experimental data.\(^18,21\) The non-linear model, based on vortex-sound theory, stated that flow–acoustic coupling is mainly observed for nozzles including cavity. The nozzle geometry has an effect on the pressure oscillations through a coupling between the acoustic fluctuations induced by the cavity volume and the vortices travelling in front of the cavity entrance. When resonance occurs, the sound pressure level \(|p'|/p_0\) (or \(P_{rms}/P_s\) in the figures of this report) increases linearly with the chamber Mach number \(M_0\), the excited mode number \(j\) and the nozzle cavity volume \(V_c\):

\[
|p'|/p_0 \sim \frac{\pi \gamma}{\gamma - 1} j M_0 \frac{V_c}{V_{tot}}
\]

where

\[
V_{tot} = \frac{\pi D^2}{4} L
\]

and where \(\gamma\) is the specific heat ratio, \(D\) is the internal diameter of the segments and \(L\) is the total length of the test section. In absence of cavity, the pressure fluctuations are damped.

### III. Experimental facilities

The experiments are conducted on 1/30-scale axisymmetric cold flow models respecting the Mach number similarity with the Ariane 5 SRM when 50% of the propellant is burnt.\(^22–25\) That mid-combustion condition \(a\) \(M_0\) is the the longitudinal Mach number averaged across the cross-section of the segment.
corresponds to the maximum of pulsations. The Mach number, based on the mean flow velocity in the segments, is of the order of 0.1. Since the Reynolds number, based on the same velocity and on the segments diameter, is of the order of $2 \cdot 10^7$ in the full-scale motor, the viscous effects are negligible and do not influence the flow properties. Therefore, exact Reynolds number scaling is not required as long as it is large enough.

The flow is either created by an axial air injection at the forward end (figure 2a) or by a radial injection uniformly distributed along chamber porous cylinders (figure 2b). The cold air is injected in the model through porous materials and the acoustic insulation of the test section from the air supply is checked. Each test section includes only one inhibitor and a submerged nozzle. The main characteristic of the submerged nozzle is the appearance of a cavity around the convergent.

![Diagram](image1.png)

(a) Axial injection  
(b) Radial injection

**Figure 2.** Axial and radial cold flow set-ups (1/30-scale).

With the radial set-up, tests can also be made without inhibitor to simulate the SVS. Experiments prove that the radial injection through porous cylinders simulates correctly the Taylor flow and the combustion situation. Experimental velocity spectra demonstrate that the flow is stable near the forward end until it reaches a critical axial position. Then, the flow becomes unstable downstream for a range of frequencies which increase with the distance from the forward end. Therefore, vortices are issued from the hydrodynamic instability of the radial injection (SVS).

The two test sections allow a wide range of parameters to be tested such as the total length, the inhibitor shape and the nozzle geometry. The internal Mach number can be varied continuously by means of a movable needle placed in the nozzle throat. The acoustic pressure measurements are performed by piezoelectric transducers and signal treatment yields the amplitude and the frequency of the pressure oscillations. The frequency resolution is smaller than 1 Hz.

**IV. Identification of flow-acoustic coupling**

Flow-acoustic coupling is identified by plotting the evolution of the maximum pressure fluctuation, in terms of frequency and amplitude, versus Mach number. Figure 3a shows the pressure fluctuation spectrum plotted versus Mach number $M_0$ and frequency $f$, for the axial flow injection configuration with an inhibitor of 58 mm internal diameter placed at 71 mm from the head of the submerged nozzle. Oscillation frequencies $f$ are close to the resonance frequencies. In first approximation the acoustic standing wave can be modeled by that of a closed-closed pipe segment of length $L$. In the Mach number range between 0.072 and 0.082, the frequency of the peak ($f = 850$ Hz) is very close to the second longitudinal acoustic mode frequency of
the test section estimated by \( f_{ac,2} = c_0/L \), where \( c_0 \) is the speed of sound (\( c_0 = 338 \text{ m/s} \)) and \( L \) is the total length (\( L = 0.393 \text{ m} \)). The oscillation frequency seems to vary slowly and linearly with the Mach number. This change takes care for the necessary phase shift needed to compensate for the change in travel time of vortical structures which is needed to obtain a phase shift equal to an integer number of \( 2\pi \) along the feedback loop. This phenomena has been extensively described for deep cavities\(^{26, 27}\) and the flute.\(^{28, 29}\) We will therefore call this a flute behavior.

\[
\text{Mach number} \quad \text{Frequency [Hz]}
\begin{array}{ccccccccccc}
0.04 & 0.05 & 0.06 & 0.07 & 0.08 & 0.09 \\
200 & 500 & 800 & 1100 & 1400 & 1700 & 2000
\end{array}
\]

\[
\text{Prms/Ps}
\begin{array}{ccccccccccc}
\end{array}
\]

Figure 3. Contours of pressure fluctuations in terms of frequency and amplitude (a) and evolution of the maximum of the pressure fluctuation, in terms of Helmholtz number and amplitude (b). Axial flow injection; \( L = 393 \text{ mm}; l = 71 \text{ mm}; d = 58 \text{ mm} \); submerged nozzle.

Flow-acoustic coupling stands-up when the vortex shedding frequency is equal to the acoustic mode frequency. There is a lock-on phenomenon of the vortex shedding frequency that jumps between the acoustic mode frequency.
modes. At resonance, the pressure spectra show excitation of the first two longitudinal acoustic modes for the axial flow injection (OVS), successive “long” excitations of the second and then third modes for the radial flow injection with inhibitor (OVS/SVS) and multiple successive “short” excitations of the first and then second longitudinal modes for the radial flow injection without inhibitor (SVS). “Long” and “short” are referring to the size of the range of Mach number of each individual excitation. In all configurations, the sound pressure level reached under resonant condition results from a vortex-nozzle interaction. The vortices impinge on the nozzle head more coherently and feed energy into one mode of the system, bringing the fluctuation level to larger value. The geometrical parameters such as the inhibitor-nozzle distance, the total length, the inhibitor inner diameter and the test section inner diameter, select the acoustic modes to be excited, the number of vortices present between the shedding point and the nozzle and the internal Mach number of excitation.

It is worth attempting to derive an analytical model to predict the conditions for the occurrence of flow-acoustic coupling. This model is based on Rossiter’s approach. The generation of self-sustained sound resonance in a tube depends on the phase of the acoustic oscillation which will occur at the acoustic node, which is the phase at the acoustic node, which is the phase at which the acoustic pressure node (highest acoustic pressure) is located. In the present model, the upstream obstacle is the inhibitor while the downstream one is the nozzle. Finally, one gets a relation linking the Mach number \( M_0 \) to the excited mode number \( j \), the stage number \( m \), the relative position of the inhibitor compared to the total length of the test section \( l \) and to the relative internal diameter of the inhibitor compared to the test section diameter \( d \) / \( D \):

\[
M_0 = \frac{C_{vc}}{2k_v} j \frac{l}{m - 0.25 L} \left( \frac{d}{D} \right)^2
\]

where \( C_{vc} \) is the “vena contracta” coefficient of the jet generated by the inhibitor and \( k_v \) is the ratio of the vortex transport velocity to the jet velocity. From the experimental investigation of the vortex properties, \( C_{vc} = 0.68 \) and \( k_v = 0.47 \).

When resonance occurs, the selection of the acoustic mode \( j \) depends on the relative position \( l / L \) of the inhibitor compared to the acoustic mode shape. Indeed, to yield high sound pressure levels and to obtain a maximum of acoustic receptivity at the inhibitor, the inhibitor must be as close as possible to an acoustic pressure node (highest acoustic velocity fluctuations). Then, the coupling will occur for that acoustic mode only when an integer number of vortices \( m \) are present between the inhibitor and the nozzle. The flow-noise coupling will excite that acoustic mode with that number of vortices only for some Mach number \( M_0 \) depending on the geometrical parameters \((l / L) \) and \((d / D) \) so that relation 3 is respected.

Looking at figure 3b, the maximum of the sound pressure level is observed experimentally to excite the second mode at a Mach number \( M_0 \) equal to 0.082. The inhibitor is placed at 26% from the end of the test section. Then, the excited mode \( j \) will be preferably the second longitudinal acoustic mode. From equation 3, a flow-noise coupling is predicted to occur at a Mach number equal to 0.086 with \( m = 2 \) vortices located between the inhibitor and the nozzle. Such a finding is also in good agreement with numerical simulations and experimental observation obtained from PIV (Particle Image Velocimetry) measurements.

V. Nozzle design modification as control of pressure oscillations

The modification of the nozzle design has been tested for both axial and radial flow configurations. As indicated by the original analytical model (Equation 1), the nozzle geometry is expected to play an important role in the amplification of the sound pressure fluctuations. Indeed, by changing the nozzle design, the downstream obstacle, on which the vortices “impinge” generating the acoustic waves, is modified as well as the flow field around the nozzle. However, the main reason of the nozzle effect is coming from the cavity volume around the nozzle. The compressibility of the air in that cavity volume induces an acoustical velocity at the cavity entrance which interacts with the vortices when they are passing just in front of the cavity entrance generating “noise”.

Several nozzle geometries have been compared for both axial and radial flow injection. For all the nozzles, the convergent and divergent parts have the same profiles. Only the cavity geometry is changed by filling it. Note that, when filling the cavity, the effective total length of the test section \( L \) is also slightly changing and the acoustic mode frequencies are modified accordingly. Figure 5a shows the evolution of the maximum of the pressure fluctuations for the four nozzles with a throat diameter of 30 mm and for an inhibitor-nozzle distance of 71 mm, for the axial flow configuration (OVS). The evolution of the Helmholtz number \((He = f l / c_0, \text{where } c_0 \text{ is the speed of sound})\), not shown here, is similar for all the nozzles except for nozzle 4,
without cavity. It means that the vortex shedding does excite the second longitudinal acoustic mode within the same Mach number range for all the nozzles. The maximum of sound pressure level that corresponds to the maximum of coupling appears at $M_0 = 0.08$ whatever the nozzle geometry. But the amplitude of the maximum resonance is highly dependent on the nozzle design. When the nozzle cavity volume decreases (from nozzle 1 to 2), the pressure fluctuation drops. The effect of the nozzle head geometry is shown by nozzle 3. This nozzle presents a smaller cavity volume than nozzle 2 that results in fainter pressure fluctuations compared to nozzle 2. Finally, the nozzle without cavity (nozzle 4) shows pressure levels reduced by a factor 10, compared to nozzle 1.

![Graph](a)

Figure 5. Pressure oscillations for different nozzle geometries (a) and effect of nozzle cavity volume on pressure levels (b) for the axial flow configuration; $L \sim 300$ mm; $l = 71$ mm; $d = 58$ mm.

The effect of the nozzle cavity volume on the amplification of the pressure fluctuations occurring on the second acoustic mode at $M_0 = 0.08$ is summarized in figure 5b, for the axial flow configuration. The evolution of the maximum sound pressure level is approximately linear with the nozzle cavity volume. Such a result compares well to the analytical model developed on purpose (Equation 1).

Figures 6a and b provide the comparison of pressure oscillations with and without cavity around the nozzle for the radial flow injection configuration with an inhibitor placed in the middle of the test section (OVS/SVS) and without inhibitor (only SVS), respectively. The nozzle design has also an effect on the amplitude of the pressure oscillations in the case of radial flow injection, even if that effect is less visible in the case of

![Graph](a)

(a) With inhibitor ($l = 318$ mm; $d = 58$ mm)

![Graph](b)

(b) Without inhibitor

Figure 6. Pressure oscillations for different nozzle geometries for the radial flow configuration; $L \sim 300$ mm.
only SVS instability. It has been shown that pressure oscillations are coming from a coupling between the acoustic fluctuations induced by the cavity volume and the vortices travelling in front of the cavity entrance. So, the best solution for passive control of the pressure oscillations is to replace the submerged nozzle by a non-integrated nozzle (without cavity). However, in practice this integration is needed for the rocket thrust control. For evident practical reasons, it is then not possible to remove the integration.

VI. Other passive ways as control of pressure oscillations

Control of pressure oscillations by passive ways remains the only possibility of improving the SRM performance. The following passive ways are only tested for the axial flow configuration (OVS). The general idea is to prevent the vortices to interact with the nozzle cavity while passing in front of the cavity entrance. For this purpose, three kinds of passive control systems are explored:

- Insertion of a membrane (impermeable or permeable) in front of the cavity entrance to prevent vortex/nozzle interaction;
- Installation of a resonator (Helmholtz resonator or quarter wavelength tube) to damp the pressure oscillations;
- Modification of the inhibitor geometry (3D shaped, outlying) to reduce the vortex coherence.

A. Insertion of a membrane

The idea is to prevent the vortices to interact with the nozzle cavity while passing in front of the cavity entrance by inserting a solid membrane at the entrance of the cavity. The first membrane to be tested is completely impermeable (figure 7) and is expected to damp completely the pressure oscillations, since the flow-acoustic coupling should disappear. In fact, the results should be similar to those obtained without cavity at the nozzle. This membrane is then the best solution for passive control of integrated nozzle. However, using a solid membrane, the integrated nozzle cannot be surrounded by propellant. The last propellant grain should be between the inhibitor and the membrane. This will result in a reduction of the performance of the launcher, since the ratio of propellant mass to inert mass is reduced.

To overcome this problem of propellant mass reduction, the next idea is to use a permeable membrane for passive control (figure 7). That membrane presents 16 small circular holes through which the flow coming from the propellant combustion can exit the nozzle cavity. The diameter of the holes is equal to 6 mm. The motor performance should not be affected by this membrane but the vortices are still able to interact with the acoustic fluctuations induced by the cavity volume. It is however expected that this interaction will be weaker than without membrane producing smaller pressure oscillations.

Pressure fluctuations are measured for an inhibitor with orifice diameter of $d = 0.058$ m placed at a distance $l = 0.071$ m from the head of the submerged nozzle. The maximum of the pressure fluctuation is plotted versus Mach number for the different test cases in figure 8.

The maximum of sound pressure levels that corresponds to a coupling on the second acoustic mode appears at $M_0 = 0.08$. But the amplitude of the maximum resonance is influenced by the use of passive control. Without cavity, the pressure fluctuation levels remain similar whatever the Mach number indicating that vortex-acoustical coupling has disappeared. In such condition, the pressure level is reduced by a factor above 10. As expected, the passive control with impermeable membrane produces the same pressure oscillations than when the cavity is not present, so a reduction by a factor above 10. This proves again that the pressure oscillations are induced by the presence of the cavity, through a coupling between the
acoustic fluctuations induced by the cavity volume and the vortices traveling in front of the cavity entrance, as already explained through the analytical model.

However, the use of impermeable membrane results in a reduction of the performance of the launcher, as explained before. With the permeable membrane, the attenuation of the pressure oscillations is less than without cavity or with the impermeable membrane. Looking to the excitation of the second mode around $M_0 = 0.08$, the pressure oscillations are weaker by a factor 1.5 (attenuation factor of 0.67) compared to the submerged nozzle without membrane.

The analytical model (relation 1) is adapted for the passive control with permeable membrane to determine the attenuation factor of the pressure oscillations. The difference compared to what was done previously is that the cross-surface of the cavity entrance is reduced to the section of the 16 holes in the permeable membrane and the mean distance over which the vortex travels in front of the cavity entrance is reduced to the mean distance in vortex path direction over the hole cross-surface. When using the permeable membrane, the attenuation factor of the pressure oscillations is given by:

$$\frac{|p'_{\text{control}}|}{|p'_{\text{no control}}|} \sim \frac{\pi}{4}$$

which is very close to the measured attenuation factor of 0.67.

B. Installation of a resonator

Resonators are acoustical elements used to attenuate the sound at narrow band frequencies both in ducts and tubes. A simple resonator comprises a cavity enclosing a mass of air, with a narrow opening to the outside called neck. In this way, the mass of air effectively acts as a “spring” at the resonant frequency of the cavity and under those conditions absorbs appreciable sound energy exciting the resonance.

The considered resonator is of the Helmholtz type and is placed at the forward end to coincide with a pressure anti-node. The resonant frequency of the Helmholtz resonator is given by:

$$f_r = \frac{c}{2\pi} \sqrt{\frac{S}{V(l_N + 2\delta)}}$$

where $V$ is the volume of the resonator cavity, $S$ is the neck cross-section, $l_N$ is the neck length. The correction factor $\delta$ depends on the radius of the neck, $r$:

$$2\delta = 1.7r$$
The transmission coefficient of this resonator can be calculated using the following equation:

\[
\alpha_t = \frac{1}{1 + \frac{c^2}{4S^2_t \left( 2\pi f_r \frac{t_N + 2\delta}{S} - \frac{c^2}{2\pi f_r V} \right)^2}}
\]

(7)

where \(S_t\) is the cross-section of the SRM model. This transmission coefficient becomes zero at the resonance frequency, as plotted in figure 9a. At this frequency large velocity amplitudes exist in the neck of the resonator, and all acoustic energy transmitted into the resonator cavity from the incident wave is returned to the main pipe, with such a phase relationship as to be reflected back towards the source.

\[\text{fr} \ [\text{Hz}]\]
\[\alpha_t\]
\[750\]
\[800\]
\[850\]
\[900\]
\[950\]
\[1000\]
\[0\]
\[0.2\]
\[0.4\]
\[0.6\]
\[0.8\]
\[1\]
\[83\]
\[89\]
\[0.2\]
\[0.4\]
\[0.6\]
\[0.8\]
\[1\]
\[83\]
\[89\]
\[0.2\]
\[0.4\]
\[0.6\]
\[0.8\]
\[1\]
\[83\]
\[89\]
\[0.2\]
\[0.4\]
\[0.6\]
\[0.8\]
\[1\]

Figure 9. Attenuation coefficient of the Helmholtz resonator and pressure oscillations measured with it.

The evolution of the pressure fluctuation amplitude and Helmholtz number in function of the Mach number is presented in figure 9b, compared to the data of the nominal configuration (without resonator). At the frequency of the second acoustic mode, the maximum pressure fluctuations are attenuated by a factor of 2. However, even though the attenuation is good, the range of frequency where the Helmholtz resonator is effective is very narrow. As plotted in figure 9a, an attenuation of at least 30% is obtained between 860 Hz and 890 Hz, which corresponds to a frequency bandwidth of 30 Hz. Making a zoom of figure 3a, the evolution of the frequency with Mach number during the acoustic coupling is approximately linear with a slope of 3000 Hz/Mach. Therefore, a frequency bandwidth of 30 Hz corresponds to a Mach range of 0.01. This is fully coherent with the results of figure 9b where the Helmholtz resonator proves to be efficient in the Mach number range between 0.08 and 0.09. This frequency bandwidth where the resonator is effective can be modified by changing the resonator cavity and the neck dimensions, but with the disadvantages of higher transmission coefficient, which means that the resonator attenuates a wider range of frequencies but less effectively. So, the resonators are not providing the best control performance. However, they could be optimized.

C. Modification of the inhibitor geometry

A last way to reduce the pressure oscillations consists in destroying the coherence of the vortices before they reach the nozzle. This is possible by modifying the inhibitor geometry. The nominal geometry is annular but other geometries are considered: an inhibitor of diameter \(d = 58 \text{ mm}\) with outlying opening (center shifted by 5.5 mm), an inhibitor with axisymmetric opening \((d = 58 \text{ mm})\) but randomly drilled at five locations and an inhibitor with a crenel-shaped opening section \((d = 58 \text{ mm})\) made of seven crenel cuts (figure 10).

The pressure fluctuations are measured for these 3D-shaped inhibitors placed at a distance \(l = 0.071 \text{ m}\) from the head of the submerged nozzle. Figure 11 shows the evolution of the maximum of the pressure fluc-
tuations for the different 3D-shaped inhibitors. The nominal case (axisymmetric inhibitor with $d = 58$ mm) and the axisymmetric inhibitor with $d = 62$ mm are also shown in figure 11.

![Figure 10. 3D-shaped inhibitors.](image)

![Figure 11. Pressure oscillations with 3D-shaped inhibitors; submerged nozzle; $l = 0.071$ m.](image)

With the inhibitor with outlying opening, the level of pressure fluctuations lessens. The height $h$ of that inhibitor varies from 3.5 mm to 14.5 mm, but has the same influence on the frequency and pressure levels than decreasing the inhibitor height in the complete perimeter, as happens for the inhibitor of 68 mm (not shown here), where the height $h$ is constant and equal to 4 mm. The outlying of the inhibitor open area seems to be a very good candidate for passive control.

For the inhibitor with axisymmetric opening but randomly drilled, the cross-section of the inhibitor with axisymmetric opening but randomly drilled is increased, as indicated in table 1, which results in a shift of the Mach number range compared to the nominal case (figure 11). This shift was already found analytically in relation 3. From relation 1, the pressure oscillations are linearly proportional to the Mach number. Therefore, a shift of the Mach number should be associated to a proportional increase of the pressure oscillations. However, the pressure level amplitude associated to the randomly drilled inhibitor is slightly decreased. Taking into account the expected increase due to the Mach number shift, the effective
reduction of the pressure oscillations is equal to 25%. So, the asymmetric shape of the drilled inhibitor allows to reduce the pressure level amplitude.

<table>
<thead>
<tr>
<th>Inhibitor</th>
<th>Opening area ([\times 10^{-3}\text{m}^2])</th>
<th>Mach number</th>
<th>Maximum (P_{\text{rms}}/P_s)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>ratio</td>
<td>(\times 10^{-4})</td>
<td>ratio Net increase (+) or reduction (-)</td>
</tr>
<tr>
<td>(d = 58) mm</td>
<td>2.642</td>
<td>1.085</td>
<td>6.0 1.0 0</td>
</tr>
<tr>
<td>(d = 62) mm</td>
<td>3.019</td>
<td>0.11</td>
<td>10.1 1.67 +37%</td>
</tr>
<tr>
<td>Drilled</td>
<td>2.838</td>
<td>0.095</td>
<td>5.0 0.83 −25%</td>
</tr>
<tr>
<td>Crenel-shaped</td>
<td>3.032</td>
<td>0.11</td>
<td>5.2 0.87 −33%</td>
</tr>
</tbody>
</table>

Table 1. Opening area of the inhibitors (the two first are axisymmetric ; the two last are asymmetric) and corresponding Mach number for excitation of the second acoustic mode.

Regarding the crenel-shaped inhibitor with the same inner diameter than the nominal case, the conclusions are similar than for the drilled inhibitor (figure 11). The evolution of the maximum of the pressure fluctuations for the crenel-shaped inhibitor is similar to that obtained with an axisymmetric inhibitor of 62 mm inner diameter (figure 11). In both cases, the excitation appears on the second acoustic mode and at the same Mach number, but for a higher Mach number compared to the nominal case due to the increase of the cross-section (table 1). This shift of the Mach number is again proportional to the increase of the cross-section. Taking into account the expected increase of pressure oscillation level due to the Mach number shift (relation 1), the effective reduction of the pressure level is equal to 33\%. For the axisymmetric inhibitor \((d = 62\) mm), the increase of the pressure level compared to the nominal case is equal to 67\%, while the expected augmentation of pressure oscillations due to the Mach number shift associated to the cross-section increase (relation 1) is 29\%. Since these two inhibitors (62 mm and crenel-shaped) are characterized by the same opening cross-section, the excitation appears for the same mode at the same Mach number \((M_0 = 0.11)\). It is then possible to deduce from these two tests the effective reduction of the pressure oscillations due to the dissymmetry of the inhibitor. That reduction is equal to 48\%.

These results also confirmed that, as expected from relation 3, the increase of the cross-section of the inhibitor is associated to a proportional shift of the Mach number, as plotted in figure 12. The Mach number that crosses the second acoustic mode versus the opening area follows a linear evolution and, therefore, if the opening area of the inhibitor is higher than \(3.6 \times 10^{-3}\text{m}^2\), the second longitudinal mode of the present setup is not excited.

Figure 12. Evolution of the Mach number that crosses the second acoustic mode versus opening area of different inhibitors.

As conclusion, the asymmetry of the inhibitor provides a promising way of reducing the pressure oscillations. A further parametric investigation is needed to determine the influence of the inhibitor shape parameters (type of asymmetry, number and height of the crenel cuts, ...) and to deduce the optimal shape.
VII. Conclusions

This paper has been dealing with different ways of passive control of pressure oscillations in SRM and with their performances in cold gas conditions. The most important reduction of pressure oscillations is obtained by removing the cavity located around the nozzle head. This has been proved to be a very good solution both for the axial and radial flow injection configurations, with reduction by a factor up to 10. However, the nozzle integration cannot be avoided and this solution can then not be implemented on real flight. An impermeable membrane in front of the cavity gives the same result than that of a nozzle without cavity while a permeable membrane (with holes to allow the combustion gas to pass through) allows a reduction by a factor 1.5. Two types of resonators were designed and tested to damp the pressure oscillations in the model. Both the quarter wavelength tube and the Helmholtz resonator show small attenuation of the pressure oscillations. However, their design can be optimized in order to maximize the acoustic damping. Regarding the 3D-shaped inhibitors, they show a good attenuation of the pressure fluctuations, especially when the opening cross-section is increased. This increase results in a shift of the Mach number associated to excitation. For a similar cross-section, the asymmetric inhibitor (crenel-shaped) provides a net reduction of 48% compared to an axisymmetric inhibitor. So, the asymmetry of the inhibitor provides a promising way of reducing the pressure oscillations.

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References


