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Mechanically amplified milli-Newton thrust balance for direct thrust measurements of electric thrusters for space propulsion

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Abstract

Direct thrust measurements by means of a thrust balance are the golden standard for measuring thrust and concurrently the specific impulse in electric thrusters. To measure these properties in the novel class of electrode-less plasma thrusters a new thrust balance based on the VAHPER (Variable Amplitude Hanging Pendulum with Extended Range) concept has been developed.

The thrust balance has a mechanical amplification mechanism with an angular magnification of $31^\circ/^\circ$. Using Lagrangian mechanics we show the thrust balance loaded with a 5.2 kg thruster prototype to behave like a damped harmonic oscillator with a natural frequency of 0.37 Hz. A variable damping system provides damping with an optimal damping ratio of 0.78 which corresponds to a settling time of only 1.8 s. Both the model as well as the damping and calibration system have been validated.

To accommodate the particularities of medium power electrode-less plasma thrusters the thrust balance design includes the following features: an optical displacement sensor, water cooling, liquid metal connectors, dedicated vacuum-rated electronics for auto-leveling, remote (in-vacuum) calibration and temperature monitoring.

To test the thrust balance, measurements were performed on a 500W Helicon Plasma Thruster breadboard model. When loaded with this thruster the measured stiffness of the system was 12.67 ± 0.01 mN/mm. For this stiffness the thrust range is 150 mN with a 0.1 mN resolution. The relative uncertainty on the thrust measurements is found to be on the order of 2%.

Index Terms

direct thrust measurement, thrust balance, force measurement, calibration, space propulsion, electric propulsion, helicon plasma thrusters

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I. INTRODUCTION

Space propulsion is rapidly electrifying, as well established technologies such as Hall-effect thrusters (HET) and gridded ion thrusters (GIT) are becoming the propulsive subsystem of choice for many new spacecraft, for both scientific [1], [2], [3] as well as for commercial [4], [5] applications.

Maneuvers in space are defined by the required velocity-increment of the spacecraft, or Δv . This velocity increment is generated by firing the on-board thruster, expelling propellant. The propellant mass required to generate a given Δv , relative to the spacecraft mass, is governed by Tsiolkovsky's rocket equation:

$$\frac{m_p}{m_{sc}} = \exp\left(\frac{\Delta v}{v_{ex}}\right) - 1 \quad (1)$$

Here v_{ex} is the exhaust velocity of the propellant expelled from the thruster, m_p is the propellant mass and m_{sc} the spacecraft dry mass (i.e. without propellant). A high v_{ex} reduces the propellant mass required for a given Δv . A lower propellant mass translates to lower launch costs, as they scale with the payload mass. The advantage of electric propulsion then lies in its ability to produce very high exhaust velocities.

Conventional rocket propulsion uses the chemical energy released by a combustion reaction of the propellant to thermally expand the generated hot gas in a convergent-divergent, or 'De Laval' nozzle and thereby accelerating it. The chemical energy available for conversion into kinetic energy is ultimately limited by the reaction enthalpy of the combustion reaction. There exists therefore a theoretical maximum of the exhaust velocity in chemical rockets, which lies around 5 km/s.

Electric propulsion (EP), more specifically electro-static and electro-magnetic thrusters, instead use the electric power on-board a spacecraft to accelerate the propellant, typically an ionized gas, to very high exhaust velocities by means of electro-magnetic forces [6], [7]. In this case there is no theoretical limit, readily producing exhaust velocities in excess of 15 km/s. The disadvantage is the reduced thrust-to-power ratio: $T/P = 2\eta/v_{ex}$, where η is the overall efficiency of the engine. EP engines therefore excel once in orbit, where low thrust on the order of milli-Newtons is sufficient for most maneuvers.

EP is an active field of research looking to improve mature technologies such as the HET and GIT as well as to develop new technologies such as electrode-less thrusters, pulsed plasma thrusters (PPT), electrosprays and field-enhanced electric propulsion (FEEP) [8], [9], [6]. Experimental work in EP is often involved with the characterization of thruster performance.

As may be clear from the previous paragraphs thrust is one of the most critical performance markers of EP devices, along with the specific Impulse (I_{sp}) and the thruster efficiency. Specific impulse is defined as the ratio of the thrust to the mass-flow-rate, normalized with $g_0 = 9.8065 \text{ m/s}^2$ the standard acceleration

due to gravity. It is a measure of the effective exhaust velocity. Direct thrust measurements by means of a thrust balance (TB) are the golden standard for measuring these quantities.

Direct thrust measurements of plasma thrusters have been common place in the EP community. While thrust measurements for chemical rocket motors are quite straight forward and based on load cells, thrust measurements of EP devices are more elaborate. EP devices also usually only operate in a high vacuum environment ($p \sim 10^{-5}$ mbar) confining experiments to a vacuum chamber. Due to their inherent low thrust-to-weight ratio they require both a rigid structure to support their weight as well as a sensitive, i.e. compliant structure that is able to transduce the low thrust values to a measurable signal. The solution of these opposite requirements generally comes in the form of a pendulum, either simple [10], [11], inverted [12], [13] or torsional [14], [15], [16] that converts thrust into displacement which is then measured. Each of these configurations has its advantages and disadvantages, which are summarized in Tab. I.

The simple pendulum is the most simple solution. It is naturally stable under external perturbations but also has the lowest sensitivity. Furthermore, its sensitivity is dependent on the pendulum length which is limited by the dimensions of the vacuum chamber that has to contain it. The inverted pendulum is the most common type of TB [17]. It is less stable than the simple pendulum, but has better sensitivity which is governed by the stiffness of the supporting flexures. The sensitivity is therefore much less dependent on the thruster mass. However, the stiffness of the flexures are prone to changes for instance, thermal drifts either due to changes in ambient temperature or due to thruster heat loads. Thermal management in a vacuum environment is challenging due to the lack of convection and the heat dissipated from the thruster can easily affect the thrust balance, including the flexures.

The inverted pendulum is also more compact than a simple pendulum. This has the advantage that the performance is less limited by the vacuum chamber dimensions. The disadvantage is that it also places the thruster, which is a source of heat and EMI, close to any sensitive components. This is of particular concern for a RF thrusters like the Helicon Plasma Thruster (HPT), where heat dissipation and EMI are significant.

The torsional pendulum has superior sensitivity. Since the plane of motion is perpendicular to the gravitational force the sensitivity is independent of the thruster mass. However the horizontal, often asymmetric, arrangement is not always easy to manage inside the limited space of a vacuum chamber. It would also place the thruster closer to the side walls of the chamber, resulting in plume-wall interaction that degrades thruster performance. These limitations are of particular concern for larger thrusters; torsional balances have therefore been most successfully applied for testing micro-propulsion devices, those providing continuous thrust as well as pulsed thrusters.

Polzin et al. [18] proposed a modification of the hanging pendulum that was dubbed the Variable

TABLE I
ADVANTAGES AND DISADVANTAGES OF THRUST BALANCE TYPES.

Design	Advantage	Disadvantage
Simple Pendulum	Stable under external perturbations	Lowest sensitivity, depends on pendulum length
Inverted Pendulum	High sensitivity Compact	Sensitivity depends on flexures, prone to changes Compact
Torsional Pendulum	Superior sensitivity, independent of mass	Asymmetric arrangement

85 Amplitude Hanging Pendulum with Extended Range (VAHPER). It includes a secondary, horizontal arm
86 that is mechanically coupled to the main pendulum arm and results in a mechanical amplification of
87 the displacement. The displacement measurement is performed on the secondary arm which allows for
88 placing the sensor far away from the thruster without losing sensitivity. In their design, it was furthermore
89 possible to change the location of the pivot of the secondary arm and thereby vary the amplification factor.

90 In this work we present a new thrust balance developed by the Space Propulsion and Plasmas Team
91 (EP2) of the Carlos III university of Madrid (UC3M), based on the design of Polzin, to perform direct
92 thrust measurements on an experimental helicon thruster platform.

93 Helicon plasma thrusters (HPT) [19], [20], [21] are electrode-less thrusters that use helicon waves to
94 produce high density plasmas. These waves are excited at frequencies in the Megahertz range, typically
95 13.56 MHz in the presence of an applied magnetic field [22]. HPT rely furthermore on magnetic nozzles
96 for plasma acceleration by converting the thermal electron energy into ion kinetic energy. Thrust is
97 generated by the magnet repulsion between diamagnetic plasma currents and the magnetic circuit of the
98 thruster [8], [6].

99 EP2 has been studying these devices for the past ten years, both theoretically [23], [24], [25], [26],
100 [27] and experimentally [28], [29]. The current experimental platform, the HPTx, developed jointly by
101 EP2 and SENER Aeroespacial is a flexible platform for investigating the effect of different design and
102 operational parameters on the performance as well as the underlying physics. It can operate with either
103 solenoids or permanent magnets, uses different propellants such as Argon and Xenon and can operate
104 with up to 1 kW of RF power.

105 To date, all experimental characterization of the HTPx has been performed mainly with electrostatic

probes. Current performance estimates are about 5-8 mN of thrust, 750-1200s of specific impulse and 7-10% of efficiency [28], [29]. However, electrostatic probes only provide indirect measurements of thrust and specific impulse. The need for reliable thrust measurements has driven the development of a new TB, designed to primarily accommodate the HPTx prototype but also applicable to other thruster types.

The HPTx weighs about 5 kg thus has an estimated thrust-to-weight ratio of only 2 mN/kg, produces strong RF fields, requires electrical currents of up to 30 A, and presents thermal heat loads of up 1.5 kW. This imposes a set of stringent requirements on the design of the thrust balance. In this article we will present both a theoretical analysis of the TB design and its technical implementation as well as validation & calibration results and some preliminary thrust measurements.

The rest of the article is structured as follows. In section II a novel analysis of the dynamic response of this type of balance, using the Lagrange formalism, is introduced as well as a model for the damping system. Section III details the technical implementation of all the features of the thrust balance. Here we also introduce briefly the vacuum facility and the thruster. In section V we present results on the validation of the dynamical model, the damping system and the calibration method as well as actual thrust measurements obtained with the HPTx. The article ends with a discussion on the obtained results and the conclusions.

II. MODELLING

We start with defining our system. As can be seen in Fig. 1, the reference frame is centered at O with the x -axis rightwards, the y -axis upwards and the z -axis out of the plane. The mechanism consists of three articulated rigid elements numbered 1, 2, 3. The first element is an upside-down L-shaped component suspended at its corner from the fixed frame at O , by means of a flexure bearing with torsional spring constant κ_1 . The second element, a straight beam, is anchored to the fixed frame at point A , also by a flexure bearing with constant κ_2 . The joints in point O and A have one degree of freedom (DoF): rotation about z . The primary and secondary elements are interconnected with a third element, another straight beam, joined at B and C by flexure bearings with κ_3, κ_4 , respectively. Points B and C have 3 DoF each, rotation around z and translation in the $x - y$ plane. The thruster, with mass M , is suspended from the bottom of element 1, while a counterweight with mass m is attached to the right end of element 2.

The positions of the centers of mass (CoM) of all elements i are noted $\mathbf{r}_i$, where we numbered the thruster and the counterweight 4 and 5, respectively. The point of element 2 where the displacement is measured is labeled E . Point D is the point where the damping force acts. Whereas point O and A are fixed to the reference frame, points B, C, D and E can move; when *not* in their initial position (i.e. when the vertical part of element 1 is parallel to the local gravity vector) they are denoted with a prime i.e.

Fig. 1. Schematic of the thrust balance mechanism.

B', C', D', E' (this would correspond to the positions colored in grey in Fig. 1). There are four relevant angles, $\theta_1, \theta_2, \theta_3$ and θ_4 as shown in Fig. 1. Lastly, it has to be noted that the gravity vector is pointing in the $-y$ direction.

The quantity of interest in conventional pendulum balances is the horizontal displacement of r_4 - here denoted x - and how it is related to the applied force T . In reference [18] it was shown that in this particular arrangement the vertical displacement of E , denoted y , is a constant multiple of x . However, as the relationship between x, y is dependent on the centre of thrust of the thruster, it makes more sense to speak of the relationship between the angles θ_1, θ_2 , as this is independent of the thruster. It can be

shown that $\theta_2 = n\theta_1$, as we will see later. For the interested reader we have reproduced the equations from the static analysis of [18] using our nomenclature; they can be found in appendix A. When looking at eq. (30)-(37) in appendix A it becomes apparent that the angles θ_i are all a function of $\theta_1 \equiv \theta$ (here we have dropped the subscript for clarity). For the following analysis it is convenient to work with the differential angles $\delta_i(\theta) = \theta_i(\theta) - \theta_i(0)$ referred to the equilibrium position $\theta = 0$. The equilibrium positions for the angles θ_i are $\theta_1 = \theta_2 = 0$ and $\theta_3 = \theta_4 = \pi/2$.

To find the dynamic response of the system we can analyze it using Lagrangian mechanics. We consider the system formed by 5 elements. To simplify the calculations somewhat, we will neglect the motion of the connecting element $i = 3$, which is a reasonable approximation, considering its size and mass compared to the other elements. When considering small angles (as we will do), element 3 ideally experiences no rotational motion, further justifying this approximation. We first define the Lagrange function of the full system:

$$\mathcal{L} = E_k - U \quad (2)$$

with E_k the kinetic energy and U the potential energy which are defined as:

$$E_k = \frac{1}{2} \sum_i m_i \dot{\mathbf{r}}_i^2 + \omega_i^T \bar{\bar{I}}_i \omega_i \quad (3)$$

$$U = \sum_i \frac{1}{2} \kappa_i \delta_i^2 + m_i \mathbf{g} \cdot \mathbf{r}_i \quad (4)$$

here $\mathbf{r}_i$ are the position vectors of the centers of mass of the 5 elements, and $\dot{\mathbf{r}}_i$ their time derivatives, m_i are the corresponding masses. The ω_i are the angular velocities of the rigid elements (only defined for $i = 1, 2$) and $\bar{\bar{I}}_i$ is the corresponding inertia matrix. Lastly, κ_i are the torsional spring constants of the four pivots and $\mathbf{g} = -g \hat{\mathbf{y}}$ is the gravitational acceleration.

Now we consider the Euler-Lagrange equation, which reads:

$$\frac{d}{dt} \left(\frac{d\mathcal{L}}{dq_j} \right) - \frac{d\mathcal{L}}{dq_j} = Q_j \quad (5)$$

where q_j are the generalized variables and Q_j are the generalized forces:

$$Q_j = \sum_i \mathbf{F}_i \cdot \frac{\partial \mathbf{r}_i}{\partial q_j} \quad (6)$$

As we will see, all quantities in eq. (3) and (4) are a function of the angle θ and its time derivative $\dot{\theta}$, only. This angle is the single generalized coordinate that determines this system. Although the other quantities are non-linear functions of θ , since $\theta \ll 1^\circ$, and we are interested in motion around the point $\theta = 0$, these functions can be linearized.

TABLE II
POSITION AND LINEAR VELOCITY FOR ELEMENTS i .

i	$\mathbf{r}_i$	$\dot{\mathbf{r}}_i$
1	$\begin{bmatrix} \cos \theta & \sin \theta \\ -\sin \theta & \cos \theta \end{bmatrix} \begin{bmatrix} x_{1,0} \\ y_{1,0} \end{bmatrix}$	$-\begin{bmatrix} \sin \theta - \cos \theta \\ \cos \theta & \sin \theta \end{bmatrix} \begin{bmatrix} x_{1,0} \\ y_{1,0} \end{bmatrix} \dot{\theta}$
2	$\mathbf{r}_A - L_2(\cos n\theta \hat{\mathbf{x}} - \sin n\theta \hat{\mathbf{y}})$	$L_2(\sin n\theta \hat{\mathbf{x}} + \cos n\theta \hat{\mathbf{y}})n\dot{\theta}$
3	-	-
4	$-L_4(\sin \theta \hat{\mathbf{x}} + \cos \theta \hat{\mathbf{y}})$	$-L_4(\cos \theta \hat{\mathbf{x}} - \sin \theta \hat{\mathbf{y}})\dot{\theta}$
5	$\mathbf{r}_A + L_5(\cos n\theta \hat{\mathbf{x}} - \sin n\theta \hat{\mathbf{y}})$	$-L_5(\sin n\theta \hat{\mathbf{x}} + \cos n\theta \hat{\mathbf{y}})n\dot{\theta}$

The positions $\mathbf{r}_i$ are dependent on θ and θ_2 only. The linearized angle θ_2 and linearized differential angles are given in eq. (7)-(10) below. Using these expressions, eq. (5) can be expressed in terms of θ only.

$$\delta_1(\theta) = \theta + \mathcal{O}(\theta^2) \quad (7)$$

$$\delta_2(\theta) = n\theta + \mathcal{O}(\theta^2) \equiv \theta_2 \quad \text{with} \quad n = 1 + n_3 + n_4 \quad (8)$$

$$\delta_3(\theta) = \theta'_3(0)\theta + \mathcal{O}(\theta^2) \equiv n_3\theta \quad (9)$$

$$\delta_4(\theta) = \theta'_4(0)\theta + \mathcal{O}(\theta^2) \equiv n_4\theta \quad (10)$$

Note that the prime denotes $d/d\theta$. The derivation of the various terms in the linearized equations can be found in appendix B.

Going back to eq. (5) we can substitute $q_j = \theta$. For now we consider a system without forcing or damping, so that $Q_j = 0$. This results in the following equation.

$$\frac{d}{dt} \left(\frac{d\mathcal{L}}{d\dot{\theta}} \right) - \frac{d\mathcal{L}}{d\theta} = 0 \quad (11)$$

Solving this equation will yield the equation of motion for the system. To solve this equation all $\mathbf{r}_i, \dot{\mathbf{r}}_i$ need to be known and are, for this purpose, tabulated in Tab. II. The angular velocities are $\omega_1 = -\dot{\theta}$ and $\omega_2 = -n\dot{\theta}$ and zero for all other i . $L_i = |\mathbf{r}_i|$ are the lengths from the center of mass of element i to A , or to O for $i = 1, 4$. There is one exception: the center of mass of the L-shaped component ($i = 1$) has both an x and a y component in the equilibrium position, which are denoted $x_{1,0}$ and $y_{1,0}$. Calculation of $\mathbf{r}_1$ furthermore requires a rotation matrix as can be seen in Tab. II.

We now write out the terms of the Lagrange function. The different components are: the linear kinetic energy,

$$\sum_{i=1}^N \frac{1}{2} m_i \dot{r}_i^2 = \frac{1}{2} (m_1 L_1^2 + m_2 n^2 L_2^2 + m_4 L_4^2 + m_5 n^2 L_5^2) \dot{\theta}^2 \quad (12)$$

182 the rotational kinetic energy,

$$\sum_{i=1}^N \frac{1}{2} I_i \omega_i^2 = \frac{1}{2} (I_1 + n^2 I_2) \dot{\theta}^2 \quad (13)$$

the gravitational potential energy,

$$\begin{aligned} \sum_{i=1}^N m_i \mathbf{g} \cdot \mathbf{r}_i = & -g[m_1(y_{1,0} \cos \theta - x_{1,0} \sin \theta) \\ & + m_2(y_A + L_2 \sin n\theta) \\ & - m_4 L_4 \cos \theta \\ & + m_5(y_A - L_5 \sin n\theta)] \end{aligned} \quad (14)$$

183 and the elastic potential energy.

$$\sum_{i=1}^N \frac{1}{2} \kappa_i \delta_i^2 = \frac{1}{2} (\kappa_1 + n^2 \kappa_2 + n_3^2 \kappa_3 + n_4^2 \kappa_4) \theta^2 \quad (15)$$

From these equations it is evident that the kinetic energy is only dependent on $\dot{\theta}$ and the potential energy is only dependent on θ , as expected. The Euler-Lagrange equation (5) for this system can now be written as:

$$\begin{aligned} & (m_1 L_1^2 + m_2 n^2 L_2^2 + m_4 L_4^2 + m_5 n^2 L_5^2 + I_1 + n^2 I_2) \ddot{\theta} \\ & + (\kappa_1 + n^2 \kappa_2 + n_3^2 \kappa_3 + n_4^2 \kappa_4 - g m_1 y_{1,0} + g m_4 L_4) \theta \\ & = g(n m_2 L_2 - m_1 x_{1,0} - n m_5 L_5) \end{aligned} \quad (16)$$

184 Note that the trigonometric functions are linearized, i.e. $\sin x \simeq \theta$ and $\cos x \simeq 1$ (for both $x = \theta$ and
185 $x = n\theta$) in the gravitational term. Also note that $y_{1,0} < 0$, so the gravitational term of the first element is
186 positive. It is interesting to see that the spring force of the flexures at A and C is multiplied by a factor
187 n^2 (which is equal to n_4^2 and about 10^3 as shown in appendix B). The above expression represents a
188 harmonic oscillator with an effective moment of inertia I , an effective torsional spring constant κ and a
189 constant offset τ_0 :

$$I \ddot{\theta} + \kappa \theta = \tau_0 \quad (17)$$

190 The constant term is $\tau_0 = g m_1 x_{1,0} + g n m_5 L_5 - g n m_2 L_2$ which are the gravitational torques. For the
191 balance to be in the equilibrium position ($\theta = 0$) it is required that τ_0 is zero. This reflects the purpose
192 of the counterweight; as its distance to the pivot A , L_5 , is variable, it can be used to balance out the
193 system by choosing L_5 such that $\tau_0 = 0$.

Now we will include the external forces. As shown in Fig. 1 there are two forces applied to the system, T the thrust operating at r_4 and $F_D = -b\dot{\mathbf{r}}_D$ the damping force, which acts on point D and is opposite and proportional to its velocity. To include these forces we now consider them as generalized forces Q_j :

$$Q_4 = \mathbf{F} \cdot \frac{\partial \mathbf{r}_4}{\partial \theta} = TL_4 \quad (18)$$

$$Q_D = \mathbf{F} \cdot \frac{\partial \mathbf{r}_D}{\partial \theta} = -bn^2L_D^2\dot{\theta} \quad (19)$$

194 where $\mathbf{r}_D = \mathbf{r}_A - L_D(\cos n\theta \hat{\mathbf{x}} - \sin n\theta \hat{\mathbf{y}})$. The new equation of motion then becomes:

$$I\ddot{\theta} + \beta\dot{\theta} + \kappa\theta = \tau(t) + \tau_0 \quad (20)$$

195 with $\beta = bn^2L_D^2$ being the torsional viscous damping factor, and $\tau = TL_4$ the torque due to the thrust.
196 It is now perfectly clear that in first approximation the balance behaves as a damped harmonic oscillator.

The equation of motion for the damped harmonic oscillator can be normalized as follows.

$$\ddot{\theta} + 2\zeta\omega_0\dot{\theta} + \omega_0^2\theta = \frac{\tau}{I}, \quad \zeta = \frac{\beta}{2\sqrt{\kappa I}}, \quad \omega_0 = \sqrt{\frac{\kappa}{I}} \quad (21)$$

197 Here we have introduced the damping ratio ζ and the natural angular frequency ω_0 .

198 The other property of interest is the stiffness k of the system: the relationship between the measured
199 displacement y and the applied thrust T , such that $T = ky$. In a steady state the equation of motion
200 reduces to:

$$\kappa\theta = TL_4 \quad (22)$$

201 Combining this with eq. (8) and (30) (from appendix A) which we can simplify as $y = L_{AE}\theta_2$ we can
202 find an expression for k .

$$k = \frac{\kappa}{nL_4L_{AE}} \quad (23)$$

203 To have a response that closely follows the step input of the applied thrust and to prevent the system
204 from oscillating eternally, a damping force is required. Generally damping ratios of $0.4 < \zeta < 0.8$
205 give a good step response [17]. The damping is realized by means of Eddy current damping, which is
206 contact-less and well suited for vacuum.

The principle is as follows. When a conductor moves through a magnetic field, Eddy currents are induced inside the conductor. Due to the magnetic field, the moving charges that make up these currents experience a Lorentz force which is proportional to and in opposite direction of the velocity. The induced current density and the resulting force are:

$$\mathbf{J} = \sigma(\mathbf{E} + \mathbf{v} \times \mathbf{B}) \quad \mathbf{F}_d = \int_V \mathbf{J} \times \mathbf{B} dV \quad (24)$$

Fig. 2. Sketch of electric and magnetic fields and current densities in they eddy current damper. Note that the currents in reality flow in closed loops.

For a thin plate of thickness δ we can approximate the problem in 2D, assuming the current density and magnetic field constant over this dimension (z). For a velocity in the y -direction $\mathbf{v} = v_y \hat{y}$ and a homogeneous axial magnetic field in the z -direction $\mathbf{B} = B_z \hat{z}$, with pole projection S . Ignoring the electric field for now, the Lorentz force is then proportional to:

$$F_d \propto -\sigma v S \delta B_z^2 \quad (25)$$

A sketch of the fields and current density is shown in figure 2. Due to the sharp gradient in the magnetic field at the edges of the pole projection area, charge accumulation occurs here. This charge results in an electric field perpendicular to the direction of motion of the conductor, in a way similar to the Hall-effect. Inside the pole projection area this electric field is opposite the component $\mathbf{v} \times \mathbf{B}$ and here effectively limits the induced current density. Outside the pole projection area the electric field is the sole reason for the induced current density. Knowing both $\mathbf{B}$ and $\mathbf{E}$ the current density $\mathbf{J}$ and correspondingly the force can be solved for. Furthermore, to ensure that the current density normal to the boundaries of the conductor is zero the method of image currents can be used. This problem was solved analytically by Bae [30] for square magnets. For circular magnets the integrals involved are rather complicated and it's

more convenient to solve it with commercial FEM software. We modeled the damping system of the thrust balance using Comsol's magnetic and electric field (mef) module. The system was modeled using the dimensions and properties detailed in section III-C, the results are reported in section V.

III. TECHNICAL IMPLEMENTATION

A. Structure

The thrust balance structure as shown in Fig. 1 can also be recognized in Fig.3 (although mirrored). Point O and A are the pivots, item 2 in Fig. 3. These pivots are anchored to a large aluminium plate (top white plate in Fig. 3). (As opposed to pivots B and C , not labeled in this figure, which are freely moving as was explained in section II). This plate is placed on top of another aluminium plate (grey, bottom). The relative attitude of the top plate with respect to the bottom-plate is controlled by the leveling system described in the next section.

The bottom plate is suspended from the roof of the vacuum chamber (not shown for clarity). It also includes the interface for the power lines, gas lines and cooling water lines (items, 14, 15, 16 respectively).

The secondary arm and the intermediate link are custom machined aluminium parts. The primary arm is a combination of a machined aluminium part, an extruded 30 mm x 60 mm extruded aluminium profile and a gun-drilled cold plate (item 9). The cold-plate doubles as the thermal and the mechanical interface for the thruster. The pivots are C-flex type G10 for the main pivot (2 bearings at O) and D10 for the other pivots (2 bearings at A , 1 bearing each at B, C).

The system further includes a calibration rig (items 7,8), liquid metal connectors (item 13) a viscous damping system (item 5) and an optical displacement sensor (item 6) which will be elaborated on in the rest of this section.

B. Leveling system

To be able to align the surface normal of the upper plate with the gravity vector the system uses two motorized screw-jacks (item 12 in Fig. 3) in conjunction with a two-axis inclinometer. The screw-jacks are fixed to the upper aluminium plate with the lead-screw projecting down, towards the bottom plate and are connected to the bottom plate by means of spherical joints (item 19 in Fig. 3). Both joints allow for 3 rotational degrees of freedom (DoF) and one of the joints allows for one additional (linear) DoF. The screw-jacks allow for varying the distance between both plates, separately at each point of contact. Both plates are connected at a third point by a rod of a fixed distance ending in a 4 DoF (3 rotational, 1 linear) joint. All joints are implemented using custom, lubricant-less bearings. This arrangement allows for controlling the pitch and roll of the top plate with respect to the bottom plate. The pitch is changed

by co-rotating both screw-jacks, i.e. by generating a common change in the distance between the two plates at each contact point, while roll is changed by counter-rotating, i.e. by generating a differential change. Control of the screw-jacks is informed by the inclinometer that measures the angle with respect to the vertical.

C. Damping System

The implementation of the damping system consists of a conductive plate attached at the end of the secondary arm. Here the displacement (and hence the velocity) is the largest. Furthermore, including the damping force on the secondary arm takes advantage of the mechanical amplification. As can be seen from eq. (19) the damping scales with $n^2 \sim 10^3$. In this way, up to critical damping can be achieved for any thruster, with only a modest damping system.

Fig. 3. CAD drawing of the thrust balance design. 1) Primary arm. 2) Pivots O and A 3) Secondary arm. 4) Counterweight. 5) Eddy current damper. 6) Displacement sensor. 7) Voice coil. 8) Load cell. 9) Water cooled mounting plate. 10) Water cooled electronics plate. 11) Propellant line mounting point. 12) Motorized screw jack for auto-leveling. 13) Liquid metal connectors. 14) Power supply user interface (UI). 15) Propellant line UI. 16) Cooling water UI. 17) Power supply thruster interface. 18) Propellant supply thruster interface. 19) Pivot points for auto-leveling system.

The magnetic field is applied by two aligned permanent magnets on each side of the plate. The damping force scales with the conductivity of the metal. Of all affordable metals, copper has the highest conductivity of any metal, but it also has a high mass density. The added mass, with a considerable arm ($L_{AE} \sim 0.5\text{m}$), leads to an undesirable increase in the moment of inertia of the second arm. Pure aluminium has 60% the conductivity of copper but is only 30% as dense, making it the preferred option. The reported conductivity of the alloy that was used, Al5745, is $2.04 \cdot 10^7\text{S/m}$. However, when we measured the conductivity it was found to be closer to $1.59 \cdot 10^7\text{S/m}$.

The conductive plate is made of aluminium and has the following dimensions: 30 mm x 60 mm x 2 mm. The two cylindrical magnets are 20 mm diameter, 5 mm thick, NdFeB grade N42 (maximum energy product 42 MGOe) magnets with a remanence of 1.3T and are spaced a distance s apart. To be able to vary the magnetic field strength, and thereby the damping, s is variable by means of small lead-screws.

D. Displacement Sensing

The quantity that must be measured is the displacement of point E (in Fig. 1). There are many different displacement sensors available: inductive, capacitive, interferometry, triangulation etc. For this particular balance we wanted a sensor that was impervious to EMI as the strong RF fields of a Helicon thruster are known to wreak havoc on (sensitive) electrical systems. The current design is outfitted with a confocal chromatic sensor CCS-Prima from STIL.

Confocal chromatic sensors [31] consist of a white light source, a system of lenses (the ‘optical pen’) and a spectrometer. The white light is refracted by the optical pen; the focal distance is wavelength dependent. An object will reflect the wavelength with a focal distance corresponding to the position of the reflecting surface. By measuring the wavelength of the reflected light with a spectrometer, and given the relationship between the focal distance and wavelength, the distance of the object to the optical pen can be inferred.

The optical pen is mounted on the top plate, pointing upwards, about 25 mm distance from the horizontal arm. This distance can be controlled by a vacuum-rated manual linear translation stage. The optical pen is connected to the light source and spectrometer by an optical fiber, passing the vacuum/ambient barrier by means of an optical feed-through. Both the emitted and reflected light are coupled through the same fiber. The in-vacuum path only consist of passive elements (fiber, optical pen) while the light source, spectrometer and other electronics reside outside of vacuum chamber far away from the RF fields of the thruster. The range and resolution can be changed by changing the optical pen. For this particular application we are using the CL5-MG20 optical pen which has a measurement range

of 12 mm and a resolution of $0.4 \mu\text{m}$. It is unaffected by a tilt of the reflecting surface of up to 14° far exceeding the maximum deflection angle $\theta_2 < 3^\circ$.

E. Calibration System

Since the stiffness of the thrust balance is dependent upon the mass of the thruster, additional stiffness from gas and cooling lines as well as thermal drifts, it is important that the thrust balance can be calibrated repeatedly, preferably in-situ without breaking the vacuum. Calibration is done by applying a known force while measuring the displacement, for a range of loads spanning the desired measurement range.

We have chosen a calibration system based on a voice coil [32]. Voice coils consist of a small solenoid and a permanent magnet. The interaction of the magnetic fields of the solenoid and the magnet results in a repulsive force, linearly proportional to the current applied to the solenoid. The force per current ratio is constant only over a short part of the stroke length, and dependent on the relative position of the solenoid and the magnet. The actual force per current ratio could very well differ from the one specified by the manufacturer. Calculating the applied force from the applied current using the specified force per current ratio can therefore introduce an error. The voicecoil used is a Moticont LVCM-010-013-10 with a reported force per current of 0.29 N/A .

In the current design, the magnet of the voice coil is instead mounted on a calibrated load cell. This allows for directly measuring the applied force, circumventing the uncertainties in the force per current ratio. The load cell used is a custom version of the Novatech F329 with an extended range, up to 200 mN. Although the load cell is calibrated, its sensitivity can change slightly under loading, especially when loaded horizontally.

F. Gas-line & Power Feeds

The power and propellant supplies are generally separated from the thruster in an experimental setting; therefore both power supply and gas lines are needed between the fixed and mobile parts of the thrust balance. For the power lines, we opted for 4 supply lines which could power up to 2 electromagnets or for example the anode, keeper and the heater of a HET. For this purpose we also included a secondary gas line to, in the future, be able to operate thrusters that need a hollow cathode for neutralization, such as a HET or GIT.

The electromagnet on the HPTx is fed with up to 30 A, requiring at least gauge 10 wires ($2.6 \text{ mm } \varnothing$) according to MIL-STD-975. We settled for $3 \text{ mm } \varnothing$ enamelled copper cables that can handle temperatures up to 200°C . Due to their considerable diameter these wires would add excessive stiffness to the system. To mechanically decouple the power lines from the system we included liquid metal connectors (item 13,

Fig. 3). These consist of a metal receptacle filled with a liquid metal and metal rod partially submerged in the metal liquid. The receptacle is mounted on the fixed part of the thrust balance while the rods are mounted on the primary arm. As the viscous drag is negligible, the rod can move freely with respect to the receptacle while maintaining the electrical contact.

In the past, mercury was used as the liquid metal but this is undesirable because of its toxicity. Instead we used galinstan, an eutectic mixture of gallium, indium and tin. It is liquid at room temperature $T_{fus} = -19^\circ\text{C}$, has an electrical conductivity of $3.46 \cdot 10^6$ S/m and a vapour pressure of less than 10^{-8} mbar at 500°C meaning, that it is suitable for testing in high vacuum. The problem with Galinstan is that it embrittles aluminium and other metals. Certain stainless steels (SS) and refractory metals have good resistance against embrittlement.

Although stainless steel (SS) is the most economic option, to accommodate the high currents we chose molybdenum for its higher conductivity $1.87 \cdot 10^7$ S/m, about 13 times that of SS. The receptacles are 15 mm diameter, 23 mm long cylinders with a 12 mm diameter, 12 mm deep cylindrical cavity.

We simulated the resistive dissipation in the liquid to see whether any notable heating would occur. The total dissipation in the liquid amounts to only 12 mW while the dissipation in the receptacle is little over 14 mW and that in the rod is 175 mW.

To minimize the impact of the gas-lines and to ensure that their impact is linear and limited to an increase in the overall stiffness we opted for thin walled PTFE (polytetrafluoroethylene) tubes of 6 mm outer diameter and 4 mm inner diameter. These tubes are mechanically fixed at two ends: at the top plate and at the cold plate and run parallel to the vertical part of the primary arm. In this way their contribution to the overall stiffness can be approximated with the Euler-Bernoulli beam bending theory. The equivalent torsional stiffness of two 480 mm tubes with a Young's modulus of 0.6 GPa is found to be 0.40 Nm/rad. As we will see later, this is less than 1% of the total torsional stiffness of the system. The overall stiffness of the balance was also calibrated before and after installing the gas-lines and no significant increase in the stiffness was found.

G. Thermal Management

The HPTx prototype operates in the 0.3-1 kW range with an efficiency of around 10 %. The electromagnet has a resistance of about 0.8Ω resulting in an Ohmic heating power of 720 W at 30 A. However, this increases with time due to the resistance increasing with temperature. The thermal loads to the balance are therefore of the order of 1-1.5 kW; the need for thermal management is obvious. Moreover, with such high heat fluxes radiative cooling and/or a thermal block would not suffice. The

equilibrium temperature would far exceed the rated temperatures of some of the thruster components. Instead we have opted for water cooling.

To realise this, three separate water cooled cold-plates are installed: one integrated in the thruster mounting plate (item 9, Fig. 3), a second one on the top plate (item 10, Fig. 3), to cool the electronics and a smaller one passing by the liquid connectors. All three cold-plates are connected in series by means of 10 mm $\varnothing$ SS and PU (polyurethane) tubing. For a total cooling line length of about 10 m and a differential pressure of 4.5 bar we estimate a flow rate of 17.5 L/min and Reynolds number of $4.6 \cdot 10^4$ implying turbulent flow. The thermal resistance of the mounting cold plate is about 5.0 °C/kW, that of the electronics plate 3.9 °C/kW and for the liquid connector cooling 24.8 °C/W providing sufficient cooling.

To minimize the effect of the cooling lines to and from the cold plate, the flow direction is kept perpendicular to the plane of motion of the balance. In this way the thrust balance will not be sensitive to any reaction forces of the water on the cooling lines. For that reason the cold-plate is also symmetric w.r.t. the centre line. To reduce the effect on the overall stiffness, the cooling lines run in semi-circular loops on each side of the centre line, from the top plate to the cold plate and back. They are made of 12 mm outer diameter, 10 mm inner diameter PU tubing. The overall stiffness of the balance was also calibrated before and after installing the cooling lines and only a small increase on the stiffness was found, on the order of a few percent.

The problem with water cooling is that the turbulent flow might cause vibrations which introduce noise into the measurement. Although water cooling is used in the shroud (an immobile part) of the thrust stand of [12], as far as we are aware there is no thrust balance design with water cooling of the mobile part(s). We found that the water cooling indeed introduces noise, increasing the RMS noise level from 0.5 μ m to 2.9 μ m. However, it also reduced the thermal drifts of the thrust balance and prevented both the thruster and balance from overheating.

H. Data Acquisition and Control

The thrust balance is controlled by an on-board micro-controller. This reduces the amount of electrical feed-through connections (not including those for the thruster) to a minimum, 2 pins for power and 2 for data.

The electronics are exposed to the vacuum environment. Care is taken to avoid the use of electrolytic capacitors which can lose their electrolyte over time due to evaporation. To prevent overheating the PCBs are mounted on the aforementioned cold plate. A metal cover is placed over the electronics to prevent electromagnetic interference (EMI).

Fig. 4. Block diagram of data acquisition & control system.

The electronics are based on two Arduino micro-controllers and are divided over two PCBs, the main board containing the master Arduino Due and a secondary board with the slave Arduino Nano. The secondary board is mounted on top of the thruster mounting plate and further contains an accelerometer (ADXL355), 8 PT1000 resistance temperature detector (RTD) signal conditioners and a variable current source to power the voice coil of the calibration system. The main board also contains an accelerometer, 8 PT1000 signal conditioners as well as a dual axis inclinometer (ADIS16209), 3 stepper motor controllers and a signal conditioner for the load cell. Master and slave are connected by 6 wires using the SPI protocol. The master Arduino is connected to a PC by an RS485 to USB adaptor.

The stepper motors drive the screw-jacks mentioned in section III and together with the inclinometers allow for control of the pitch and roll of the top plate such that it can be maintained level with the local horizontal at all times. The accelerometers allow for measuring ambient vibrations which can be used to enhance the post-processing. The RTDs are used to track thermal drifts of various parts of the thrust balance. A block diagram of the full data & acquisition system can be seen in Fig. 4.

IV. EXPERIMENTAL SETUP

To validate the thrust balance performance various measurements were performed, including some preliminary measurements using the HPTx. Ideally the thrust measurements would be compared to those of an already validated thrust balance. However, these are not yet available, mainly because most existing thrust balances are not compatible with the HPTx platform, which is one of the main drivers for the current work. What is available however, are combined probe measurements that can give an estimate of the thrust and specific impulse. These measurements estimate a thrust in the order of 5-8 mN.

A. Vacuum Facility

The vacuum facility that houses the thrust balance is a 3.5 m long, 1.5 m diameter stainless steel vacuum chamber at the EP2LAB at UC3M. The chamber has two magnetically levitated turbo-molecular pumps and three cryo-panels. The ultimate pressure is $5 \cdot 10^{-7}$ mbar; the background pressure with 10 sccm of Xenon is about $1.8 \cdot 10^{-5}$ mbar.

B. HPTx Prototype

The HPTx is an experimental platform developed by EP2 (jointly with SENER Aeroespacial). It is a laboratory breadboard model that allows for several operational parameters to be varied among which: RF power, mass flow rate, magnetic field. It consists of a solenoid with about 50 G/A that can handle currents of up to 30A; a 25 mm inner diameter, 120 mm long quartz tube; a ceramic injector at the back of this tube and a half-turn helical antenna fed by an RF power generator operating at a frequency of 13.56 MHz. Between the power generator and the thruster is a matching circuit to reduce power reflections due to impedance mismatch. A schematic of the prototype can be seen in Fig. 5 and the operational parameters are tabulated in Tab. III. More details on the HPTx and its predecessors can be found in [28], [29]. For the purpose of validating the thrust balance it is important to note that the thruster weighs 5.2 kg and is mounted to the balance using a mechanical interface that weighs 1.7 kg.

V. RESULTS

A. Validation

To validate the model we simulated the response for our particular balance and compared it to the measured response. All relevant properties and dimensions of the balance are tabulated in Tab. IV and V. The moments of inertia and the masses of all elements are obtained from a CAD model of the balance. Note that the mass of the primary arm includes the 1.7 kg cold-plate and that the mass of the thruster

Fig. 5. Schematic of the HPTx setup.

TABLE III
HPTX OPERATIONAL PARAMETERS.

parameter	nominal	range	unit
RF power	450	300-600	W
B-field	1000	0-1500	G
mfr, Xe	10	5-30	sccm
mfr, Ar	20	10-50	sccm

here includes the 1.7 kg mechanical interface. The resulting center of mass is almost aligned with the vertical part of the arm; $x_{1,0} = 2.0$, $y_{1,0} = -375.0$ mm.

For the dimensions in Tab. V the linearization (see. appendix B) yields the following values: $\theta'_3(0) = -1$ which means that $n = \theta'_4(0) = 31.0$. The result $n = 31.0$ is also found using the static analysis in appendix

TABLE IV
MASS AND MOMENT OF INERTIA OF ALL ELEMENTS i

i	m_i [kg]	L_i [mm]	I_i [kg·m ²]
1	3.626	375.0	$150 \cdot 10^{-3}$
2	0.237	116.9	$6.86 \cdot 10^{-3}$
3	-	-	-
4	6.9	647.0	-
5	0.271	97.4	-

TABLE V
DIMENSIONS OF THE SCHEMATIC THRUST BALANCE.

Point	x [m]	y [m]	κ [N·m/rad]
O	0.000	0.000	0.19160
A	0.300	0.045	0.02330
B	0.310	0.000	0.01165
C	0.310	0.045	0.01165
D	-0.185	0.045	-
E	-0.120	0.045	-
M	0.000	0.674	-

431 A, confirming the accuracy of the linearization.

432 Using equations (16) and (17) and the data in Tab. V and IV, we can calculate the effective moment
433 of inertia $I = 13.0$ kg·m², and the effective torsional stiffness $\kappa = 47.1$ N·m/rad of the unloaded thrust
434 balance. This results in a natural frequency of 0.30 Hz for the pendulum motion, equivalent to a period
435 of about 3.3 seconds. When loaded with the HPTx the results are 15.9 kg·m², 90.9 N·m/rad, 0.38 Hz
436 and 2.39 s.

To validate the dynamic response of the designed thrust balance, Fig. shows 6 the calculated and measured response of the thrust balance when excited with a stepped load. The thrust balance was excited by the voice coil by stepping the applied current nearly instantaneously from zero to some constant value. The response was normalized and centred on $t = 0$, when the step is applied, and then fitted using the step response function for an under-damped harmonic oscillator.

$$A(t) = 1 - e^{-\zeta\omega_0 t} \left[\frac{\sin \left(\sqrt{1 - \zeta^2} \omega_0 t + \cos^{-1} \zeta \right)}{\sqrt{1 - \zeta^2}} \right] \quad (26)$$

Fig. 6. Comparison of calculated and measured step response. Time dependent normalized amplitude A as a function of time t .

The resulting values are $\omega_0 = 2.56$ rad/s corresponding to 0.41 Hz and $\zeta = 0.57$. The plot of the model is generated using the same value for ζ . The relative difference in frequency between the measured and calculated response is about 8%.

The stiffness of the balance changes linearly with thruster mass. The static model, using eq. (38) and the dimensions and stiffness from Tab. V predicts a stiffness of $k = 0.753 \cdot M + 4.06$ mN/mm, ranging from about 4.8 N/m for a 1 kg thruster to about 42 mN/mm for a 50 kg thruster. For the HPTx weighing 5.2 kg, plus 1.7 kg of the cold plate and another 1.7 kg of the mechanical interface, a stiffness of 10.5 mN/mm is expected. Using eq. (23) our model predicts a stiffness of 10.4 mN/mm. Both values agree within less than 0.1%.

To validate the model of the damping system we simulated the damping coefficient b for varying magnet spacing s with Comsol's magnetic and electric field (mef) module using the parameters described in section III-C. We also measured the damping ratio ζ by fitting eq. (26) to the measured step response

for the same spacing s . For each s we performed two square pulses, fitting eq. (26) on the positive edge and also on the negative edge (after multiplying by -1), resulting in 4 points per value of s . The damping coefficient b can be converted to ζ using:

$$\zeta = \frac{(nL)^2}{2\sqrt{\kappa I}} \cdot b \quad (27)$$

which follows from eq. (19) and (21). The results are plotted in Fig. 7. The experimental data (ζ) is scaled using eq. (27) with 2.97 s/kg (thruster) and 4.08 s/kg (no thruster). From the figure it is clear that the FEM model accurately predicts the damping ratio. As a control we also measured the magnetic field in the centre between the magnets for varying magnet spacing and compared it to the simulated value; this was also found to be in good agreement ($R^2 = 0.995$ and an RMSE (root mean squared error), normalized to the mean, of 2%). The damping system is capable of providing a damping ratio from 0.1 to more than 1 which is more than sufficient for practical application. We did find a slight difference between the results obtained from the positive and the negative edges, most clearly seen at $s = 10$ mm of the blue curve in Fig. 7. This is probably due to the fact that for the negative edge the plate does not start centered on the magnet. Regardless, this minor difference does not affect the previous conclusions.

The optimal damping ratio is that for which the settling time is minimal. The settling time is defined as the time it takes for a step response to fall within 2% of the final value. Using the parameters derived from our Lagrangian model we simulated the settling time for different values of the normalized damping coefficient ζ , resulting in the plot of Fig. 8. The optimal damping ratio is about $\zeta = 0.78$ with a settling time of 1.8s. We also inferred the settling time and damping ratio from the measured response of the system to a step input for various separation distances of the magnets which are the red circles in Fig. 8. As is apparent the measurement and simulation agree well. These results were obtained without a thruster mounted on the balance. Including the thruster mass in the model yields similar results, the optimum ζ is still 0.78 and the settling time is slightly lower at 1.5 seconds. The discontinuities in the graph are due to the definition of the settling time and can be explained as follows: for a given value of ζ there is a point where the step response $A(t)$ will have crossed the lines $A = 0.98$ or $A = 1.02$ for the last time. This is the settling time. When ζ is increased there will be a point at which this ultimate crossing will occur one half-period earlier, the settling time will then be reduced discontinuously by a half-period.

B. Calibration

Calibration of the system is necessary to obtain the actual stiffness of the balance which is needed for post-processing measurement data. Comparison of the calibrated stiffness with the stiffness predicted by the model also provides another venue for validation.

Fig. 7. Comparison of calculated and measured damping ratio. The model output is scaled using eq. (27) with 2.97 s/kg (thruster) and 4.08 s/kg (no thruster).

For the HPTx a maximum thrust level of around 8 mN was expected and therefore we intended to calibrate up to 20 mN. The force per current ratio of 0.29 N/A implies that the current applied to the voice coil should be varied from 0 to 70 mA. To verify this we first performed a linear fit of the applied current versus measured force of the voice coil and found instead a force per current ratio of 0.35 N/A a 21% difference. This significant difference could indicate that the calibration of the voice coil is affected by the horizontal orientation and the lateral loading due to the voice coil magnet. This could explain at least part of the difference in the predicted and measure thrust balance stiffness. Despite the different force per current value we maintained the maximum current at 70 mA for a calibration range of slightly less than 25 mN.

The calibration protocol was as follows: 0 to 70 mA is applied in steps of 10 mA. Each step lasts for about 20 seconds to allow the balance to settle and to be able to average sufficient data points in one

Fig. 8. Settling time t_s as a function of damping ratio ζ .

step. To account for drifts, each current step is followed by a zero step that also lasts 20 seconds. In this way each step has its own zero value. Furthermore, to check for hysteresis the procedure is repeated from 70 mA down to 0 mA. The displacement data is sampled at 10 Hz and the load cell data is sampled at 2 Hz. The resulting data is shown in Fig. 9. This data was taken with the HPTx mounted on the balance and with the cooling water running. The data is processed as follows. At each step as well as the subsequent 'zero'-step, 150 displacement sensor data points and 30 load cell data points are taken. The displacement data is then averaged every 5 points to reduce it to 30 points. The zero values are subtracted from the preceding 'high' values. We then use a least squares fitting procedure to obtain a linear fit of the data. This results in the plot shown in Fig. 10, where we plotted the displacement versus the force, as well as the linear fit. In the upper window of the plot the standardized residuals are shown. These residuals should be normally distributed around zero. It can be seen that there is a pattern in the residuals. This is indicative of non-linearity. When doing a quadratic fit the residuals were normally distributed.

Fig. 9. Raw calibration data. Applied force F and displacement y as a function of time t .

We quantified the non-linearity as the largest deviation of the quadratic fit with respect to the linear fit, relative to the full scale which resulted in a 0.4% non-linearity error. Since this uncertainty is so small we find the use of the linear fitting justified as it yields a single calibration constant that is easy to use.

The inverse of the slope of the fit is $k_{cal} = 23.21 \pm 0.02$ mN/mm. However, this is the stiffness for forces applied at the position of the voice coil. This needs to be scaled by the ratio between the distance from O to the voice coil (368 mm) and the distance from O to the point of action of the thrust force L_T (generally, but not necessarily: $L_T = L_4$) which is 674 mm; this ratio is 0.546. The stiffness of the thrust balance is therefore 12.7 mN/mm which is about 20% higher than that predicted by either model.

In this particular calibration the relative uncertainty on the calibration constant is about 0.07%. In general we have found that the relative uncertainty is $\leq 1\%$.

Fig. 10. Processed calibration curve. Top: standardized residuals. Bottom: displacement y as a function of applied force F .

C. HPTx Thrust Measurements

In this section we report one of the first thrust measurements of the HPTx using the new thrust balance. In Fig. 11 the output of the displacement sensor during a firing of the HPTx is shown. The operating point was 15 sccm of Xenon, 450 W of RF power and a magnetic field of 1500 G. The blue line shows the raw data; as can be seen there is a thermal drift from the moment the magnetic field is turned on, which in first approximation is linear. It was found that subtracting the slope of the line connecting the point of the curve, right before turning on the magnetic field (40 s) and the point right after turning it off again (150 s) removed most of the drift as evidenced by the red line.

In Fig. 11 several events can be distinguished. Firstly at around 40 seconds the magnetic field is turned on, resulting in a negative thrust. This can be explained by the magnetic force between the solenoid and the magnet of the voice coil. A linear relationship was found between the magnetic field of the solenoid

Fig. 11. Measurement of the HPTx operating at 450W, peak magnetic field of 1500G and 15 sccm of Xenon. Displacement y as a function of time t .

and the negative thrust generated, which seems to support this hypothesis. It is important to note that this negative thrust level is considered the ‘zero level’ of the measurement.

At about 64 seconds the propellant flow is turned on leading to a small increase in the thrust value, the so-called cold gas effect.

Little after 86 seconds the RF power is turned on starting the plasma discharge. This is the largest contribution to the thrust. A small overshoot can be seen after which the thrust stabilizes. The increased noise possibly comes from the cooling system. Currently the water cooling circuit for the thrust balance and that of the RF generator are both supplied from the facility water lines. The thrust balance cooling water runs continuously but the RF generator has a valve which opens/closes when the RF is on/off. Although they are connected at outlets several meters apart we suspect that opening and closing the valve causes pressure waves that induce vibrations in the thrust balance. The vibrations are worse when the

valve closes (RF OFF), probably due to the induced water hammer, at 132 s. Then subsequently at 145 s and 166 s propellant flow and magnetic field are turned off. Despite the oscillations when turning off and on the thruster the thrust balance signal is appreciably more stable than that of other thrust balances running a HPT [33], [34], [35].

To obtain the net displacement we first obtain the zero level by taking the mean of a 150 data points of the (detrended) data set, after turning on the magnetic field and before turning on the propellant flow where the curve is 'flat', between 45 and 60 seconds; we then obtain the mean of 330 data points of the steady-state part of the data between turning on and off the RF power, between 92 and 115 seconds. The mean values are denoted by the dash-dotted lines in Fig. 11. The difference between these values is the net displacement, 408 μm . Given the calibration constant of 12.7 mN/mm this is equal to 5.1 mN.

D. Uncertainty Estimate

In this section we report the analysis of the experimental uncertainty estimation. The final thrust value is the product of the following variables:

$$T = y \cdot k_{cal} \cdot \frac{L_{vc}}{L_T} \quad (28)$$

where $L_{vc} = 368$ mm is the distance from the voice coil to O and y the displacement measured by the sensor. All of these quantities come with an uncertainty. The uncertainty in L_T and L_{vc} are those in the measurement of both lengths and is $\sigma_{L_T} = \sigma_{L_{vc}} = 1$ mm. The uncertainty in k_{cal} is obtained concurrently with k_{cal} and for this particular measurement is $\sigma_{k_{cal}} = 0.1$ mm/mN. According to [17] the uncertainty in y , σ_y , can be estimated from the calibration data. However, we are of the opinion that in our case this underestimates the uncertainty. Therefore, we use the root sum of squares of the standard deviation of the zero level and thrust level. In this measurement of Fig. 11 this results in an uncertainty of 8 μm . The uncertainty in the thrust measurement is calculated from the root sum of squares of all the relative uncertainties.

$$\frac{\sigma_T}{T} = \sqrt{\left(\frac{\sigma_y}{y}\right)^2 + \left(\frac{\sigma_{k_{cal}}}{k_{cal}}\right)^2 + \left(\frac{\sigma_{L_T}}{L_T}\right)^2 + \left(\frac{\sigma_{L_{vc}}}{L_{vc}}\right)^2} \quad (29)$$

For the measurement of Fig. 11 the relative uncertainty in the thrust measurement is 2% which is dominated by the uncertainty in the displacement measurement. The thrust value is then $T = 5.1 \pm 0.1$ mN.

VI. DISCUSSION AND CONCLUSIONS

In this paper we have presented a mechanically amplified thrust balance designed specifically for helicon thrusters. This design has several advantages such as increased sensitivity; a more compact form

factor; a displacement sensor mounted on the main structure, far away from the source of disturbances; among other advantages mentioned throughout this work.

A dynamic analysis using Lagrangian mechanics has been presented. This model reproduces the results of the static analysis of Ref. [18]. On top of that it can be used to calculate the time-response of the thrust balance and analyze the required damping force.

The model predicts an angular magnification of $31^\circ/^\circ$ and a stiffness of 9.65 mN/mm in agreement with the static model. The model shows that the system is equivalent to a damped harmonic oscillator and predicts a natural frequency of 0.38 Hz when loaded with a 5.2 kg thruster (6.9 kg including the 1.7 kg mounting structure), within 8% of the measured value of 0.41 Hz.

The thrust balance includes a viscous damping system based on eddy currents, with a variable damping ratio. The damping system was modeled using Comsol and the calculated damping ratio was in good agreement with the measured value. It was shown that a damping ratio of 0.78 is optimal resulting in the lowest settling time, 1.8 s.

The calibration system is based on a voice-coil mounted on a load-cell to avoid uncertainties in the voice coil's force constant. When verifying the force constant using this setup it was found that it is 21% higher than specified in the data sheet. This could indicate that the load cell calibration is affected by the fact that it is loaded laterally. If the load cell indeed over-predicts the force it could explain the difference between the model and the measurement. This needs be verified e.g. by cross-comparison with a different calibration system. Coupling between the solenoid of the helicon thruster and the magnet of the voice coil was observed, which poses a minor inconvenience during thrust measurements. The stiffness of the thrust balance loaded with a 5 kg thruster and corrected for relative position of the calibration system and the thruster is 12.7 mN/mm, almost 20% higher than expected from the models. However, correcting the stiffness with the ratio between the force per current ratio of the voice coil as reported by the manufacturer and the measured one, yields a stiffness of 10.4 mN/mm in exact agreement with the prediction of our model. This lends credence to the hypothesis that the load cell sensitivity has increased 20%, probably due to loading and orientation, and thereby over-predicting the stiffness.

The design further includes liquid metal connectors to mitigate the contribution of the power cables to the overall stiffness, on-board electronics to minimize electrical feed-through connections, leveling system to keep the balance aligned with the horizontal, and a confocal chromatic displacement sensor with 0.4 μm resolution and 12 mm range that is impervious to EMI. Another novelty is the active water cooling of the thruster mounting plate. This acts as a thermal sink for the thruster shielding the thrust balance from the heat load. We have shown that the water cooling increases the noise by a factor 6 from 0.5 μm to 2.9 μm (equivalent to 6 and 37 μN). Currently the water cooling is supplied from the facility's

water net which does not have a regulated outlet pressure. This could be improved with a closed loop, dedicated chiller with more accurate pressure control.

The thrust balance performance has been validated by testing the HPTx platform. A displacement of $408 \pm 8 \mu\text{m}$ was measured operating the thruster at 15 sccm of Xe, 450 W RF power and a magnetic field of 1500 G. This corresponds to a thrust level of 5.1 ± 0.1 mN. The relative uncertainty on the thrust measurement was 2% in this particular case and dominated by the noise induced by the RF generator valve. It is expected that this can be reduced to $\leq 1\%$ by decoupling the water cooling system.

APPENDIX A STATIC MODEL

$$y = 2L_{AE} \sin\left(\frac{\theta_2}{2}\right) \quad (30)$$

$$\theta_2 = \theta_1 + \theta_3 + \theta_4 - \pi \quad (31)$$

$$\theta_1 = 2 \sin^{-1}\left(\frac{x}{2L_4}\right) \quad (32)$$

$$\theta_3 = \cos^{-1}\left(\frac{L_{OB}^2 + L_{AB'}^2 - L_{OA}^2}{2L_{OB}L_{AB'}}\right) \quad (33)$$

$$+ \cos^{-1}\left(\frac{L_{AB'}^2 + L_{BC}^2 - L_{AC}^2}{2L_{AB'}L_{BC}}\right) \quad (34)$$

$$\theta_4 = \cos^{-1}\left(\frac{L_{AC}^2 + L_{BC}^2 - L_{AB'}^2}{2L_{AC}L_{BC}}\right) \quad (35)$$

$$L_{AB'}^2 = L_{OB}^2 + L_{OA}^2 - 2L_{OB}L_{OA} \cos(\gamma + \theta_1) \quad (36)$$

$$\gamma = \cos^{-1}\left(\frac{L_{OA}^2 + L_{OB}^2 - L_{BC}^2 - L_{AC}^2}{2L_{OA}L_{OB}}\right) \quad (37)$$

where $\gamma \equiv \angle AOB$.

The variables L_i denote the length from the centre of mass r_i to its corresponding point of rotation, while L_{jk} is the length between the points j, k .

Note that in the previous equations, it is implicit that θ_3, θ_4 are initially right angles. It can be seen that y is a function of θ_2 which is in turn a function of θ_1 , which is a function of x .

The stiffness of the thrust balance can be obtained from a force balance at all four pivots (O, A, B, and C in Fig. 1). The resulting relationship between the applied thrust T and the angle θ_1 is given by:

$$T = \frac{\tau_O + \tau_{B'}}{L_4} + Mg \sin(\theta_1) + \frac{(\tau_A + \tau_{C'}) L_{OB} \cos\left(\frac{\pi}{2} - \theta_3\right)}{L_4 L_{AC} \cos\left(\frac{\pi}{2} - \theta_4\right)} \quad (38)$$

where,

$$\begin{aligned}\tau_O &= \kappa_1 \theta_1 & \tau_{B'} &= \kappa_3 \left(\theta_3 - \frac{\pi}{2} \right) \\ \tau_A &= \kappa_2 \theta_2 & \tau_{C'} &= \kappa_4 \left(\theta_4 - \frac{\pi}{2} \right)\end{aligned}\quad (39)$$

APPENDIX B

LINEARIZED EQUATIONS

We can use a Taylor expansion to the first order to linearize the angles θ_2, θ_3 and θ_4 around $\theta = 0$.

$$\theta_3 = \theta_3(0) + [\theta'_3(0)] \cdot \theta \quad (40)$$

$$\theta_4 = \theta_4(0) + [\theta'_4(0)] \cdot \theta \quad (41)$$

$$\theta_2 = \theta_3(0) + \theta_4(0) - \pi + [1 + \theta'_3(0) + \theta'_4(0)] \cdot \theta \quad (42)$$

Here the primes denote a derivative to θ . We simplify this notation by introducing the following nomenclature: $n_3 = \theta'_3$, $n_4 = \theta'_4$ and $n = 1 + \theta'_3(0) + \theta'_4(0)$. We already have the equations for θ_3, θ_4 (eq. (34),(35)) we only need to find their derivatives. To this end we start by normalizing L_{OA}, L_{BC} and L_{AC} with L_{OB} .

$$p = \frac{L_{OA}}{L_{OB}} \quad q = \frac{L_{BC}}{L_{OB}} \quad s = \frac{L_{AC}}{L_{OB}} \quad (43)$$

We also introduce the substitution:

$$z(p, \theta, \gamma) = 1 + p^2 - 2p \cos(\theta + \gamma) \quad (44)$$

We then rewrite eq. (37), (34) and (35) using the above expressions.

$$\gamma = \cos^{-1} \left(\frac{1 + p^2 - q^2 - s^2}{2p} \right) \quad (45)$$

$$\theta_3 = \cos^{-1} \left(\frac{1 - p \cos(\theta + \gamma)}{\sqrt{z(p, \theta, \gamma)}} \right) \quad (46)$$

$$+ \cos^{-1} \left(\frac{z(p, \theta, \gamma) + q^2 - s^2}{2q \sqrt{z(p, \theta, \gamma)}} \right) \quad (47)$$

$$\theta_4 = \cos^{-1} \left(\frac{s^2 + q^2 - z(p, \theta, \gamma)}{2qs} \right) \quad (48)$$

We then take the derivative to θ .

$$\frac{d\theta_3}{d\theta} = \frac{1 - p \cos(\theta + \gamma)}{z(p, \theta, \gamma)} - 1 \quad (49)$$

$$- \frac{p \sin(\theta + \gamma) \left[1 - \frac{q^2 - s^2}{z(p, \theta, \gamma)} \right]}{\sqrt{4q^2 s^2 - [z(p, \theta, \gamma) - q^2 - s^2]^2}} \quad (50)$$

$$\frac{d\theta_4}{d\theta} = \frac{2p \sin(\theta + \gamma)}{\sqrt{4q^2 s^2 - [q^2 + s^2 - z(p, \theta, \gamma)]^2}} \quad (51)$$

When we evaluate θ_3, θ_4 and their derivatives at $\theta = 0$ all cosines and sines become a function of γ .

When we substitute in eq. (37) we get rid of these trigonometric functions and are left with the following:

$$\theta_3(0) = \cos^{-1} \left(\frac{1 - p^2 + q^2 + s^2}{2\sqrt{q^2 + s^2}} \right) \quad (52)$$

$$+ \cos^{-1} \left(\frac{q^2}{\sqrt{q^2 + s^2}} \right) \quad (53)$$

$$\theta'_3(0) = \frac{1 - p^2 - q^2 - s^2}{2(q^2 + s^2)} \quad (54)$$

$$- \frac{s\sqrt{4p^2 - (1 + p^2 - q^2 - s^2)^2}}{2q(q^2 + s^2)} \quad (55)$$

$$\theta_4(0) = \frac{\pi}{2} \quad (56)$$

$$\theta'_4(0) = \frac{\sqrt{4p^2 - (1 + p^2 - q^2 - s^2)^2}}{2qs} \quad (57)$$

For the dimensions given in Tab. V we find that $p = 0.9786$, $q = 0.1452$ and $s = 0.0323$ resulting in $\theta_3(0) = \pi/2$, $\theta'_3(0) = -1$ and $\theta'_4(0) = 31$. The fact that $\theta_3(0)$ and $\theta_4(0)$ are right angles is something that is designed for on purpose. In that case $\theta_3(0) + \theta_4(0) - \pi$ is zero and the linearized θ_2 does not have an intercept. We can furthermore see that $\theta'_3(0)$ cancels out the 1 and that the angular magnification $n = n_4 = \theta'_4(0) = 31$. If we look at the differential angles for this case we find that:

$$\delta_1(\theta) = \theta \quad (58)$$

$$\delta_2(\theta) = \theta_2 = n \cdot \theta \quad (59)$$

$$\delta_3(\theta) = \theta_3 - \theta_3(0) = n_3 \cdot \theta \quad (60)$$

$$\delta_4(\theta) = \theta_4 - \theta_4(0) = n_4 \cdot \theta \quad (61)$$

$$(62)$$

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